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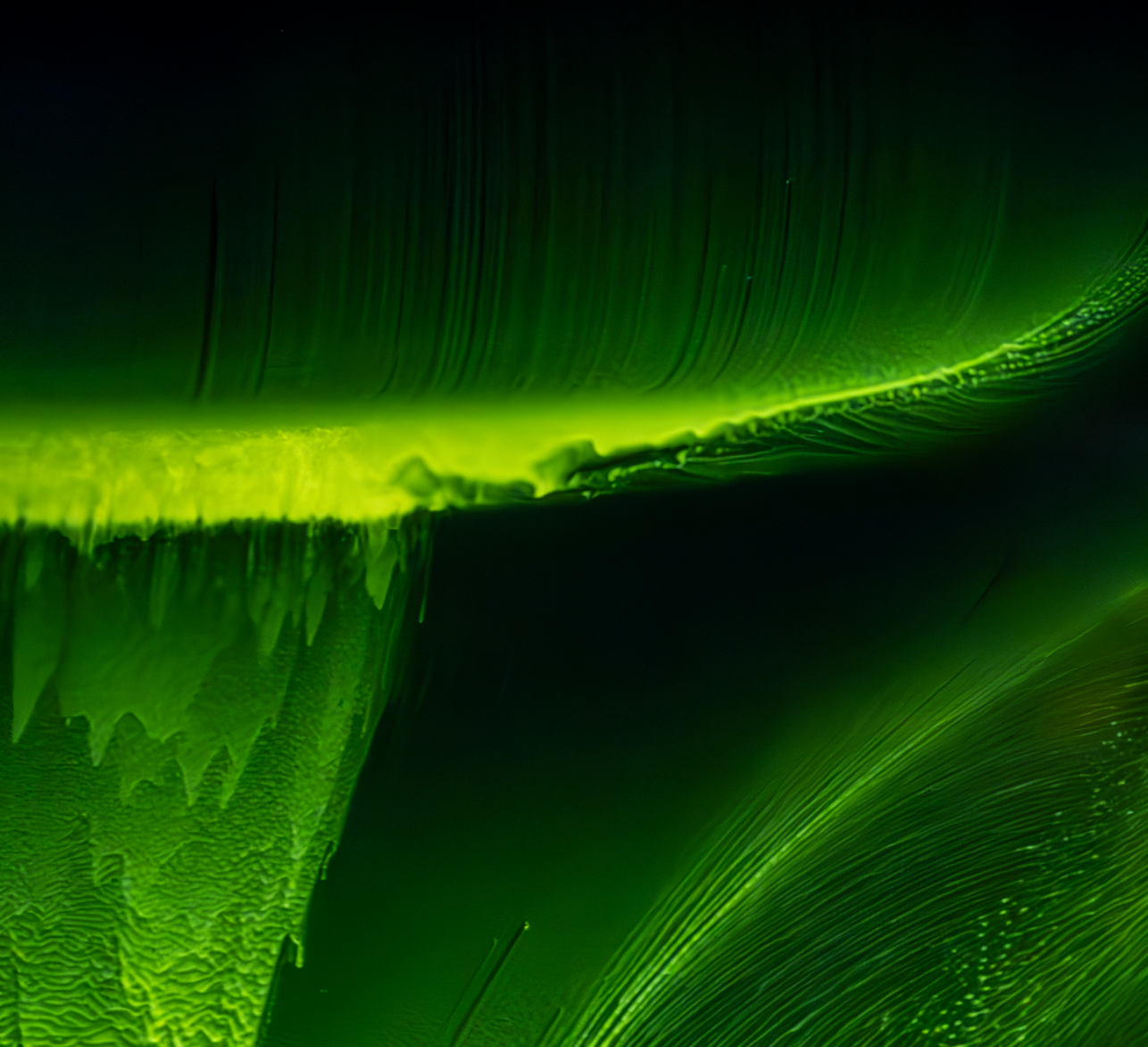
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DYNAMIC STALL OF THICK WIND TURBINE AIRFOILS

Mehdi Doosttalab



DYNAMIC STALL OF THICK WIND TURBINE AIRFOILS

DYNAMIC STALL OF THICK WIND TURBINE AIRFOILS

Dissertation

for the purpose of obtaining the degree of doctor
at Delft University of Technology
by the authority of the Rector Magnificus,
Prof.dr.ir. H. Bijl,
chair of the Board for Doctorates
to be defended publicly on
Monday, 5 October 2026, 10:00

by

Mehdi DOOSTTALAB

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Keywords: Airfoils, wind turbine, dynamic stall, flatback airfoils, vortex generators

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Cover: Surface fluorescent oil flow visualization showing the near-wall effect between the airfoil and the wind tunnel wall.

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Dedicated to all my teachers, especially the rigorous ones who never overlooked my smallest mistakes and pushed me beyond my imagined boundaries.

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Delft, Sep 2026

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SUMMARY

Over recent decades, the wind energy sector has undergone rapid expansion, driven by escalating global energy demand and the widespread transition toward sustainable power generation. This development has been accompanied by a substantial increase in wind turbine rotor size. However, scaling up turbine dimensions introduces significant engineering challenges, particularly in achieving high aerodynamic performance while maintaining cost effective structures. The resulting trend toward longer and more slender blades has amplified both static and dynamic loading, making structural integrity a central concern in contemporary turbine design. To address these challenges, modern rotor concepts incorporate thicker inboard and mid-span airfoils, including flatback configurations, which improve structural efficiency and aerodynamic performance while reducing overall blade mass. At the same time, enlarging blade radii leads to greater elastic deformations, which further increase the dynamic loads experienced by the rotor.

Ensuring reliable turbine operation requires accurate load assessment, which depends heavily on aeroelastic and dynamic load simulations across a broad range of operating conditions. As blades become larger and more flexible, they are subjected to stronger inflow turbulence, more pronounced unsteady variations in angle of attack, and an increased likelihood of transient stall phenomena. These factors make accurate unsteady aerodynamic modeling essential. In this context, dynamic stall models are frequently employed to predict unsteady aerodynamic forces. Historically, however, most dynamic stall models were developed for thin airfoils in helicopter applications, and despite subsequent adaptations for wind turbine use, their suitability for thick airfoils remains limited. The aerodynamic response of thick airfoils, particularly those typical of large modern blades, still lacks comprehensive validation, resulting in uncertainties in load predictions. Moreover, thick mid-span airfoils experience large unsteady angles of attack and are more susceptible to early stall, necessitating active or passive flow-control strategies. Flatback airfoils and vortex generators have emerged as promising solutions, offering increased lift, delayed flow separation, and improved stall characteristics. Nevertheless, their combined impact on unsteady aerodynamics is not yet fully understood, revealing a critical gap in current aeroelastic modeling capabilities.

This knowledge gap compromises the reliability of current aeroelastic models and poses risks for the design, optimization, and structural assessment of large modern turbines. Consequently, this dissertation is motivated by the need to advance the understanding and modeling of dynamic stall phenomena in thick wind turbine airfoils, with particular emphasis on the combined effects of vortex generators under unsteady flow conditions. By

addressing these gaps, the work aims to contribute improved physical insight and enhanced predictive capability for the aerodynamic behavior of next generation wind turbine blades.

Therefore, several wind tunnel campaigns were conducted to investigate the unsteady aerodynamic characteristics of two 35% thick airfoil configurations, comprising a conventional airfoil and a flatback airfoil, tested in different setups in Low-speed, low-Turbulence wind Tunnel (LTT) of the Delft University of Technology.

The results show that flatback airfoils exhibit inherently more stable hysteresis loops, as evidenced by reduced force fluctuations, larger lift overshoots, and delayed separation compared to sharp trailing edge airfoils, primarily due to their more favorable suction side pressure gradients under the studied operating conditions. Dynamic stall on these thick airfoils is governed not by leading edge vortex formation, as seen in thin airfoils, but by a gradual trailing edge separation mechanism that produces moderated load transients. Reduced frequency strongly influences dynamic stall, i.e, higher frequencies delay separation, enhance lift overshoot, and stabilize flow evolution for both airfoils. The addition of vortex generators substantially improves flow attachment during the upstroke phase, increases lift overshoot, and suppresses force fluctuations, particularly when placed near the leading edge. Their effectiveness, however, depends sensitively on chordwise placement, the underlying airfoil geometry, and the oscillation regime. While flatback airfoils generally benefit from improved stall delay, they experience larger force excursions during fully separated flow, indicating both the potential and the risks of employing blunt trailing edge designs. The evaluation of the Beddoes–Leishman and Risø dynamic stall models demonstrates that both models are not capable to predict separated flow behavior on thick airfoils, especially regarding lift overshoot, lift drop, post-stall loads, drag, and pitching moment. Lack of modeling accuracy lead to systematic discrepancies in model predictions, even when tuning time constants. These findings highlight the need for new or revised separation and reattachment modules tailored specifically for thick airfoils for reliable load prediction in the inboard and mid-span regions of large modern wind turbine blades.

SAMENVATTING

In de afgelopen decennia heeft de windenergiesector een snelle groei doorgemaakt, gedreven door de toenemende wereldwijde vraag naar energie en de transitie naar duurzame energieopwekking. Deze ontwikkeling gaat gepaard met een aanzienlijke toename van de rotorgrootte van windturbines. Het opschalen van de rotorgrootte brengt echter aanzienlijke technische uitdagingen met zich mee, met name het bereiken van goede aerodynamische prestaties in combinatie met kostenefficiënte constructies. De daaruit volgende trend naar langere en slankere bladen heeft zowel de statische als de dynamische belastingen vergroot, waardoor de structurele integriteit een centrale rol is gaan spelen in het ontwerp van moderne windturbines. Om deze uitdagingen het hoofd te bieden, maken hedendaagse rotorconcepten gebruik van dikkere profielen in het wortel- en midden-spangebied, waaronder flatback-configuraties, die zowel de structurele efficiëntie als de aerodynamische prestaties verbeteren en tegelijkertijd de totale bladmassa reduceren. Tegelijkertijd leidt de toename van de bladradius tot grotere elastische vervormingen, wat de dynamische belastingen op de rotor verder vergroot.

Betrouwbaar werkende windturbines vereisen een nauwkeurige bepaling van de belastingen, die in sterke mate afhankelijk zijn van aero-elastische en dynamische belastingssimulaties over een breed scala aan bedrijfscondities. Naarmate bladen groter en flexibeler worden, worden zij blootgesteld aan sterkere instroomturbulentie, grotere stationaire variaties in de invalshoek en een verhoogde kans op tijdelijke overtrekverschijnselen. Deze factoren maken een nauwkeurige modellering van stationaire aerodynamica essentieel. In dit kader worden dynamische overtrekmodellen veelvuldig toegepast om stationaire aerodynamische krachten te voorspellen. Historisch gezien zijn deze modellen echter voornamelijk ontwikkeld voor dunne profielen in helikoptertoepassingen en, ondanks latere aanpassingen voor windturbines, blijft hun geschiktheid voor dikke profielen beperkt. De aerodynamische respons van dikke profielen, met name die kenmerkend zijn voor grote moderne rotorbladen, is nog onvoldoende gevalideerd, wat leidt tot aanzienlijke onzekerheden in belastingsvoorspellingen. Bovendien worden dikke mid-span profielen blootgesteld aan grote stationaire invalshoeken en zijn zij gevoeliger voor vroegtijdige overtrek, waardoor actieve of passieve stromingscontroletechnieken noodzakelijk worden. Flatback-profielen en vortex generators hebben zich daarbij ontwikkeld tot veelbelovende oplossingen, doordat zij zorgen voor een verhoogde lift, vertraagde stroming loslating en verbeterde overtrekarakteristieken. Desondanks is de gecombineerde invloed van deze concepten op de stationaire aerodynamica nog niet volledig begrepen, wat wijst op een kritieke lacune in de huidige aero-elastische modelleringscapaciteiten.

Deze kennislacune ondermijnt de betrouwbaarheid van bestaande aero-elastische modellen en vormt een risico voor het ontwerp, de optimalisatie en de structurele beoordeling van grote moderne windturbines. Dit proefschrift is dan ook gemotiveerd door de noodzaak om het begrip en de modellering van dynamische overtrekverschijnselen in dikke windturbine-profielen te verbeteren, met in het bijzonder aandacht voor de gecombineerde invloed van vortex generators onder instationaire stromingscondities. Door deze lacunes aan te pakken, beoogt dit werk bij te dragen aan een verbeterd fysisch inzicht en een verhoogde voorspellende capaciteit voor het aerodynamische gedrag van windturbinebladen van de volgende generatie.

Daartoe zijn meerdere windtunnelcampagnes uitgevoerd om de instationaire aerodynamische karakteristieken te onderzoeken van twee luchtprofielconfiguraties met een relatieve dikte van 35%, bestaande uit een conventioneel luchtprofiel en een flatback-luchtprofiel. Deze profielen zijn onder verschillende opstellingen getest in de lage-snelheids-, lage-turbulentiewindtunnel (LTT) van de Technische Universiteit Delft.

De resultaten tonen aan dat flatback-profielen intrinsiek stabielere hysteresislussen vertonen, grotere liftovershoots ontwikkelen en een vertraagde stroming loslating laten zien in vergelijking met profielen met een scherpe achterrand, voornamelijk als gevolg van gunstigere drukgradiënten aan de zuigzijde. Dynamische overtrek bij deze dikke profielen wordt niet gedomineerd door de vorming van een voorrandvortex, zoals bij dunne profielen, maar door een geleidelijk voortschrijdend achterrand loslaat mechanisme dat gematigde belastingsvariaties veroorzaakt. De gereduceerde frequentie heeft een sterke invloed op het dynamische overtrekgedrag: hogere frequenties vertragen de stroming loslating, vergroten de liftovershoot en stabiliseren de stromingsontwikkeling voor beide profielen. De toepassing van vortex generators verbetert de stromingsaanhechting aanzienlijk tijdens de opgaande slag, verhoogt de liftovershoot en dempt krachtschommelingen, met name wanneer deze nabij de voorrand worden geplaatst. Hun effectiviteit is echter sterk afhankelijk van de positie langs de koorde, de onderliggende luchtprofielgeometrie en het oscillatieregime. Hoewel flatback-profielen over het algemeen profiteren van een grotere overtrekvertraging, vertonen zij in volledig losgelaten stroming grotere krachtpieken, wat zowel het potentieel als de risico's van het toepassen van stompe achterranden onderstreept. De evaluatie van de dynamische overtrekmodellen van Beddoes–Leishman en Risø laat zien dat beide modellen het gedrag van losgelaten stroming op dikke profielen niet adequaat kunnen voorspellen, met name wat betreft liftovershoot, liftafname, post-overtrekbelastingen, weerstand en steekmoment. De beperkte modelnauwkeurigheid leidt tot systematische afwijkingen in de voorspellingen, zelfs bij het afstellen van tijdsconstanten. Deze bevindingen benadrukken de noodzaak van nieuwe of aangepaste loslaat en heraanliggingsmodellen die specifiek zijn afgestemd op dikke profielen, om betrouwbare belastingsvoorspellingen mogelijk te maken in het wortel- en mid-spangebied van grote moderne windturbinebladen.

NOMENCLATURE

Greek symbols

- α = angle of attack [$^{\circ}$]
 α = shear wave number [m^{-1}]
 α_0 = zero lift angle of attack [$^{\circ}$]
 α_0 = speed of sound [m/s]
 $\alpha_{3/4}$ = angle of attack at three-quarter chord [$^{\circ}$]
 α_e = equivalent angle of attack [$^{\circ}$]
 α_E = equivalent angle of attack (Risø model) [$^{\circ}$]
 α_f = quasi steady angle of attack [$^{\circ}$]
 $\alpha_{\pm fs}$ = fully separated flow angles of attack [$^{\circ}$]
 α_j = shear wave number [-]
 α_L = ONERA model coefficient [-]
 α_m = mean angle of attack [$^{\circ}$]
 α_n^{CRIT} = critical angle of attack [$^{\circ}$]
 α_{qs} = quasi steady angle of attack [$^{\circ}$]
 β = compressibility factor [-]
 ϵ = model parameter [-]
 γ = specific heat ratio [-]
 η = drag recovery factor [-]
 $\lambda_2 = \lambda_2$ -criterion [s^{-2}]
 λ_L = ONERA model coefficient [-]
 μ = absolute viscosity [Ns/m^2]
 ν = frequency [Hz]
 ρ = density [kg/m^3]
 ρ_s = mean density [kg/m^3]
 σ = diaphragm volume increase [-]
 σ = past normalized time [-]
 σ_L = ONERA model coefficient [-]
 τ = time constant [-]
 τ_f = Risø model coefficient [-]
 τ_p = Risø model coefficient [-]
 τ_v = reduced time [-]
 ϕ = indicial response function [-]
 $\phi(k)$ = phase angle [rad]

ω = angular frequency [rad/s]
 ω^+ = non-dimensional pitch rate (equal to $\omega C/V$) [-]
 Ω = rotational speed [rad/s]

Abbreviations

AEP = Annual Energy Production [$kWh/year$]
AI = Adaptive Integration
AOA = Angle Of Attack [$^\circ$]
BEM = Blade Element Momentum
B-L = Beddoes-Leishman
BSA = Bottom Stagnation Angle [$^\circ$]
BSDS = Blade Systems Design Study
CCD = Charge-Coupled Device
CDD = Constraint Decision Defuzzification
CFD = Computational Fluid Dynamics
COA = Center Of Area
COG = Center Of Gravity
COM = Center Of Maximum
CX = Carbon Experimental
DES = Detached Eddy Simulation
DC = Direct Current
DES = Detached Eddy Simulation
DFR = Drag Force Range, the distance between maximum and minimum C_D
DLC = Design Load Case
DS = Downstroke
DSAOA = Dynamic Stall Angle Of Attack [$^\circ$]
ECD = Extreme Coherent gust with Direction change
EDC = Extreme Direction Change
EOG = Extreme Operating Gust
ETM = Extreme Turbulence Model
EWM = Extreme Wind speed Model
EWS = Extreme Wind Shear
FFT = Fast Fourier Transformation
FOV = Field Of View
GEKO = Generalized $k-\omega$
IEA = International Energy Agency
IEC = International Electrotechnical Commission
IFFT = Inverse Fast Fourier Transformation
LES = Large Eddy Simulation
LESP = Leading Edge Suction Parameter

LEVoGs = Leading Edge Vortex Generators
LEV = Leading Edge Vortex
LFA = Local Inflow Angle [$^{\circ}$]
LFR = Lift Force Range, the distance between maximum and minimum C_L
LTI = Linear Time-Invariant
LTT = Low-Turbulence Tunnel
MDNS = Mean Dynamic Normal Slope [rad]
MDO = Multidisciplinary Design Optimization
MLOS = Maximum Lift Overshoot
MUB = Modell-Unterschallwindkanal Braunschweig
NACA = National Advisory Committee for Aeronautics
NASA = National Aeronautics and Space Administration
NFR = Normal Force Range, the distance between maximum and minimum C_N
NMAE = Normalized Mean Absolute Error [%]
NREL = National Renewable Energy Laboratory
NTM = Normal Turbulence Model
NWP = Normal Wind Profile model
ODE = Ordinary Differential Equation
PISO = Pressure Implicit Splitting of Operators
PIV = Particle Image Velocimetry
PSID = Pounds per Square Inch Differential
Re = Reynolds number [-]
RMS = Root-Mean-Square
SA = Simulated Annealing
SERI = Solar Energy Research Institute
SFA = Spanwise Inflow Angle [$^{\circ}$]
SIMPLE = Semi-Implicit Method for Pressure-Linked Equations
TX = Twist-Bend Experimental
SNL = Sandia National Laboratories
SST = Shear Stress Transport
STL = Stall
TSA = Top Stagnation Angle [$^{\circ}$]
TX = Twist-Bend Experimental
UAE = Unsteady Aerodynamics Experiment
URANS = Unsteady Reynolds-Averaged Navier-Stokes
US = Upstroke
VGs = Vortex Generators

Symbols

A = amplitude of oscillation [$^{\circ}$]

- A_i = indicial response function coefficient [-]
 b_i = indicial response function coefficient [-]
 C = chord [m]
 C_D = drag coefficient [-]
 C_{D0} = zero-lift drag coefficient [-]
 C_f = skin friction coefficient [-]
 C_{fij} = SNEL model coefficient correction term [-]
 C_L = lift coefficient [-]
 C_{Li} = lift coefficient correction term [-]
 C_M = moment coefficient [-]
 C_{M0} = zero-lift moment coefficient [-]
 C_M^v = vortex induced moment coefficient [-]
 C_N = normal coefficient [-]
 C_{Nl} = critical normal coefficient [-]
 C_N^p = normal force coefficient under potential flow condition [-]
 C_N^v = vortex induced normal coefficient [-]
 C_p = pressure coefficient [-]
 $C_{T(s)}^p$ = tangential force coefficient [-]
 C_T^v = vortex induced tangential force coefficient [-]
 C_v = vortex lift increment [-]
 D = deficit function [-]
 D_{C_T} = static C_T curve fitting parameter [-]
 e_L = ONERA model coefficient [-]
 f = non-dimensional steady separation position [-]
 f_{ti} = SNEL model forcing term [-]
 f_s = sampling frequency [Hz]
 f_q = frequency [Hz]
 F = external force [-]
 $F(x)$ = objective function [-]
 F^{**} = fuzzy-based objective function [-]
 $G(x)$ = inequality constraints [-]
 $H(k)$ = Transfer Function [-]
 $H(x)$ = equality constraints [-]
 $\ddot{h}$ = plunge acceleration [m/s^2]
 i = imaginary number [-]
 I_{XX} = sectional moment of inertia [m^4]
 J_n = Bessel function [-]
 k = reduced frequency [-]
 k_p = polytropic constant [-]
 K_{C_T} = static C_T curve fitting parameter [-]
 L = tube length [m]

- M = Mach number [-]
 p = pressure [N/m^2]
 P_i = Bézier-Spline control point [-]
 Pr = Prandtl number [-]
 $P_{(t)}$ = Bézier-Spline parametric curve [-]
 Q = Q-criterion [-]
 r = blade local radius [m]
 r_L = ONERA model coefficient [-]
 R = tube radius [m]
 R_{kk} = roughness Reynolds number [-]
 s = non-dimensional time [-]
 s_L = ONERA model coefficient [-]
 s_k = grade of membership [-]
 t = time [s]
 $T(t_i)$ = knot vector [-]
 t^* = dimensionless time [-]
 T_p = leading edge pressure peak time constant [-]
 T_f = boundary layer separation lag time constant [-]
 T_f = time constant ($\emptyset$ ye model) [-]
 T_v = leading edge vortex decay constant [-]
 T_{vl} = leading edge vortex convection time constant [-]
 U_∞ = free stream velocity [m/s]
 v = pressure transducer volume [m^3]
 v_i = velocity vector ($i=x, y, z, r, \theta, s$) [m/s]
 V = free stream velocity [m/s]
 V_{hub} = hub wind speed [m/s]
 V_{in} = cut-in velocity [m/s]
 V_{out} = cut-out velocity [m/s]
 V_r = rated velocity [m/s]
 V_t = tube volume [m^3]
 V_v = pressure transducer volume [m^3]
 $\bar{x}$ = vector of design variables [-]
 x_{c_k} = center of gravity of the membership function [-]
 x_i = aerodynamic state variables [-]
 $x(n)$ = raw recorded pressure signal [pa]
 X = deficiency function [-]
 $X(x_i)$ = design variables vector [-]
 X_i = aerodynamic state variables [-]
 $X(k)$ = complex Fourier coefficient of the signal [-]
 X_{CP} = center of the pressure offset [-]
 y_i = aerodynamic state variables [-]

Y = deficiency function [-]

Y^+ = dimensionless wall distance [-]

Superscripts

C = circulatory quantity [-]

$CRIT$ = critical quantity [-]

dyn = total dynamic coefficient [-]

f = separated flow quantity [-]

fs = fully separated [-]

ind = induced quantity [-]

I = non-circulatory quantity [-]

P = total quantity (B-L model) [-]

p = attached flow quantity (Risø model) [-]

st = steady quantity [-]

$'$ = lagged value [-]

$''$ = unsteady boundary layer lagged value [-]

$-$ = mean value [-]

Subscripts

att = attached flow quantity [-]

bl = unsteady boundary layer lagged value [-]

c = circulatory quantity [-]

$corr$ = corrected quantity [-]

dyn = dynamic quantity [-]

f = lagged value [-]

∞ = infinity [-]

max = maximum value [-]

pot = potential flow quantity [-]

st = steady quantity [-]

Part I: Foundations and Methodology

1

RESEARCH BACKGROUND

This chapter introduces the context and motivation for the research conducted in this dissertation, which focuses on aerodynamic aspects of wind turbine airfoils. It begins by outlining the broader scientific and engineering challenges that inspired the study, followed by a discussion of the specific gaps in knowledge that this work aims to address. The main objectives of the research are clearly defined, and the key questions guiding the investigation are formulated. This is followed by an outline of the dissertation structure and its contribution to addressing the research questions. Finally, the second section presents the state of the art, including the background knowledge and literature review necessary to understand the scope and foundations of this work.

1.1 INTRODUCTION

1.1.1 BACKGROUND

Driven by rising oil prices, increasing energy demand, and environmental concerns, the wind energy industry has experienced rapid growth in recent decades [1]. Over the past three decades, there has been a significant increase in the size of newly developed wind turbines, with rotor diameters expanding from 10 – 15m meters to a recent offshore turbine introduced by Vestas measuring 236m in diameter [2]. Designing and developing such turbines presents a significant challenge for engineers, who balance lightweight and low-cost materials with aerodynamic performance. As a result of the design evolution, modern wind turbines feature longer and more slender rotor blades subjected to significantly higher static and dynamic loads than previous generation rotors [3].

New design approaches involve increasing the thickness of the inboard and mid-span section of the blade, particularly using flatback airfoils [4]. This technique has improved structural and aerodynamic efficiency while reducing blade weight. However, as the rotor blade radius increases, the blade elastic deformations also increase [5], resulting in higher dynamic loads on the turbine.

Load assessment plays a crucial role in the design and certification of wind turbines [6, 7]. This process entails the simulation of diverse static and dynamic load scenarios, including wind conditions, rotor speed, pitch angle variations, and wind gusts, in accordance with standardized protocols. The calculated loads are essential for verifying the structural integrity and longevity of the entire turbine system. These loads are primarily induced by environmental and operational factors including wind gusts, rapid pitch angle changes, yaw angle, wind shear, and aeroelastic torsion due to rotation. Dynamic load and aeroelastic analysis are essential components of wind turbine load assessment, particularly under unsteady and turbulent operating conditions. To support these analyses, dynamic stall models are commonly employed to predict transient aerodynamic forces. Over the past several decades, a variety of dynamic stall models have been developed, primarily for thin airfoils in helicopter aerodynamics [8–12], with a relative thickness ranging from 9.5% to 12% [8]. Although some of these models have been adapted for wind turbine applications [13], their suitability for thick airfoil geometries, which are typical of modern turbine blades and feature relative thicknesses ranging from 15% to 50% [14, 15], remains limited. There is a notable lack of validated data regarding the accuracy of load predictions produced by these models when applied to thick wind turbine airfoils, indicating a significant gap in current aeroelastic modeling capabilities.

The utilization of high relative thickness airfoils in the mid-span section exposes them to elevated dynamic loads and significant fluctuations in angles of attack. Given their inherently low stall angle of attack, the implementation of active or passive flow

control methods becomes imperative to address and mitigate these challenges effectively. Using flatback airfoils [4] and/or vortex generators is one of the most advanced and effective solutions. Flatback airfoils are formed by thickening the aft camber line of sharp trailing edge profiles. Compared to sharp trailing edge thick airfoils (conventional thick airfoils), they offer structural benefits and improved aerodynamics, including higher lift coefficients and steeper lift curves due to reduced adverse pressure gradients on the suction side [4, 15, 16]. Vortex generators, on the other hand, produce streamwise vortices that energize the boundary layer by mixing high energy outer flow with low-momentum inner flow. This mechanism delays flow separation [17, 18], increases lift, and raises the stall angle. The effectiveness of vortex generators depends on their geometry, size, and chordwise placement [19, 20].

1.1.2 MOTIVATION AND RESEARCH QUESTIONS

While thin airfoils have been extensively studied [21], the dynamic behavior of thick wind turbine airfoils, especially when combined with vortex generators remains comparatively underexplored. Accurately assessing loads and designing turbines requires an understanding of the dynamic characteristics of airfoils used in rotor blade design. However, our current knowledge of the dynamic characteristics of thick conventional and flatback airfoils, particularly in combination with VGs, is limited. Insufficient knowledge in this area could lead to significant errors in dynamic load calculations for rotor blades, resulting in poor design and potential damage to wind turbines.

This dissertation aims to advance the understanding of dynamic stall phenomena in thick wind turbine airfoils, with a particular focus on configurations incorporating vortex generators. In summary, the following research questions need to be addressed:

1. How does rotor blade length influence the blade geometry layout?
 - What are the design trends for future large scale rotor blades?
 - What design challenges arise, and which technological solutions are available?
2. How frequently does dynamic stall occur in large scale wind turbines, and what are its defining characteristics?
 - What are the primary triggering factors for dynamic stall?
 - Which physical phenomena define dynamic stall?
 - What is the dynamic stall envelope in the root and mid-span regions of large scale wind turbines?
 - How does the dynamic stall envelope vary along the blade span?

3. How do engineering solutions for large wind turbine blades influence airfoil performance?
 - What are the characteristics difference between conventional and flatback thick airfoils?
 - How do vortex generators affect the aerodynamic performance of thick airfoils?
4. What are the dynamic stall characteristics of thick wind turbine airfoils?
 - What are the differences in dynamic stall behavior between conventional and flatback thick airfoils?
 - What is the influence of vortex generators on the dynamic stall behavior of both conventional and flatback airfoils?
 - How do different unsteady conditions affect the dynamic stall characteristics of thick airfoils?
 - What is the underlying flow physics governing dynamic stall in thick airfoils?
5. How do current dynamic stall models perform in simulating the unsteady aerodynamic behavior of thick wind turbine airfoils?
 - How do the various dynamic stall models compare in accuracy when benchmarked against experimental data?
 - How accurately can the effects of vortex generators on dynamic stall in thick airfoils be modeled?
 - How comparable is the modeling accuracy for conventional versus flatback airfoils?

The present research serves as a direct extension of an extensive longitudinal study conducted at Hochschule Bremen between 2008 and 2011. That foundational phase focused on the Multidisciplinary Design Optimization (MDO) and rigorous aerodynamic characterization of flatback airfoils specifically engineered for multi-megawatt wind turbine systems. Detailed documentation of this preliminary research is archived in Appendix B.

1.1.3 APPROACH AND THESIS OUTLINE

A multi-stage investigative approach was developed, integrating advanced experimental fluid dynamics with numerical modeling to address the research objectives. This methodology was designed to capture the complex interaction between structural boundary conditions and aerodynamic responses in thick airfoils, investigate the unsteady fluid

dynamics and flow physics of the dynamic stall phenomenon, and assess the accuracy of dynamic stall models used in aeroelastic analyses of wind turbines.

The research began with a comprehensive aeroelastic simulation of a state of the art, multi-megawatt wind turbine. This phase was critical for establishing the specific unsteady boundary conditions, such as effective angle of attack variations and flow velocities, that thick airfoils experience under operational loads defined by the IEC 61400-1 international standard.

The experimental component involved an extensive wind tunnel campaign conducted on two distinct airfoil geometries, both featuring a 35% relative thickness. These models were specifically engineered to allow for a direct performance comparison between a conventional sharp trailing edge and a flatback geometry. Data acquisition utilized high-frequency surface pressure transducers to resolve both steady and unsteady aerodynamic coefficients across a wide envelope of airfoil configurations and flow regimes. To ensure a high degree of data integrity, computational fluid dynamics simulations were executed in parallel, providing a validation benchmark for the complex post-processing algorithms required to interpret unsteady pressure data.

To further explore the underlying fluid mechanics of the dynamic stall phenomenon, Particle Image Velocimetry (PIV) was employed. This non-intrusive laser diagnostic technique allowed for the visualization and quantification of vortex shedding and dynamic stall mechanisms. Such a fundamental understanding of the flow physics is a prerequisite for the accurate implementation and calibration of semi-empirical dynamic stall models.

Finally, the experimental datasets were utilized to perform a rigorous sensitivity analysis and validation of two industry-standard dynamic stall models. These models were numerically coded and benchmarked against established literature to ensure their robustness in predicting the performance of thick airfoils used in modern wind turbine applications.

PART I: FOUNDATIONS AND METHODOLOGY

Chapter 1: Research background

This introductory chapter establishes the research trajectory by presenting the background. It provides an exhaustive literature review covering the evolution of large-scale wind turbine blades, the aerodynamic benefits and challenges of flatback airfoils, the implementation of passive flow control via vortex generators, and a theoretical overview of the dynamic stall phenomenon and existing mathematical modeling approaches.

Chapter 2: Methodology

This chapter provides a comprehensive overview of the research methodology. It begins with a numerical aeroelastic analysis of a contemporary wind turbine system to establish realistic operational parameters. This is followed by a detailed description

of the experimental setup, including wind tunnel specifications, the design and fabrication of the 35% thickness airfoil models, and a technical overview of the measurement instrumentation. The chapter concludes with a discussion of the specialized algorithms employed for unsteady data post-processing.

PART II: RESULTS

Chapter 3: Thick conventional and flatback airfoils in steady flow conditions

This chapter focuses on the comparative analysis of the conventional and flatback airfoils under steady flow conditions. It evaluates the impact of trailing edge geometry and vortex generators on aerodynamic performance and overall efficiency across multiple configurations.

Chapter 4: Dynamic stall characteristics of thick wind turbine airfoils

Chapter 4 investigates the hysteretic behavior and unsteady aerodynamic performance of both airfoils in their "clean" state (absence of flow control devices). A parametric study is presented, analyzing how factors such as boundary layer tripping, mean angle of attack, reduced frequency, and trailing edge thickness dictate the onset and severity of dynamic stall.

Chapter 5: Vortex generator effects on dynamic stall of thick airfoils

Building on the previous findings, this chapter examines the integration of vortex generators as a flow control strategy. It quantifies how these devices modify the flow physics of thick airfoils, specifically focusing on their ability to suppress or delay dynamic stall across different flow regimes and geometric profiles.

Chapter 6: Evaluating dynamic stall models for thick wind turbine airfoils

This chapter assesses the predictive accuracy of the Beddoes-Leishman (B-L) and Risø dynamic stall models when applied to thick airfoil geometries. The chapter begins with a fundamental analysis of the flow physics derived from PIV data and concludes with a direct comparison between the numerical model predictions and the experimental results obtained in the preceding chapters.

PART III: CONCLUSIONS

Conclusions and recommendations

This section synthesizes the major findings of the thesis, providing a critical assessment of the research contributions and offering strategic recommendations for future investigations into the aerodynamic optimization of thick airfoils.

PART IV: APPENDICES**Appendix A**

Dynamic stall models methodology and validation.

Appendix B

Comprehensive archival study regarding the multidisciplinary design optimization and aerodynamic analysis of flatback airfoils conducted at Hochschule Bremen (2008-2011) under supervision of Prof. Dr.-Ing. Olaf Frommann, which serves as the foundational basis for the current research.

1.2 STATE OF THE ART

1.2.1 LARGE-SCALE WIND TURBINES

In the field of offshore wind energy, modern turbines now exceed 15 MW in rated capacity, with rotor diameters reaching 236 meters. This growth facilitates enhanced energy capture and improved economies of scale, yet it also introduces a myriad of engineering challenges, particularly in the realm of turbine blade design.

As wind turbine blades increase in length, they are subjected to heightened gravitational and aerodynamic loads, which profoundly impact their structural integrity and dynamic behavior. This is largely driven by the "square-cube law," where the structural mass of the blade tends to scale cubically while the power capture only increases with the square of the radius [22]. These loads result in elevated bending moments, which require the utilization of advanced materials and structural configurations to maintain stiffness and prevent excessive deflection or fatigue failure [23]. In addition, the aeroelastic coupling between aerodynamic forces and structural deformation becomes more pronounced, increasing the risk of instabilities such as flutter, torsional divergence, and dynamic stall [24]. Another significant challenge arises in the domains of manufacturing and transportation [25].

To address these challenges, research initiatives such as the Blade System Design Study (BSDS) [26], launched by Sandia National Laboratories in 2002, have played a pivotal role. The BSDS program investigated advanced structural concepts, cost-effective manufacturing strategies, and innovative design approaches for large-scale wind turbine blades. One of the key outcomes of the BSDS was the identification of increased relative thickness in the inboard blade sections as a critical strategy for improving structural efficiency and reducing overall blade weight, two essential factors in the design of next generation wind turbine blades. Thicker airfoils in the root and mid-span regions provide greater internal volume, enabling the integration of robust structural components such as spar caps and shear webs, which enhance stiffness and fatigue resistance without significantly increasing mass. However, this approach results in significant aerodynamic penalties. Thick, sharp trailing edge airfoils tend to suffer from reduced maximum lift coefficients, increased sensitivity to leading edge roughness, and a propensity for early flow separation. The BSDS program underscored the benefits of employing flatback or blunt trailing edge airfoils in the inner region, encompassing up to approximately 40% of the blade span, as a solution to mitigate the issues associated with sharp trailing edge airfoils. These airfoils offer improved lift characteristics and reduced sensitivity to leading edge erosion, while also simplifying manufacturing processes and increasing structural robustness. Subsequently, the EU-funded AVATAR project (2013-2017) focused on improving and validating aerodynamic modeling tools for the design and analysis of next generation large-scale wind turbines exceeding 10 MW in rated power [27]. Together, these design strategies contribute to

the feasibility of ultra large blades capable of supporting turbines in the multi-megawatt class. This design trend is evident in several published reference wind turbine models, including the NREL 5 MW turbine [28], the INNWIND 20 MW concept turbine [29], the DTU 10 MW offshore wind turbine [30], Sandia National Laboratories 13.2 MW wind turbine studies [31], the LEANWIND 8 MW turbine [32], and the IEA Wind 15 MW [33] and 22 MW [34] offshore reference wind turbines.

FLATBACK AIRFOILS

Flatback airfoils are created by adding thickness to the aft portion of a sharp trailing edge airfoil along the camber line. This modification allows the airfoil to maintain high thickness-to-chord ratios without the severe aerodynamic drawbacks typically associated with conventional thick airfoils.

These airfoils have been extensively investigated and promoted by Sandia National Laboratories (SNL) as a solution to the structural and aerodynamic challenges inherent in large, modern wind turbine blades [4]. Compared to conventional thick airfoils with sharp trailing edges, flatback airfoils offer several structural and aerodynamic advantages. Figure 1.1 shows the difference between the sharp trailing edge airfoil (FB-3500-00500) and the flatback airfoil (FB-3500-1750), which were developed by Sandia National Laboratories in collaboration with the University of California, Davis [35].

The aerodynamic performance of flatback airfoils has been the subject of numerous experimental and computational studies. Wind tunnel experiments conducted by SNL and

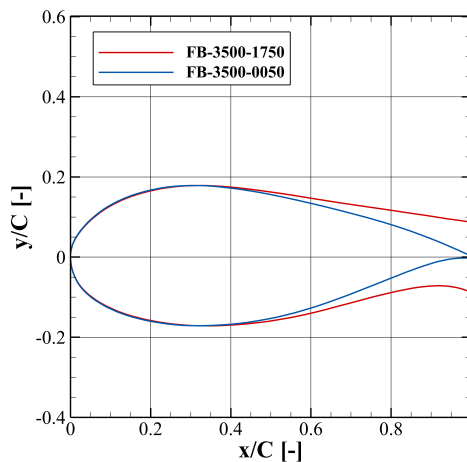


Figure 1.1: Flatback and sharp trailing edge thick airfoils comparison.

its collaborators have shown that, due to the reduced adverse pressure gradient on the suction side, flatback airfoils exhibit a higher lift coefficient and a steeper lift curve slope than sharp trailing edge airfoils of equivalent thickness. Furthermore, the lift coefficient of flatback airfoils has lower sensitivity to the leading edge roughness than the traditional sharp trailing edge airfoils, which is a highly desirable characteristic for wind energy applications. From a structural perspective, increasing the trailing edge thickness enables a more efficient material distribution, resulting in enhanced flapwise and edgewise stiffness. This is particularly beneficial for large blades, where structural requirements often dominate the design of the inboard (root) region. The improved stiffness provided by flatback airfoils facilitates weight reduction and enhances resistance to fatigue and buckling, both of which are critical for the long-term reliability of wind turbines.

The advantages of flatback airfoils have been successfully demonstrated in several large wind turbine blade design projects. One of the most notable examples is the Sandia 100-meter blade program [36]. In this study, a series of design evaluations were performed to assess the influence of flatback airfoils on blade mass, stiffness, and aerodynamic performance. The final design, designated SNL100-03, incorporated flatback airfoils in the inboard region of the blade. This configuration resulted in a 16% reduction in total blade weight compared to its predecessor, which employed conventional sharp trailing edge airfoils, while maintaining or improving both structural integrity and aerodynamic efficiency. These findings underscore the potential of flatback airfoils to enable lighter, stiffer, and more aerodynamically robust blades for next generation wind turbines.

Despite their structural and aerodynamic advantages, flatback airfoils present certain drawbacks. Most notably, they exhibit higher drag and generate increased aerodynamic noise compared to sharp trailing edge airfoils [37]. The blunt trailing edge introduces complex unsteady flow phenomena and noise generation mechanisms, which remain insufficiently understood. These effects are particularly critical for airfoils located in the mid-span region of the blade, where relative flow velocities are significantly higher than at the root. This issue is of particular importance for wind turbines deployed in noise-sensitive environments, where acoustic emissions are a key design constraint. From an aerodynamic perspective, the drag penalty associated with flatback airfoils has been shown to exert only a minor influence on rotor thrust and torque coefficients under typical inboard twist configurations. In fact, the additional lift generated by flatback airfoils can effectively offset the adverse impact of increased drag, thereby preserving overall rotor performance [38]. Flatback airfoils have been adopted by several leading wind turbine manufacturers, such as Nordex, Siemens Gamesa, Vestas, and GE, particularly for the inboard sections of large wind turbine blades. A detailed discussion of flatback airfoil design and performance is presented in Appendix B.

VORTEX GENERATORS

To mitigate flow separation and improve aerodynamic performance, various boundary layer control techniques have been developed. These methods can generally be divided into active and passive approaches. Active control strategies involve direct manipulation of the boundary layer through mechanisms such as suction, blowing, or turbulent mixing, often implemented via technologies like synthetic jets or plasma actuators. Although effective, such systems typically require external energy input and added mechanical complexity.

Passive control, on the other hand, relies on devices such as Vortex Generators (VGs), which are fixed aerodynamic surfaces designed to energize the boundary layer by diverting high-momentum fluid from the outer flow into the near-wall region [17, 18]. This re-energization reduces the tendency of the boundary layer to separate, particularly in regions with strong adverse pressure gradients. VGs are utilized across multiple sectors, including aerospace, automotive, and energy, where they are employed to control both external and internal flows.

VGs are especially effective in improving pressure recovery over airfoil surfaces and delaying separation [17], which is critical in the inboard sections of wind turbine blades where flow separation is more prevalent due to utilization of thick airfoils, lower Reynolds numbers, and complex three-dimensional flow structures. In wind turbine applications, VGs are strategically placed to stabilize flow and enhance blade performance, thereby contributing to increased energy yield. A variety of VGs geometries are available, including rectangular, triangular, and trapezoidal profiles [39], each offering distinct vortex characteristics and flow control capabilities. Furthermore, VGs arrays can be configured in co-rotating or counter-rotating arrangements [40], influencing the strength and orientation of the generated vortices and allowing for tailored flow control solutions depending on the aerodynamic requirements.

The aerodynamic effectiveness of VGs is strongly influenced by their geometric parameters and precise placement relative to the local boundary layer. For optimal vortex formation and boundary layer energization, the VGs tip height must exceed the local boundary layer thickness, which increases along the chord of the airfoil due to boundary layer growth. The chordwise position of the VGs array is a critical design variable [19]. VGs installed near the leading edge interact with thinner boundary layers and generate stronger streamwise vortices, which can significantly delay flow separation and increase the stall angle of attack. This upstream placement, however, also promotes earlier transition from laminar to turbulent flow, resulting in higher skin friction drag under attached flow conditions. Conversely, VGs positioned further aft interact with thicker boundary layers and, if their height is insufficient to penetrate the boundary layer, their effectiveness is diminished, yielding negligible improvements in aerodynamic performance. Aft-mounted VGs, while less effective at delaying stall, induce less drag in attached flow, which can be advantageous for optimizing the lift-to-drag ratio and, consequently, the Annual Energy Production (AEP) of wind turbines. The selection of VGs chordwise location is typically guided by the target stall angle of attack; in wind turbine applications, excessively high stall

angles are avoided to prevent unnecessary increases in maximum static and dynamic blade loads, which can compromise structural integrity and operational reliability. Thus, the relationship between VGs height, chordwise position, and local boundary layer characteristics is fundamental to achieving the desired balance between enhanced stall margin and minimized drag penalties. A comprehensive and authoritative treatment of VGs aerodynamics is provided by Baldaccino [41], whose work offers an in-depth analysis of the underlying flow mechanisms as well as detailed performance assessments of VGs on wind turbine airfoils; readers seeking a deeper understanding of VGs behavior and their aerodynamic implications are strongly referred to this source.

1.2.2 DYNAMIC STALL

As it has been discussed, the evolution of wind turbine technology over the past three decades has been marked by a substantial increase in rotor dimensions, reflecting the industry's pursuit of higher energy yields and improved efficiency. This scaling trend has led to the development of turbines with significantly larger and more slender blades, designed to operate under a wide range of atmospheric conditions. While these advancements have enabled greater performance, they have also introduced complex aerodynamic challenges. The longer blades are subject to highly variable and intensified loading, especially in unsteady regimes [5], necessitating a deeper understanding of transient aerodynamic phenomena.

Among these, dynamic stall is of particular importance. This effect arises from the inherently unsteady nature of wind and the rotational motion of the blades, influencing the instantaneous aerodynamic forces and moments acting on the rotor [6]. Accurate modeling of such phenomena is critical for predicting blade performance, assessing fatigue loads, and ensuring structural integrity.

Accurate load prediction and simulation of wind turbine blades have long posed significant challenges for engineers. These difficulties primarily stem from the inherently unsteady and stochastic nature of the atmospheric environment in which wind turbines operate. On one hand, the flow field around the rotor is highly unsteady and three-dimensional, influenced by turbulence, shear, and wake interactions. On the other hand, the continuous increase in rotor blade size over the past decade has further complicated the aerodynamic behavior of modern turbines, as it naturally leads to an increase in the structural flexibility of the rotor blade [42]. As a result, the analysis of unsteady aerodynamic loads has become increasingly complex. Despite ongoing research efforts, the detailed characteristics of these flow fields remain insufficiently understood [6, 43, 44].

Dynamic flow conditions are usually classified by ranges of reduced frequencies in wind turbines [6]. Reduced frequency is defined as $k = \omega C / 2V$. Here V is the airfoil relative velocity, ω is the angular frequency, and C is the chord length. Leishman [45] classified

the unsteady flow conditions for rotorcrafts into four categories. Steady flow for $k = 0$. For $0 \leq k \leq 0.05$, the flow is quasi-steady, and the unsteady effects are minor and can be neglected. Unsteady flow can be considered from $k \geq 0.05$, and for $k \geq 0.2$ the flow is categorized as highly unsteady. On the other hand, Pereira et al. [46] suggested considering unsteady effects for $k > 0.02$ for wind energy applications.

Leishman [45] classified the sources of unsteady aerodynamic loading into two categories: periodic and aperiodic. Aperiodic sources of aerodynamic instabilities include wind veer, atmospheric turbulence, wake interactions, and terrain-induced flow structures associated with complex topography, such as mountain generated vortices. On the other hand, yaw misalignment, blade-tower interaction, and wind shear are periodic unsteady aerodynamic sources.

Wind veer and vertical wind shear introduce rapid changes in wind direction and speed across the rotor plane, creating periodic variations in the local angle of attack and asymmetric blade loading, while turbulence and topographic features generate localized updrafts, downdrafts, and azimuth-dependent inflow distortions. Additional unsteadiness arises from blade-tower interaction, which modifies the local flow field and affects wake development. Yaw misalignment further amplifies these effects, producing the advancing-retreating blade phenomenon and a skewed wake, leading to asymmetric induction and imbalanced aerodynamic loading [7, 47]. Understanding and modeling these coupled inflow driven mechanisms is essential for accurate load prediction and effective turbine control strategies.

Dynamic stall results from a rapid change of angle of attack on an airfoil, affecting the boundary layer on the surface of the airfoil so that the onset of stall occurs at several degrees higher than the standard static polar. Furthermore, the flow remains separated over a wider range of decreasing angles of attack, delaying flow reattachment compared with static conditions and increasing the extent of aerodynamic hysteresis. This phenomenon can lead to high loads on a wind turbine, which can damage or drastically reduce the fatigue life of the turbine [48].

The effects of the unsteady flow have been experienced and studied for many years [45]. One of the first researchers who observed this phenomenon is Kramer [49]. He addressed three primary phenomena that caused the delay in the stall onset. The first one is the unsteadiness in the circulation during increasing the angle of attack, which sheds into the wake and reduces the lift. The second relates to the virtual camber in a pitching airfoil, which decreases the leading edge pressure and pressure gradient on the suction side for a given lift coefficient. This effect was also observed by Ericsson [50], Carta [51], Johnson et al. [52], Ericsson et al. [53], McCroskey [54], and Beddoes [55]. The third one is related to the unsteady effects in the boundary layer; especially reversal flows in the presence of external pressure gradients. This has been observed by McAlister et al. [56], Scruggs et al. [57], Telionis [58], and McCroskey [59]. All the mentioned phenomena delay the onset of stall. Ultimately, with a further increase in the angle of attack, a high adverse pressure gradient near the leading edge leads to flow separation. Leishman [45] addressed the

formation of a free shear layer downstream of the leading edge, which rolls up quickly and forms vortical structures in the boundary layer that move towards the trailing edge causing a better pressure recovery and an increase in lift. This vortex shedding process has been studied by Ham [60], McCroskey et. al. [61, 62], and Beddoes [63]. Later, more experimental studies opened up a better understanding of the onset of dynamic stall [55, 64–66]. Liiva et al. [67] and Wood [68] performed a full-scale experiment to study the dynamic stall on a helicopter rotor. The results showed that the dynamic stall quality is almost Mach number independent under different forcing conditions; however, the quantity behavior of dynamic stall could vary for different Mach numbers, different airfoils, and the presence of 3D conditions [45]. De Tavernier et al. [21] conducted a study on a vertical axis wind turbine airfoil with a maximum thickness of 25%. Their findings indicate that the height and mounting position of VGs are crucial factors in delaying or suppressing dynamic stall, depending on the VGs configuration. Chellini et al. [69] investigated the dynamic stall characteristics of the FFA-W3-211 airfoil used in IEA reference wind turbine designs. Their results revealed that high positive angles of attack are characterized by the absence of a leading-edge vortex, whereas at high negative angles of attack the flow is dominated by a vortex forming near the leading edge. Mai et al. [70], Heine et al. [71], and Le Pape et al. [72] investigated the impact of Leading Edge VGs (LEVoGs) on alleviating leading edge vortex shedding during dynamic stall events on rotorcraft airfoils. However, these findings are not applicable to thick airfoils used in wind energy applications, which exhibit no leading edge vortex separation [13] and have different VGs geometries, such as circular, backward wedge, and forward wedge. These VGs are installed in close proximity to the leading edge on the pressure side of the airfoil.

Zhu et al [73]. investigated the impact of rotation augmentation on dynamic stall by utilizing URANS and experimental data from the NREL Phase VI program. The study analyzed the dynamic stall response of a 3D non-rotating blade, a 3D-rotating blade, and a 2D airfoil based on the NREL S809 airfoil, which was exclusively employed in the NREL Phase VI blade. The findings indicated that rotational effects inhibit the expansion of separated flow during the upstroke (US) phase, hasten flow reattachment in the downstroke (DS) phase, and delay the onset of dynamic stall due to 3D-rotational influences. Additionally, a notable reduction in aerodynamic hysteresis was observed. The presence of rotational augmentation in the inboard section of the blade also led to negative aerodynamic pitch damping, potentially inducing stall flutter. Visbal and Garmann [74] employed Large Eddy Simulations to investigate the initiation of unsteady separation and the formation of dynamic stall vortex on a NACA 0012 airfoil with a constant pitch rate at the quarter-chord position at $Re = 2 \times 10^5$ and $Ma = 0.1$. The study observed the development of a laminar separation bubble as the transition point shifted towards the leading edge, which contracted with increasing angle of attack, leading to the buildup of leading edge suction. Subsequently, this laminar separation bubble destabilized beyond a critical point, resulting in suction collapse and the rapid formation of reversed flow near the leading edge. The turbulent separation facilitated the integration of turbulent boundary layer vorticity into a

coherent dynamic stall vortex. Additionally, the study noted high-frequency maximum pressure fluctuations near the leading edge prior to the breakdown of the laminar separation bubble. The authors underscored the crucial role of the laminar separation bubble in accurately predicting dynamic stall phenomena. While conducted at lower Reynolds numbers than those typical of modern wind turbines, the study provides valuable insight into the mechanisms governing dynamic stall onset.

AERODYNAMICS OF DYNAMIC STALL

Figure 1.2 illustrates various stages of dynamic stall. In the first stage, the US-phase, i.e., increasing the angle of attack, on the linear part of the lift curve, the flow reversal appears in the boundary layer due to the reduced adverse pressure gradient caused by the kinematic pitch rate. In the second stage, flow separation occurs at the leading edge, forming a leading edge vortex. If this vortex is moved towards the trailing edge on the suction side, which happens between the stage two and three, it generates additional lift, particularly if it is close to the surface. In some low Mach number cases, it has been reported that this lift overshoot can reach up to 50 to 100% of the maximum static lift coefficient [45]. However, it is important to note that this lift overshoot can result in a sudden nose-down pitching moment, which can be a critical issue from a structural standpoint. This is due to the movement of the suction peak along the suction side of the airfoil, which increases the lever arm length. The speed at which the vortex moves on the airfoil is estimated to be between one-third to half of the free stream velocity [75, 76]. In stage four, beginning of the DS-phase, i.e., decreasing the angle of attack, the vortex leaves the trailing edge and enters the wake downstream of the airfoil. At this point, the flow is fully separated on the suction side of the airfoil, and the flow field is the same as the static flow field at this angle of attack. The vortex shedding of the flow separation on the suction side, causes the lift fluctuations. As the angle of attack decreases, the reattachment process begins, but with a significant delay compared to the static lift polar [77]. This lag is related to the effect of the reverse kinematic-induced camber due to the negative pitch rate on the leading edge pressure gradient. Stage 5 represents a fully attached flow situation. The hysteresis observed in different stages reduces the aerodynamic damping, potentially leading to aeroelastic problems on the rotor blade [45]. Note that the dynamic stall mechanism described above is mainly characteristic of thin airfoils exhibiting leading edge separation. Thick wind turbine airfoils often experience trailing-edge initiated separation, resulting in different dynamic stall behavior. These differences are discussed in the results chapters of this thesis.

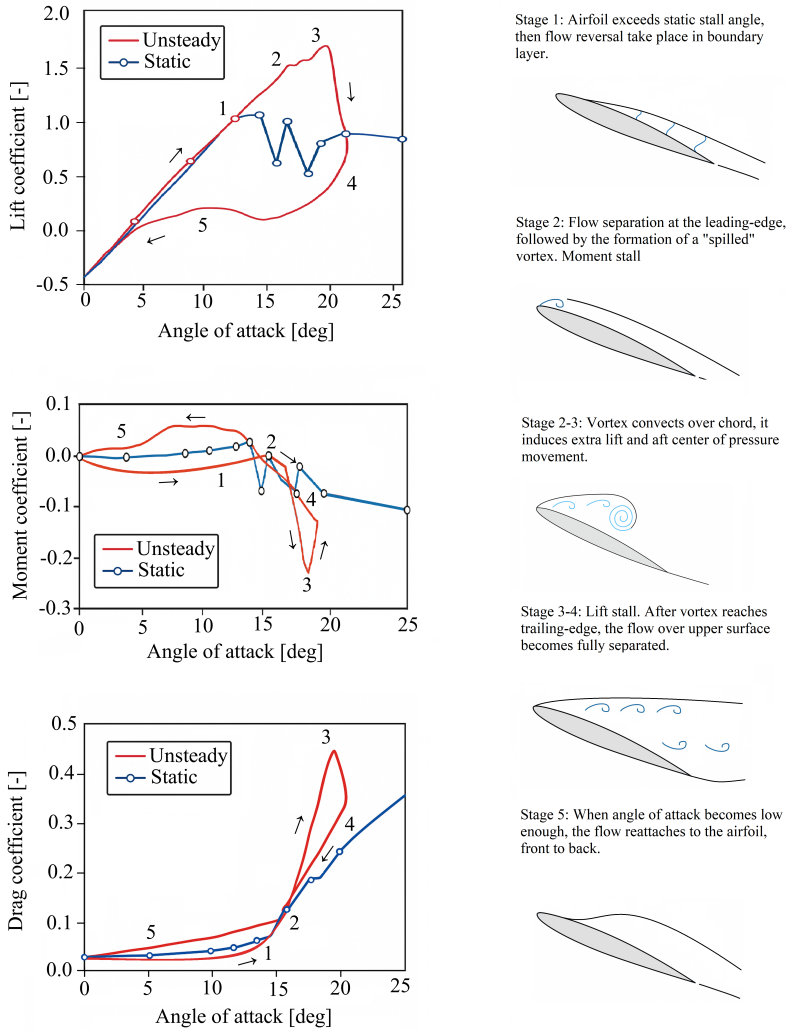


Figure 1.2: Different stages of dynamic stall. Reproduced from Leishman [45], based on the dynamic stall process originally described by McCroskey et al. [78].

1.2.3 DYNAMIC STALL ENGINEERING MODELS

Over the past decades, several dynamic stall models have been developed to simulate this complex phenomenon. These models aim to capture the transient lift, drag, and moment characteristics that arise during rapid changes in angle of attack, which are critical for accurate aeroelastic and load predictions in rotorcraft and wind turbine applications.

Among the most widely recognized models are the B-L [8], Risø [13], Øye [9], ONERA [10], Boeing-Vertol [11], and Snel [12] dynamic stall models. Each of these formulations incorporates varying degrees of empirical and semi-empirical approaches, tailored to specific flow regimes and application domains. Among these, the B-L and Risø models are used extensively in the wind turbine aeroelastic and load assessments codes, i.e., OpenFAST [79], HAWC2 [80], and Bladed [81].

Sheng et al. [82] modified the classical B-L model for low Mach number flows, introducing changes to the stall onset criterion, DS-phase behavior, chordwise force formulation, and dynamic vortex modeling. The revised model demonstrated good agreement with experimental data for the S809 and NACA 0012 airfoils, particularly in deep stall conditions.

Xiong et al. [83] further improved the B-L model for large horizontal-axis wind turbines equipped with thick airfoils operating at low Mach numbers. Key modifications included adjustments to the tangential force coefficient in the deep stall regime, simplification of the pitching moment coefficient related to trailing edge separation, and refinement of vortex-induced tangential forces. Validation against S809 data showed improved accuracy compared to the original model. Additionally, analysis of a 2 MW turbine revealed that the original B-L model overpredicted fatigue loads relative to the modified version.

Boutet et al. [84] tuned the B-L model for very low Reynolds and Mach numbers and a wide range of reduced pitch rates. Enhancements included updates to the attached flow module, stall onset criteria, time-varying reduced pitch rate, time delays, separation point dynamics, and vortex shedding behavior. Validation against flat plate, NACA 0012, and NACA 0018 airfoils showed improved accuracy, particularly in predicting normal force overshoot and reducing errors in the pitching moment hysteresis loop.

Elgammi et al. [85] introduced further refinements to the B-L model to improve its performance for the S809 airfoil. The updated model reduced the number of empirical constants and expanded the operational range. A new flow separation model replaced Kirchhoff's law, and a noise generator was integrated to better capture load fluctuations associated with vortex shedding. Validation using unsteady pressure measurements from Ohio State University showed improved correlation with experimental data and highlighted further potential for refinement.

Benedetto et al. [86] developed a low-cost computational dynamic stall model tailored for deep stall conditions involving multiple leading edge vortex shedding events and low Mach number flows. Based on the Larsen et al. [87] model and validated using extensive NACA 0012 experimental data, the model showed good agreement with measurements and offered a computationally efficient alternative for stall prediction.

Andersen et al. [88] extended the Risø dynamic stall model [13] to account for airfoils with deformable trailing edges. The model incorporated Gaunaa's inviscid formulation [89] in the linear region and was validated against wind tunnel measurements. While the model showed good agreement with experimental data, it lacked accuracy in predicting drag and moment coefficients under deep stall conditions.

Gupta and Leishman [90] adapted the classical B-L model to simulate the dynamic

behavior of the S809 airfoil across a wider range of angles of attack and Reynolds numbers. The modified model performed well in predicting the onset of dynamic stall but showed limitations during the reattachment process in the DS-phase. Nevertheless, the study concluded that with accurate representation of static stall characteristics, the model could effectively predict dynamic loads for wind turbine airfoils.

Bangga et al. [91] introduced a second-order dynamic stall model (IAG) incorporating higher harmonic vortex shedding effects. Developed from the B-L, Snel, Adema-Snel, and ONERA models, the IAG model included first- and second-order correction terms. Validation against S801, S809, S814, and NACA 4415 airfoils showed improved accuracy in capturing higher harmonics and better agreement with wind tunnel data compared to sinusoidal motion-based models.

Holierhoek et al. [92] conducted a comparative study of the ONERA, B-L, and Snel models using experimental data from Ohio State University and NASA Ames. The analysis revealed significant discrepancies between simulations and measurements, particularly in deep stall conditions. Each model showed strengths in specific cases but lacked consistent accuracy across all regimes. The authors emphasized the need for model validation tailored to the target airfoil and flow conditions prior to use in load calculations.

Adema et al. [93] presented an improved version of the Snel model, validated against experimental data from Ohio State University [94]. The modifications enhanced the model's accuracy, especially in predicting vibration amplitudes and frequencies. The improved model was implemented in the Bladed load calculation software and showed better performance in high angle of attack scenarios, as confirmed by comparisons with the New MEXICO experiments [95]. The study concluded that improved load prediction enables lower safety factors, contributing to reduced turbine and energy costs.

Guntur et al. [96] investigated the effects of rotational augmentation and dynamic stall on wind turbine blades by comparing CFD simulations with experimental data from the NREL Phase VI campaign. The simulations employed the Delayed Detached Eddy Simulation turbulence model and the Risø dynamic stall code [13], using rotationally augmented steady-state polars derived from the NREL Phase IV experiment. The results showed good agreement between the dynamic stall code and experimental data when using 3D-augmented polars. In the CFD simulations, two out of three cases matched the experimental measurements, while one case exhibited poor correlation. The study highlighted both similarities and discrepancies in dynamic stall behavior between rotating and non-rotating blades. Some of these discrepancies remain unexplained, indicating that further research is needed to improve our understanding of rotational augmentation effects in dynamic stall.

Pereira et al. [46] utilized data from the MEXICO project [95] to validate a customized version of the B-L dynamic stall model for horizontal-axis wind turbines. The model was implemented within a Blade Element Momentum (BEM) code and compared against experimental measurements. While the computational results failed to capture the trends observed in yawed configurations—particularly during the DS-phase of dynamic stall—the

predicted loading amplitudes showed good agreement with the experimental data. These findings suggest that while the model can reproduce certain aspects of dynamic stall behavior, further refinement is necessary to improve its accuracy under yawed flow conditions.

In the following, five commonly used dynamic stall models applicable to wind turbine aerodynamics are reviewed and compared with respect to their theoretical foundations, numerical implementation, and suitability for capturing unsteady aerodynamic phenomena in rotor applications.

BEDDOES-LEISHMAN MODEL

This dynamic stall model was developed in a series of research projects by Beddoes and Leishman [8, 97] for helicopter aerodynamics applications. This model has been validated against the experimental data several times. This is a semi-empirical model, which has a rather low complexity to keep the computational costs down. One of the advantages of this model is that it uses the experimental static airfoil data to extract the necessary empirical coefficients. The B-L model consists of four modules that function as a loop, in which, the output of each module is used as an input of the next module. These are: (1) unsteady attached flow, (2) unsteady non-linear trailing edge separated flow, (3) leading edge separation or dynamic stall onset, and (4) vortex shedding forces. In the following each module will be discussed separately.

UNSTEADY ATTACHED FLOW

In this module the unsteady attached flow coefficients are calculated. This module was developed by Beddoes [98] and then modified some years later by Leishman [99] and consists of two circulatory and noncirculatory loads. Note that the circulatory loads are the results of the forces around the airfoil, that create circulations, and non-circulatory loads are related to the acceleration of the flow over the surface of the airfoil and is based on the thin airfoil theory. The total normal force coefficient can be calculated as:

$$C_{N(s)}^P = C_{N(s)}^C + C_{N(s)}^I \quad (1.1)$$

Where the $C_{N(s)}^C$ and $C_{N(s)}^I$ are the circulatory and noncirculatory normal force coefficient respectively and s is the non-dimensional time, defined as required time for a fluid particle to travel the half chord length, which obtained by:

$$s = \frac{2V}{C} t \quad (1.2)$$

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The $C_{N(s)}^C$ can be obtained by using the Duhamel's superposition of step indicial aerodynamic response integral [100]:

$$C_{N(s)}^C = \frac{dC_N}{d\alpha}(\alpha_{qs(0)}\phi_{(s)} + \int_0^s \frac{\alpha_{qs}}{dt}\phi_{(s-\sigma)}d\sigma - \alpha_0) \quad (1.3)$$

Where $\phi_{(s)}$ is the ratio between the unsteady circulatory normal loading and the steady solution and α_0 is the zero normal force coefficient angle of attack. Jones [101] approximation of the Wagner's indicial lift response function [102] can be used to calculate $\phi_{(s)}$:

$$\phi_{(s)} = 1 - A_1 e^{-b_1 \beta^2 s} - A_2 e^{-b_2 \beta^2 s} \quad (1.4)$$

Where $A_1 = 0.165$, $A_2 = 0.335$, $b_1 = 0.045$, $b_2 = 0.3$ are the constants for the flat plate and $\beta = \sqrt{1 - M^2}$ is the compressibility factor. Combining above equations yields:

$$C_{N(s)}^C = \frac{dC_N}{d\alpha}(\alpha_{e(s)} - \alpha_0) \quad (1.5)$$

Where the $\alpha_{e(s)}$ is the effective or equivalent angle of attack due to presence of the wake. $\alpha_{e(s)}$ can be obtained by:

$$\alpha_{e(s)} = \alpha_{qs(s)} - X_{(s)}^1 - X_{(s)}^2 \quad (1.6)$$

$\alpha_{qs(s)}$ is the quasi-steady angle of attack at the current time, which is calculated by:

$$\alpha_{qs} = \left(\alpha + \frac{C}{2V} \frac{d\alpha}{dt} \right) \quad (1.7)$$

and

$$X_{(s)}^1 = X_{(s-\Delta s)}^1 e^{(-b_1 \beta^2 \Delta s)} + A_1 \Delta \alpha_{qs(s)} e^{(-b_1 \beta^2 \frac{\Delta s}{2})} \quad (1.8)$$

$$X_{(s)}^2 = X_{(s-\Delta s)}^2 e^{(-b_2 \beta^2 \Delta s)} + A_2 \Delta \alpha_{qs(s)} e^{(-b_2 \beta^2 \frac{\Delta s}{2})} \quad (1.9)$$

Where $\Delta s = s_n - s_{n-1} = 2V/c(t_n - t_{n-1})$ and $\Delta \alpha_{qs(s)} = \alpha_{qs(s)} - \alpha_{qs(s-1)}$. On the other hand, the noncirculatory coefficient $C_{N(s)}^I$ is calculated via:

$$C_{N(s)}^I = \frac{4K_\alpha T_I}{M} \left(\frac{\Delta \alpha_{qs(t)}}{\Delta t} - D_{(t)} \right) \quad (1.10)$$

Where $T_I = MC/V$, $K_\alpha = 0.75$, and $\Delta t = t_n - t_{n-1}$

$$D_{(t)} = D_{(t-\Delta t)} e^{\left(\frac{-\Delta t}{K_\alpha T_I} \right)} + \left(\frac{\Delta \alpha_{qs(t)}}{\Delta t} - \frac{\Delta \alpha_{qs(t-\Delta t)}}{\Delta t} \right) e^{\left(\frac{-\Delta t}{2K_\alpha T_I} \right)} \quad (1.11)$$

The tangential force coefficient can be calculated as:

$$C_{T(s)}^P = \frac{dC_N}{d\alpha} (\alpha_{e(s)} - \alpha_0) \tan \alpha_{e(s)} \quad (1.12)$$

The same approach of normal force coefficient can be used to calculate the pitching moment coefficient. The circulatory pitching moment coefficient is obtained by:

$$C_{M(s)}^C = -\frac{dC_N}{16 d\alpha} (q(s) - X_{(s)}^3) \quad (1.13)$$

$$X_{(s)}^3 = X_{(s-\Delta s)}^3 e^{(-b_5 \beta^2 \Delta s)} + A_5 \Delta q_{(s)} e^{(-b_5 \beta^2 \frac{\Delta s}{2})} \quad (1.14)$$

$$q = \frac{C}{V} \frac{d\alpha}{dt} \quad (1.15)$$

The non-circulatory pitching moment coefficient has two components and is computed by:

$$C_{M,\alpha(s)}^I = -\frac{1}{M} \frac{T_I}{2M} \left[A_3 b_3 T_{\alpha,M} \left(\frac{d\alpha(s)}{dt} - X_{(s)}^4 \right) + A_4 b_4 T_{\alpha,M} \left(\frac{d\alpha(s)}{dt} - Y_{(s)}^4 \right) \right] \quad (1.16)$$

$$X_{(s)}^4 = X_{(s-\Delta s)}^4 e^{-\left(\frac{\Delta s}{b_3 T_{\alpha,M}}\right)} + \left(\frac{d\alpha(s)}{dt} - \frac{d\alpha_{(s-\Delta s)}}{dt} \right) e^{-\left(\frac{\Delta s}{2b_3 T_{\alpha,M}}\right)} \quad (1.17)$$

$$Y_{(s)}^4 = Y_{(s-\Delta s)}^4 e^{-\left(\frac{\Delta s}{b_4 T_{\alpha,M}}\right)} + \left(\frac{d\alpha(s)}{dt} - \frac{d\alpha_{(s-\Delta s)}}{dt} \right) e^{-\left(\frac{\Delta s}{2b_4 T_{\alpha,M}}\right)} \quad (1.18)$$

$$T_{\alpha,M} = \frac{(A_3 b_4 + A_4 b_3) 2M}{b_3 b_4 (1 - M)} \quad (1.19)$$

$$C_{M,q(s)}^I = -\frac{7T_{q,M}}{12M} \frac{T_I}{2M} \left(\frac{dq(s)}{dt} - D_{M,q(s)}^1 \right) \quad (1.20)$$

$$D_{M,q(s)}^1 = D_{M,q(s-\Delta s)}^1 e^{-\left(\frac{\Delta s}{T_{q,M}}\right)} + \left(\frac{dq(s)}{dt} - \frac{dq_{(s-\Delta s)}}{dt} \right) e^{-\left(\frac{\Delta s}{2T_{q,M}}\right)} \quad (1.21)$$

$$T_{q,M} = \frac{14M}{15(1 - M) + 1.5\beta^2 M^2 A_5 b_5 \frac{dC_N}{d\alpha}} \quad (1.22)$$

UNSTEADY NON-LINEAR TRAILING EDGE SEPARATED FLOW

To consider the loss of the lift slope due to the trailing edge separation, which causes nonlinearity of the curve slope, especially for thick airfoils of the wind industry, this model uses the theory of Kirchhoff and Helmholtz for the flat plate to model the normal force coefficient in this situation. It is function of angle of attack, attached lift slope, and location

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of the separation point (f) on the airfoil. The separation point function is defined between 0 at the trailing edge and 1 at the leading edge. The function of f is usually defined by the available experimental data for a given airfoil as a function of angle of attack. The normal non-linear normal force coefficient for the separated trailing edge flow is:

$$C_N^{st} = \frac{dC_N}{d\alpha} \left(\frac{1 + \sqrt{f}}{2} \right)^2 (\alpha - \alpha_0) \quad (1.23)$$

Another source of the non-linearity in the unsteady flow is related to the boundary layer development lag and the leading edge pressure lag. The leading edge pressure lag is calculated as follows, which determines an equivalent angle of attack of a lagged force, that produces an identical leading edge pressure.

$$C'_N = C_{N(s)}^P - D_{pf(s)} \quad (1.24)$$

Where the D_{pf} is the deficit function.

$$D_{pf(s)} = D_{pf(s-\Delta s)} e^{\frac{-\Delta s}{T_p}} + \left(C_{N(s)}^P - C_{N(s-\Delta s)}^P \right) e^{\frac{-\Delta s}{2T_p}} \quad (1.25)$$

Where the T_p is the nondimensional leading edge pressure peak time constant, which is a function of Mach number and airfoil shape [8, 103]. To consider the effect of the boundary layer response lag, a second separation position function is defined as:

$$f_{bl} = f_p - D_{bl} \quad (1.26)$$

Where the D_{bl} is the deficit function and the f_p is calculated by:

$$f_p = f_{(\alpha_f)} \quad (1.27)$$

Where

$$\alpha_f = \frac{C'_N}{\frac{dC_N}{d\alpha}} + \alpha_0 \quad (1.28)$$

And the $D_{bl(s)}$ is calculated as:

$$D_{bl(s)} = D_{bl(s-\Delta s)} e^{\frac{-\Delta s}{T_f}} + (f_{p(s)} - f_{p(s-\Delta s)}) e^{\frac{-\Delta s}{2T_f}} \quad (1.29)$$

Note that the T_f is the boundary layer unsteady time constant. And finally, the nonlinear normal force coefficient, considering the trailing edge separation is determined by:

$$C_{N(s)}^f = \frac{dC_N}{d\alpha} \left(\frac{1 + \sqrt{f_{bl}}}{2} \right)^2 (\alpha_{e(s)} - \alpha_0) + C_{N(s)}^I \quad (1.30)$$

The tangential force and pitching moment coefficients are calculated by:

$$C_{T(s)}^f = \eta C_{T(s)}^P \sqrt{f_{bl}} \quad (1.31)$$

$$C_{M(s)}^f = C_{N(s)}^f X_{CP.N(s)}(f_{bl}) \quad (1.32)$$

Where η is the drag recovery factor, which is derived from the static airfoil data and X_{CP} is the center of the pressure offset from the quarter-chord position and in the above equation is used as a function of trailing edge separation $X_{CP} = f(f_{sep})$, which is calculated from the static airfoil polars by:

$$X_{CP.N}(\alpha) = \frac{C_M^{st} - C_{M0}}{C_N^{st}} \quad (1.33)$$

LEADING EDGE SEPARATION OR DYNAMIC STALL ONSET

As the leading edge vortices move towards the trailing edge, they cause a moving suction peak that affects the normal force coefficient. The effect of the leading edge vortex shedding on the normal force coefficient can be defined by the difference between the circulatory normal force and the aforementioned unsteady non-linear normal force coefficient, which was obtained from the Kirchhoff theory. The term is defined as:

$$C_{v(s)} = C_{N(s)}^P \left(1 - \left(\frac{1 + \sqrt{f_{bl}}}{2} \right)^2 \right) \quad (1.34)$$

And the vortex shedding normal force is obtained by:

$$C_{N(s)}^v = \begin{cases} C_{v(s-\Delta s)} e^{-\frac{\Delta s}{T_v}} + (C_{v(s)} - C_{v(s-\Delta s)}) e^{-\frac{\Delta s}{2T_v}}; & 0 < \tau_{v(s)} < T_{vl} \\ C_{v(s-\Delta s)} e^{-\frac{\Delta s}{T_v}}; & else \end{cases} \quad (1.35)$$

Where T_v is the leading edge vortex decay constant and T_{vl} is the leading edge vortex convection time constant and τ_v is the reduced time, to define the location of the leading edge vortex above the airfoil. Pereira et.al [46] and Elgammi et. al [104] redefined the value of $\tau_{v(s)}$ as:

$$\tau_{v(s)} = \begin{cases} \tau_{v(s-\Delta s)} + 0.45 \Delta s; & C_{N(s)}^f > C_{NI} \\ 0; & C_{N(s)}^f < C_{NI} \text{ and } \Delta \alpha_{e(s)} > 0 \end{cases} \quad (1.36)$$

Where C_{NI} is the inviscid critical static normal force, defined by:

$$C_{NI} = \frac{dC_N}{d\alpha} (\alpha_n^{CRIT} - \alpha_0) \quad (1.37)$$

Where α_n^{CRIT} is the critical angle of attack, in which the moment coefficient has a breaking point [8]. Depending on the airfoil and low condition, different approaches can be used to determine the critical angle of attack [105]. The tangential force component can be calculated by following [90, 97, 105]:

$$C_{T(s)}^v = K_{C_T} + C_{T(s)}^f f_{bl} \left[D_{C_T} (C'_N - C_{Nl}) + (f_{bl} - f_p) \right] \quad (1.38)$$

Where K_{C_T} and D_{C_T} are obtained by curve fitting of a ramp motion at low-frequency. However, For practical proposes, the static airfoil data can be used instead [105]. The pitching moment term, that caused by moving the leading edge vortex is obtained by:

$$C_{M(s)}^v = -X_{CP.N(s)} C_{N(s)}^v = -0.25 \left[1 - \cos \left(\frac{\pi \tau_{v(s)}}{T_{vl}} \right) \right] C_{N(s)}^v \quad (1.39)$$

And finally, the total dynamic normal force coefficients are obtained by:

$$C_{N(s)}^{dyn} = C_{N(s)}^v + C_{N(s)}^f = \frac{dC_N}{d\alpha} \left(\frac{1 + \sqrt{f_{bl}}}{2} \right)^2 (\alpha_{e(s)} - \alpha_0) + C_{N(s)}^I + C_{N(s)}^v \quad (1.40)$$

$$C_{T(s)}^{dyn} = \begin{cases} C_{T(s)}^f; & |C'_N| \leq C_{Nl} \\ C_{T(s)}^v; & |C'_N| > C_{Nl} \end{cases} \quad (1.41)$$

$$C_{L(s)}^{dyn} = C_{N(s)}^{dyn} \cos \alpha + C_{T(s)}^{dyn} \sin \alpha \quad (1.42)$$

$$C_{D(s)}^{dyn} = C_{D0} + \left(C_{N(s)}^{dyn} \sin \alpha - C_{T(s)}^{dyn} \cos \alpha \right) \quad (1.43)$$

$$C_{M(s)}^{dyn} = C_{M0} + \left[C_{M(s)}^C + \left(C_{M,\alpha(s)}^I + C_{M,q(s)}^I \right) \right] + C_{M(s)}^f + C_{M(s)}^v \quad (1.44)$$

RISØ MODEL

This model was developed at Risø National Laboratory in Denmark by Hansen et. al [13] for wind energy applications and is a modified version of B-L model and has therefore the same block structure of the B-L model. This model ignores the compressibility impulsive forces and leading edge separation, due to the low-speed flow regimes and thick airfoils application in the wind industry. Considering these assumptions, the attached lift can be obtained by Theodorsen Theory and only the trailing edge separation is considered in the stall conditions.

ATTACHED FLOW

For a thin symmetric airfoil undergoing small harmonic pitch and plunge motions, the unsteady lift can be approximated using Theodorsen's theory. According to the analysis by Van der Wall and Leishman, for moderate reduced frequencies and using Duhamel's integral formulation, the lift can be written as:

$$L = \pi \rho \frac{C^2}{4} (\dot{V}\alpha + V\dot{\alpha} + \ddot{h}_{1/2}) + \pi \rho CV \left[\omega_{3/4}(0) \phi(s) + \int_0^s \frac{d\omega_{3/4}}{d\sigma} \phi(s-\sigma) d\sigma \right] \quad (1.45)$$

Here, $(\cdot) = \partial/\partial t$, $\ddot{h}_{1/2}$ denotes the plunge acceleration, α the geometric angle of attack, ϕ the indicial function, and s the non-dimensional time given by:

$$s = \frac{2}{C} \int_0^t \frac{V d\tau}{dt} \quad (1.46)$$

The first term represents the non-circulatory (added-mass) component of the lift, associated with the acceleration of the surrounding air mass. Note that the $\ddot{h}_{1/2}$ and $\dot{V}\alpha$ terms may be neglected for moderate reduced frequencies. The second term corresponds to the circulatory contribution, describing the memory effect introduced by previously shed vorticity in the wake. This term may be interpreted as an effective downwash at the three-quarter-chord location. Hence,

$$\omega_{3/4}^{\text{eff}} = \omega_{3/4}(0) \phi(s) + \int_0^s \frac{d\omega_{3/4}}{d\sigma} \phi(s-\sigma) d\sigma \quad (1.47)$$

Performing integration by parts on Duhamel's integral and rewriting it in terms of the non-dimensional variables yields:

$$\omega_{3/4}^{\text{eff}} = \omega_{3/4}(t) \phi(0) - \int_0^t \omega_{3/4}(t') \frac{d\phi}{dt'} \left(\frac{2}{C} \int_{t'}^t V(\tau) d\tau \right) dt' \quad (1.48)$$

where t and t' correspond to the non-dimensional variables s and σ , respectively. The indicial function ϕ is approximated using two exponential time lags:

$$\phi(s) = 1 - A_1 e^{-b_1 s} - A_2 e^{-b_2 s} \quad (1.49)$$

where the constants A_1 , A_2 , b_1 , and b_2 depend on the specific application (e.g. wind turbines, helicopter blades, etc). Substituting $s = \frac{2}{C} \int_0^{t'} \frac{V d\tau}{dt}$ into equation (1.49) and differentiating with respect to t' yields:

$$\frac{d\phi}{dt'} \left(\frac{2}{C} \int_{t'}^t V(\tau) d\tau \right) = -\frac{2V(t')}{C} \sum_{i=1}^2 b_i A_i e^{-b_i \frac{2}{C} \int_{t'}^t V(\tau) d\tau} \quad (1.50)$$

Inserting equations (1.50) into (1.48) gives:

$$\omega_{3/4}^{\text{eff}} = \omega_{3/4}(t)(1 - A_1 - A_2) + y_1(t) + y_2(t) \quad (1.51)$$

where the auxiliary states $y_i(t)$ are defined as

$$y_i(t) = b_i A_i \frac{2}{c} \int_0^t \omega_{3/4}(t') V(t') e^{-b_i \frac{2}{c} \int_{t'}^t V(\tau) d\tau} dt' \quad (1.52)$$

with $i = 1, 2$. Differentiating equation (1.52) with respect to time gives:

$$\dot{y}_i + b_i \frac{2V}{C} y_i = b_i A_i \frac{2V}{C} y_i \omega_{3/4}, \quad y_i(0) = 0. \quad (1.53)$$

Using the Beddoes–Leishman assumption $\alpha_{3/4} = \omega_{3/4}/V$ and defining $y_i = V x_i$, one obtains:

$$\dot{x}_i + \frac{2V}{C} \left(b_i + \frac{C \dot{V}}{2V^2} \right) x_i = b_i A_i \frac{2V}{C} \alpha_{3/4} \quad (1.54)$$

with $x_i(0) = 0$ and $V \neq 0$.

The effective angle of attack under attached flow conditions is therefore:

$$\alpha_E = \alpha_{3/4}(1 - A_1 - A_2) + x_1(t) + x_2(t). \quad (1.55)$$

The corresponding unsteady attached-flow lift coefficient is:

$$C_L^p = 2\pi(\alpha_E - \alpha_0) + \frac{\pi C \frac{d\alpha}{dt}}{(2V)} \quad (1.56)$$

Where α_0 is the zero-lift angle of attack. It should be emphasized that the acceleration term is neglected, since for moderate reduced frequencies, its order is lower than the pitch rate term.

STALL WITH TRAILING EDGE SEPARATION

In contrast to the B–L model, which accounts for both leading and trailing edge separation, the Risø model considers only trailing edge separation. This simplification is justified by the fact that leading edge separation is not a dominant phenomenon for the thick airfoils typically used in wind turbine applications, unlike in aviation airfoils. Since the model does not attempt to represent the motion of a leading edge vortex, its mathematical formulation is correspondingly simpler than that of the B–L model. As discussed previously for the B–L model, the static lift coefficient can be written (here using C_L instead of C_N) as:

$$C_L^{st} = \frac{dC_L}{d\alpha} \left(\frac{1 + \sqrt{f(\alpha)}}{2} \right)^2 (\alpha - \alpha_0) \quad (1.57)$$

where $dC_L/d\alpha$ is the lift curve slope in the linear region of the polar, and $f(\alpha)$ is the separation point function. For a known $dC_L/d\alpha$, the separation point can be determined using:

$$f^{st} = \left(2 \sqrt{\frac{C_L^{st}(\alpha)}{C_{L,\alpha}(\alpha - \alpha_0)} - 1} \right)^2 \quad (1.58)$$

Note that the fully separated flow ($f = 0$) angles of attack α^{+fs} and α^{-fs} for the suction and the pressure side of the airfoil can be determined via:

$$\left| C_L^{st}(\alpha^{\pm fs}) \right| = \left| \frac{\frac{dC_L}{d\alpha}(\alpha^{\pm fs} - \alpha_0)}{4} \right| \quad (1.59)$$

Beyond this range, equation (1.57) no longer applies, and the separation point function becomes zero. To resolve this limitation, the static lift curve is expressed as a linear interpolation between fully attached and fully separated flow:

$$C_L^{st} = \frac{dC_L}{d\alpha}(\alpha - \alpha_0)f^{st} + C_L^{fs}(\alpha)(1 - f^{st}) \quad (1.60)$$

where the lift coefficient for fully separated flow is given by:

$$C_L^{fs} = \frac{C_L^{st} - \frac{dC_L}{d\alpha}(\alpha - \alpha_0)f^{st}}{1 - f^{st}} \quad (1.61)$$

It can be seen the above equation is limited in the fully attached flow ($f^{st}=1$). Substituting the separation point function into equation (1.61) shows:

$$C_L^{fs}(\alpha) \rightarrow \frac{C_L^{st}(\alpha)}{2};$$

when

$$f^{st} \rightarrow 1$$

for

$$\frac{dC_L}{d\alpha}(\alpha - \alpha_0) \rightarrow C_L^{st}(\alpha)$$

This implies that, at full separation, the lift coefficient is half that of fully attached flow.

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TRAILING EDGE DYNAMIC LIFT

As in the B-L model, two state variables, x_3 and x_4 , are used to represent the dynamics of trailing edge separation. The B-L model describes the dynamic behaviour of trailing edge separation through two differential equations involving these variables, which are associated with the lift and boundary layer respectively. In the Risø model, trailing edge separation is assumed to depend on the pressure distribution, which itself depends on the lift coefficient. A time delay between the pressure and the lift is assumed, leading to the modified relation:

$$\dot{x}_3 + T_p^{-1} x_3 = T_p^{-1} C_L^p(t) \quad (1.62)$$

where $C_L^p(t)$ is the attached flow unsteady lift (given by equation (1.56)), and T_p is the pressure lag time constant. The initial condition is $x_3(0) = 0$. The variable $x_3(t) = C_L^{p'}$ acts as an effective lift coefficient that defines a corresponding quasi-steady angle of attack:

$$\alpha_f = \frac{C_L^{p'}}{\frac{dC_L}{d\alpha}} + \alpha_0 \quad (1.63)$$

For the aforementioned angle of attack, the equivalent separation point of $f' = f^{st}(\alpha_f)$ can be obtained using equation (1.58). The x_4 variable models the lag of the boundary layer for the trailing edge separation and is obtained by:

$$\dot{x}_4 + T_f^{-1} x_4 = T_f^{-1} f'(t) \quad (1.64)$$

Where the T_f is a time constant of the lag in the boundary layer. Considering the equation (1.60), the unsteady separation point is defined: $f'' = x_4(t)$ with the initial condition of $x_4(0) = 0$, which is equivalent to f_{bl} in B-L model. The total unsteady lift, under attached condition and stalled dynamic trailing edge separation is calculated by:

$$C_L^{dyn} = \frac{dC_L}{d\alpha} (\alpha_E - \alpha_0) f'' + C_L^{fs}(\alpha_E) (1 - f'') + \pi T_u \frac{d\alpha}{dt} \quad (1.65)$$

Where $T_u = \frac{C}{2V}$

Note that equations (1.54), (1.62), (1.64), and (1.65) should be solved simultaneously.

DRAG

The unsteady drag model differs from the B-L model by constraining the unsteady drag to variations around the static drag curve, which serves as an input to the model. The drag is composed of induced drag, which results from changes in the effective lift force due to

wake-induced downwash, and viscous drag, which arises from the friction and pressure drag of the viscous boundary layer.

Induced drag: Under unsteady conditions, the shed wake induces a lag between the effective angle of attack and the geometric angle of attack. As the unsteady lift force is perpendicular to the chord line, i.e., α_E , it has a component in the drag direction defined by the geometric angle of attack. The induced drag is given by:

$$\Delta C_D^{ind} = (\alpha - \alpha_E) C_L^{dyn} \quad (1.66)$$

Viscous drag: Viscous drag originates from the boundary layer and contributes almost steadily to the total drag, with little variation in angle of attack. When trailing edge separation occurs, there is a significant increase in viscous drag due to pressure drag increase. The B-L model determines the unsteadiness of pressure drag due to variations in the trailing edge separation point from the analytical expression for pressure drag on a flat plate in a potential Kirchhoff flow:

$$C_D^{Kirchhoff} = \frac{dC_L}{d\alpha} \alpha^2 \left(\frac{1 - \sqrt{f}}{2} \right)^2 \quad (1.67)$$

However, Beddoes and Leishman have noted that their approach is not consistent with experimental tests, except for minor separations. In Risø model, it is proposed that the factor $(1 - \sqrt{f})^2/4$ characterizes the relative variation of pressure drag resulting from changes in the separation point. The contribution of unsteadiness to the overall drag, due to the unsteady separation point f'' lagging behind the separation point for the effective angle of attack, is assumed to be expressed as:

$$\Delta C_D^{f''} = (C_D^{st}(\alpha_E) - C_{D0}) \left(\left(\frac{1 - \sqrt{f''}}{2} \right)^2 - \left(\frac{1 - \sqrt{f^{st}(\alpha_E)}}{2} \right)^2 \right) \quad (1.68)$$

Here, the term $C_D^{st}(\alpha_E) - C_{D0}$ replaces the lift slope in the original Kirchhoff's expression, and it characterizes the magnitude of the pressure drag. The total dynamic drag is given by:

$$C_D^{dyn} = C_D^{st}(\alpha_E) + \Delta C_D^{ind} + \Delta C_D^{f''} \quad (1.69)$$

Where the first term in the equation ensures that the unsteady drag is constrained by the static drag curve C_D^{st} , while the remaining two terms describe the unsteadiness of the induced drag and pressure drag, respectively.

1

PITCHING MOMENT

The unsteady pitching moment model shares similarities with the original B-L model, where the moment is affected by unsteady TE separation causing the pressure center to travel. However, similar to the drag, this model constrains the unsteady moment to variations about the static moment curve provided as input. The unsteady moment coefficient is calculated by:

$$C_M^{dyn} = C_M^{st}(\alpha_E) + \Delta C_M^{f''} - \pi C \left(\frac{d\alpha}{dt} \right) \quad (1.70)$$

Where $\Delta C_M^{f''}$ denotes the unsteady moment resulting from dynamic trailing edge separation, and the last term accounts for the added mass effect that results from the pitch rate of the airfoil. To model the effect of the trailing edge separation on the pitching moment, similar to the B-L model, the arm of the lift force, i.e., the distance between the pressure center and the quarter-chord position, which is depend to the trailing edge separation, is calculated by:

$$X_{CP,L}(\alpha) = \frac{C_M^{st} - C_{M0}}{C_L^{st}} \quad (1.71)$$

Note that this equation can be used up to angle of attack of 35°. Considering the above equation, it is assumed that the unsteady moment coefficient due to the dynamic variations of the separation point can be expressed as:

$$\Delta C_M^{f''} = C_L^{dyn} \left(X_{CP,L}(f'') - X_{CP,L}(f^{st}(\alpha_E)) \right) \quad (1.72)$$

ØYE MODEL

The Øye dynamic stall model, developed in 1991 for wind turbine applications [9], provides a simplified representation of unsteady aerodynamic behavior during stall onset. It employs a first-order Ordinary Differential Equation (ODE) to define a lift interpolation factor f , which enables a continuous transition between the lift characteristics of fully attached and fully separated flow regimes.

The interpolation factor f ranges from a value of 1 under fully attached flow conditions and decreases to 0 when the flow becomes fully separated. Øye introduced a critical angle of attack, denoted α_{Sep} , beyond which the flow is considered fully separated and the interpolation factor is set to zero. The lift curve for attached flow is modeled as a linear function with slope $(dC_L/d\alpha)_{\alpha_0}$, while the separated flow is approximated with a reduced slope of $1/2(dC_L/d\alpha)_{\alpha_0}$. At the separation angle α_{Sep} , the slope is further reduced to $1/12(dC_L/d\alpha)_{\alpha_0}$, and a Hermite interpolation method is applied to ensure smooth blending

between the two flow regimes. The time-dependent behavior of the dynamic interpolation factor is described by the following ODE:

$$\dot{f}_{dyn} = \frac{f_{st} - f_{dyn}}{T_f} \quad (1.73)$$

where the time constant T_f is defined as:

$$T_f = \frac{\epsilon C}{2V} \quad (1.74)$$

with C representing the airfoil chord length, V the freestream velocity, and ϵ model parameter. The quasi-steady interpolation factor f_{st} is computed as:

$$f_{st} = \frac{C_L(\alpha) - C_{L,Sep}(\alpha)}{C_{L,att}(\alpha) - C_{L,Sep}(\alpha)} \quad (1.75)$$

where $C_{L,att}(\alpha)$ and $C_{L,Sep}(\alpha)$ denote the lift coefficients for fully attached and fully separated flow conditions, respectively [106]. The resulting dynamic lift coefficient is given by:

$$C_L^{dyn} = f_{dyn} \cdot C_{L,att}(\alpha) + (1 - f_{dyn}) \cdot C_{L,Sep}(\alpha) \quad (1.76)$$

This model is characterized by its low computational complexity and minimal parameter requirements, making it suitable for preliminary aerodynamic analyses, control-oriented simulations, and low-fidelity aeroelastic modeling. However, it does not account for the dynamic evolution of the pitching moment coefficient C_M or the drag coefficient C_D , which significantly limits its capability to capture the full spectrum of unsteady aerodynamic phenomena. Consequently, the Øye model may not be adequate for applications requiring accurate prediction of aerodynamic damping, energy dissipation, or control surface loading under dynamic stall conditions.

ONERA MODEL

The Onera dynamic stall model is a semi-empirical formulation originally developed by Tran and Petot in 1981 for helicopter applications [10]. It comprises two differential equations: the first models the inviscid attached flow using a first-order linear equation, while the second captures the nonlinear viscous behavior associated with stall through a second-order equation. This model was developed further by Tran, Petot, McAlister, Dat, Lambert, and Peters [107, 108], with further revisions by Petot [109] in later years.

The model is derived by linearizing a system of nonlinear equations containing higher-order terms and derivatives. This linearization assumes small deviations between steady and dynamic lift coefficients and minor variations in angle of attack. While these assumptions

simplify the model, they may limit its accuracy for wind turbine applications, where large unsteady aerodynamic effects are common [87, 92]. Additionally, the model requires empirical coefficients obtained from unsteady wind tunnel experiments, which must be tailored to specific airfoils and flow conditions. The governing equations of the model [92] are expressed as follows:

$$\dot{C}_{L1} + \lambda_L C_{L1} = \lambda_L C_{L,pot} + (\lambda_L s_L + \sigma_L) \dot{\alpha} + s_L \ddot{\alpha} \quad (1.77)$$

$$\ddot{C}_{L2} + a_L \dot{C}_{L2} + r_L C_{L2} = -r_L (\Delta C_L + e_L \Delta \dot{C}_L) \quad (1.78)$$

The dynamic lift coefficient is calculated by:

$$C_L^{dyn} = C_{L1} + C_{L2} \quad (1.79)$$

Where:

$$a_L = \alpha_0 + \alpha_2 (\Delta C_L)^2 \quad (1.80)$$

$$(1.81)$$

$$r_L = [r_0 + r_2 (\Delta C_L)^2]^2 \quad (1.82)$$

$$(1.83)$$

$$e_L = e_2 (\Delta C_L)^2 \quad (1.84)$$

The dot operator denotes differentiation with respect to non-dimensional time s . The coefficients $\lambda_L, s_L, \sigma_L, r_0, r_2, \alpha_0, \alpha_2, e_2$ are functions of airfoil geometry and angle of attack, and are obtained from experimental data. The difference between inviscid and steady-state lift coefficients is defined as:

$$\Delta C_L = C_{L,pot} - C_{L,st} \quad (1.85)$$

It is important to note that the Onera model primarily predicts the dynamic lift coefficient. It does not directly compute the dynamic drag coefficient or pitching moment coefficient.

SNEL MODEL

The Snel model is a second-order nonlinear dynamic stall formulation developed by Snel [12] specifically for modeling unsteady aerodynamics in wind turbines. It is a semi-empirical model that achieves reasonable accuracy without relying on airfoil-specific parameters, which are typically obtained through costly wind tunnel experiments. The

model builds upon the conceptual basis introduced by Truong [110], who suggested that the dynamic lift coefficient C_L^{dyn} should be expressed as a combination of the static lift response, a first-order frequency-dependent correction, and a second-order term representing self-excited higher-frequency dynamics. The dynamic lift coefficient is computed using [92]:

$$C_L^{dyn} = C_{L,st} + \Delta C_{L1} + \Delta C_{L2} \quad (1.86)$$

The first-order correction ΔC_{L1} is modeled using the SIMPLE approach [111] and is governed by:

$$C_{f11}\Delta\dot{C}_{L1} + C_{f10}\Delta C_{L1} = f_{i1} \quad (1.87)$$

where f_{i1} is the forcing term and C_{f11} is the time constant defined as:

$$\tau = \frac{C}{2V} \quad (1.88)$$

The forcing term is given by:

$$f_{i1} = \tau \Delta \dot{C}_{L,pot} \quad (1.89)$$

with

$$\Delta C_{L,pot} = C_{L,pot} - C_{L,st} = 2\pi \sin(\alpha - \alpha_0) - C_{L,st} \quad (1.90)$$

The stiffness-like coefficient C_{f10} is defined conditionally as:

$$C_{f10} = \begin{cases} \frac{1+0.5\Delta C_{L,pot}}{8(1+60\tau\dot{\alpha})}, & \text{if } \dot{\alpha}\Delta C_{L,pot} \leq 0 \\ \frac{1+0.5\Delta C_{L,pot}}{8(1+80\tau\dot{\alpha})}, & \text{if } \dot{\alpha}\Delta C_{L,pot} > 0 \end{cases} \quad (1.91)$$

The second-order correction ΔC_{L2} is governed by:

$$\tau^2 \Delta \ddot{C}_{L2} + C_{f21} \Delta \dot{C}_{L2} + C_{f20} \Delta C_{L2} = f_{i2} \quad (1.92)$$

where

$$C_{f20} = k_s^2 [1 + 3(\Delta C_{L2})^2] [1 + 3\dot{\alpha}^2] \quad (1.93)$$

$$C_{f21} = \begin{cases} 60\tau k_s [-0.01(\Delta C_{L,pot} - 0.5) + 2(\Delta C_{L2})^2], & \text{if } \dot{\alpha} > 0 \\ 2\tau k_s, & \text{if } \dot{\alpha} \leq 0 \end{cases} \quad (1.94)$$

with $k_s = 0.2$ [110] being the reduced frequency of vortex shedding of the airfoil in the uniform flow. And finally, f_{i2} which is function of $\Delta C_{L,pot}$ and is obtained by:

$$f_{i2} = 0.12 (-0.15\Delta C_{L,pot} + 0.05\Delta \dot{C}_{L,pot}) \quad (1.95)$$

Similar to the Øye and Onera models, the present model does not explicitly simulate the dynamic drag coefficient C_D or the pitching moment coefficient C_M . This limitation restricts its applicability in high-fidelity simulations, such as aeroelastic stability analysis,

gust response modeling, and control system design.

SELECTION OF DYNAMIC STALL MODELS FOR THE PRESENT STUDY

Two dynamic stall models, namely the B-L model and the Risø model, are selected in this study to evaluate their predictive performance against experimental measurements. These two models are chosen because both are capable of predicting the unsteady lift, drag, and pitching moment coefficients under dynamic stall conditions, and they are widely implemented in state of the art commercial aeroelastic simulation tools such as HAWC2 [80], OpenFAST [79], and Bladed [81]. Consequently, a systematic assessment of these models for thick airfoils, representative of modern large-scale wind turbine blades, provides valuable insight into their applicability and limitations. Such an evaluation is expected to contribute to improved dynamic load predictions and, ultimately, more reliable aeroelastic design of next generation wind turbines. The validation of these dynamic stall models are presented in appendix A.

METHODOLOGY

This chapter details the methodology employed to investigate the dynamic stall behavior of thick airfoils in wind turbine applications. It opens with an overview of dynamic stall in large-scale turbines, specifically examining the associated unsteady aerodynamic boundary conditions. The subsequent section evaluates the performance of various dynamic stall models by analyzing them against newly acquired experimental data. The chapter then provides a comprehensive description of the experimental framework, including wind tunnel model design, facility specifications, and instrumentation. Finally, the approach for unsteady data-acquisition are presented, alongside a discussion of the post-processing techniques and validation strategies used to ensure the accuracy and reliability of the measurements.

2.1 OCCURRENCE OF DYNAMIC STALL IN AN EXAMPLE MODERN LARGE-SCALE WIND TURBINE

The aeroelastic analysis of wind turbines plays an important role in ensuring the structural integrity and long-term reliability of modern wind energy systems. This analysis is conducted in accordance with the IEC 61400-1 standard, which specifies the design requirements for wind turbines with the objective of achieving an appropriate level of structural safety and operational performance over the turbine's intended service life.

Wind turbine manufacturers employ this standard to simulate the aeroelastic loads acting on the primary load-bearing components of the turbine, such as the rotor blades, hub, nacelle, tower, and foundation. These simulations are essential for evaluating both ultimate loads, which correspond to extreme events, and fatigue loads, which are indicative of long-term cyclic loading. They are utilized to validate the structural integrity of individual components. The calculated loads incorporate a defined safety level, as prescribed by ISO 2394 [112], ensuring that the design accounts for uncertainties in material properties, environmental conditions, and modeling assumptions. The load types considered in the analysis include gravitational and inertial loads arising from the mass and motion of the turbine components, aerodynamic loads resulting from wind interaction with the rotor and other exposed surfaces, and actuation loads generated by the pitch and yaw mechanisms, generator torque, inverter dynamics, and braking systems. In addition, the turbine is subjected to wake-induced loads, impact and icing loads, tower-blade interaction loads, and vortex-induced vibrations, all of which contribute to the overall aeroelastic loading environment.

In order to fully capture the operational and environmental variability a wind turbine may experience, the design process defines a set of design situations. These represent the most critical and representative conditions over the turbine's lifetime. Each design situation is associated with a set of design load cases, which combine operational modes such as power production, start-up, and shutdown with external conditions like wind turbulence, gusts, directional changes, and electrical faults. The DLCs are categorized into:

- Normal design situations, evaluated under both normal and extreme environmental conditions
- Fault design situations, which simulate the effects of control system failures, grid disconnections, or mechanical malfunctions
- Transportation, installation, and maintenance situations, which account for non-operational configurations and handling loads

Each load case is simulated in conjunction with the turbine's control system behavior, which significantly influences the dynamic response of the structure. The control system's

Table 2.1: Design Load Cases (DLC) of the IEC 61400-1 standard.

Design situation	DLC	Wind condition	Other conditions	Type of analysis	Partial safety factors
1) Power production	1.1	NTM $V_{in} < V_{hub} < V_{out}$	For extrapolation of extreme events	U	N
	1.2	NTM $V_{in} < V_{hub} < V_{out}$		F	*
	1.3	ETM $V_{in} < V_{hub} < V_{out}$		U	N
	1.4	ECD $V_{hub} = V_r \pm 2 \text{ m/s}$, V_r		U	N
	1.5	EWS $V_{in} < V_{hub} < V_{out}$		U	N
2) Power production plus occurrence of-fault	2.1	NTM $V_{in} < V_{hub} < V_{out}$	Normal control system fault or loss of electrical network or primary layer control function fault	U	N
	2.2	NTM $V_{in} < V_{hub} < V_{out}$	Abnormal control system fault or secondary layer protection function related fault	U	A
	2.3	EOG $V_{hub} = V_r \pm 2 \text{ m/s}$, V_{out}	External or internal electrical fault including loss of electrical network	U	A
	2.4	NTM $V_{in} < V_{hub} < V_{out}$	Control system fault, electrical fault or loss of electrical network	F	*
	2.5	NWP $V_{in} < V_{hub} < V_{out}$	Low voltage ride through	U	N
3) Start-up	3.1	NWP $V_{in} < V_{hub} < V_{out}$		F	*
	3.2	EOG $V_{hub} = V_{in}$, $V_r \pm 2 \text{ m/s}$, V_{out}		U	N
	3.3	EDC $V_{hub} = V_{in}$, $V_r \pm 2 \text{ m/s}$, V_{out}		U	N
4) Normal shutdown	4.1	NWP $V_{in} < V_{hub} < V_{out}$		F	*
	4.2	EOG $V_{hub} = V_r \pm 2 \text{ m/s}$, V_{out}		U	N
5) Emergency stop	5.1	NTM $V_{hub} = V_r \pm 2 \text{ m/s}$, V_{out}		U	N
6) Parked (standing still or idling)	6.1	EWM 50-year return period		U	N
	6.2	EWM 50-year return period	Loss of electrical network connection	U	A
	6.3	EWM 1-year return period	Extreme yaw misalignment	U	N
	6.4	NTM $V_{hub} < 0.7V_{ref}$		F	*
7) Parked and fault conditions	7.1	EWM 1-year return period		U	A
8) Transport, assembly, maintenance and repair	8.1	NTM V_{maint} to be stated by the manufacturer		U	N
	8.2	EWM 1-year return period		U	A

Abbreviations of the above table:

DLC: Design Load Case; ECD: Extreme Coherent gust with Direction change; EDC: Extreme Direction Change; EOG: Extreme Operating Gust; ETM: Extreme Turbulence Model; EWM: Extreme Wind speed Model; EWS: Extreme Wind Shear; NTM: Normal Turbulence Model; NWP: Normal Wind Profile model; V_r : Rated wind speed; V_{hub} : Hub wind speed; V_{in} : Cut-in wind speed; V_{out} : Cut-out wind speed; F: Fatigue; U: Ultimate strength; N: Normal; A: Abnormal.

interaction with the aerodynamics and structural dynamics introduces complex feedback mechanisms that must be accurately modeled. This design load cases are shown in table 2.1.

The design load cases defined in the IEC 61400-1 standard provide the operational envelope within which dynamic stall becomes a critical design driver. While the standard ensures a defined safety level for structural integrity, the physical onset of dynamic stall is dictated by the unsteady inflow conditions specified in cases like the Normal Turbulence Model (NTM) and Extreme Turbulence Model (ETM). Note that the standard engineering models such as B-L dynamic stall model, often struggle to predict the "deep stall" seen in large turbines with thick airfoils (>30%), which may lead to simulation inaccuracies in the ultimate loads described in table 2.1.

To determine the unsteady aerodynamic operating range experienced by thick airfoils on large modern wind turbines, the Nordex N175 turbine is taken as the representative case study. The N175 is one of the most recently developed machines in the company's high performance onshore portfolio and features a rotor diameter of 175m together with a nominal rated electrical power of 6.3MW. The turbine operates over a broad inflow range, with cut-in and cut-out wind speeds of 3m/s and 20m/s, respectively, and can be installed on towers of up to 179m hub height. Owing to its large rotor size and design philosophy, the N175 is optimized for low and medium wind regimes, where complex inflow turbulence and significant atmospheric variability can impose challenging aeroelastic loading conditions on the rotor blades. To determine the dynamic stall boundary conditions relevant for thick inboard airfoil sections of this turbine, the ALASKA in-house aeroelastic load calculation code was employed. ALASKA simulates the coupled aerodynamic and structural response of all primary load carrying components of a wind turbine, including the rotor blades, hub, nacelle, tower, and foundation, under a wide variety of operational conditions. The software computes both ultimate loads and fatigue loads in compliance with the IEC

Table 2.2: Dynamic stall boundary conditions for four airfoils of the N175 wind turbine.

Airfoil thickness			55%			45%			35%			30%		
Load case	Wind condition	Wind speed	α_m [°]	A [°]	k [-]	α_m [°]	A [°]	k [-]	α_m [°]	A [°]	k [-]	α_m [°]	A [°]	k [-]
1.1-1.2	NTM	$V_{in}+2$	12.76	15.54	0.34	8.06	11.64	0.34	5.35	8.52	0.33	4.55	6.51	0.29
		V_r-2	14.44	15.18	0.20	9.19	10.89	0.20	6.20	7.72	0.20	5.30	5.81	0.17
		V_r	15.90	13.93	0.15	10.46	11.16	0.15	7.14	8.44	0.15	5.81	6.58	0.13
		V_r+2	17.53	15.25	0.12	11.41	12.39	0.12	7.29	9.38	0.12	5.16	7.59	0.11
		V_{out}	22.04	20.96	0.08	16.02	18.67	0.08	10.03	15.28	0.08	5.19	12.12	0.07
1.3	ETM	V_r	16.92	23.14	0.15	10.67	17.28	0.15	7.01	12.87	0.15	5.37	9.99	0.13
		V_{out}	21.15	33.13	0.09	14.78	27.90	0.09	8.93	22.14	0.09	4.36	17.18	0.08
1.5	ETM	V_r	18.28	11.79	0.13	11.42	7.85	0.13	7.29	5.20	0.13	5.37	3.90	0.11
		V_{out}	22.80	19.76	0.07	15.48	15.34	0.07	9.05	10.74	0.07	4.37	7.44	0.06
2.1	NTM	V_{out}	12.90	29.06	0.12	8.35	24.41	0.12	4.31	19.50	0.11	1.51	15.89	0.10

61400-1 design standard and incorporates safety factors and reliability criteria consistent with ISO 2394. The code has been extensively validated through comparisons with field measurements from multiple Nordex prototype turbines, ensuring the reliability of its aeroelastic predictions. Using ALASKA, a comprehensive set of DLCs was evaluated for the N175, focusing on those load scenarios most relevant to the onset of unsteady, stall dominated aerodynamic behavior. Table 2.2 presents the subset of DLCs that are particularly significant for investigating dynamic stall phenomena, especially for airfoil sections with high relative thickness (30–55%). It is important to note that α_m , A , and k denote the mean angle of attack, amplitude of oscillation, and reduced frequency, respectively.

As shown in table 2.2, the inboard sections of the N175 turbine (55% and 45% thickness) are subjected to high mean angles of attack and large oscillation amplitudes. Under DLC 1.3 (ETM) at cut-out wind speed, the amplitude can reach as high as 33° . The boundary conditions presented in table 2.2 reveal that the N175 turbine blades are subjected to aerodynamic environments that frequently exceed static stall limits, particularly in the inboard regions. In DLC 1.3 (ETM) at V_{out} , the combination of a mean angle of attack of $\alpha_m=21.15^\circ$ and an oscillation amplitude of 33.13° for the 55% thickness airfoil indicates that this section of the blade reaches a significant high angle of attack in the deep stall regime. Under these conditions, the possible formation and subsequent shedding of a dynamic stall vortex could result in highly impulsive aerodynamic loads at the root the blade. Furthermore, when these fluctuations occur at high reduced frequencies, the flow cannot remain attached, leading to a very high lift and moment overshoots.

The variation in reduced frequency, which reaches values as high as 0.34 at $V_{in}+2$, indicates that the aerodynamic response is highly non-stationary. As noted by Holierhoek et al. [92], high reduced frequencies significantly increase the area of the lift hysteresis loop, which can introduce negative aerodynamic damping into the system. The negative damping causes instability in the edgewise and flapwise vibration modes of the blade [113]. Furthermore, the results show that the mean angle of attack, the amplitude of oscillation and the reduced frequency are reduced with decreasing the airfoil thickness.

Overall, the results demonstrate that dynamic stall is a critical aerodynamic driver within the operational envelope of the large wind turbines, particularly under NTM and ETM conditions. Under NTM (DLC 1.1–1.2), the presence of stochastic turbulence triggers continuous fluctuations in the effective angle of attack, which can periodically push inboard sections beyond their static stall limits, especially during transient yaw misalignments or shear-driven disturbances. Under ETM (DLC 1.3), the intensity of turbulence and gustiness is substantially higher, resulting in large amplitude, broadband inflow fluctuations that greatly increase the likelihood of deep stall, vortex shedding, and moment overshoots. These ETM driven unsteady inflow conditions represent some of the most severe aerodynamic environments faced by modern utility scale turbines. In both NTM and ETM scenarios, the combination of highly flexible blades, thick inboard airfoil sections, and atmospheric turbulence produces persistent and repeated incursions into the dynamic stall regime, making dynamic stall not merely an isolated event but a recurrent and structurally

significant phenomenon across normal operational conditions. This reinforces the need for accurate modeling and careful design calibration to ensure robust aeroelastic performance and structural integrity throughout the turbine's lifetime.

2.2 EXPERIMENTAL SETUP

The experiments were carried out in the Low-Turbulence Tunnel (LTT), a closed-return, low-speed wind tunnel facility at the Delft University of Technology [114]. This wind tunnel is designed to provide a highly uniform and stable flow environment for detailed aerodynamic investigations. The test section has dimensions of $1250 \times 1800 \times 2600$ mm. The flow is accelerated into the test section through a nozzle with a contraction ratio of 17.8 : 1, which produces a low free-stream turbulence intensity of 0.07% at a reference velocity of 75 m/s. Such low-turbulence levels are essential for accurately resolving small-scale flow structures and for minimizing external disturbances that could influence the unsteady aerodynamic behavior of the model. The wind tunnel is capable of achieving a maximum flow speed of 120 m/s in the empty test section. The flow is driven by a 525 kW variable-speed electric motor powering a six-bladed fan, which enables precise control of the operating velocity across the entire range used in this study. To maintain stable thermal conditions throughout the experiments, a heat exchanger is installed in the settling chamber. This system regulates the air temperature inside the wind tunnel, thereby preventing thermal drift that could affect the density, the dynamic pressure, or the performance of optical measurement techniques such as PIV.

The aerodynamic experiments were conducted on two thick airfoil configurations, designated X-35-02 and X-FB-35-10, at a Reynolds number of $Re = 1.0 \times 10^6$. Both airfoils possess a maximum relative thickness of 35%, which is located at 32% of the chord length, a leading edge radius of 5%, and trailing edge thicknesses of 2% and 10%, respectively. The second model, X-FB-35-10, represents the flatback variant of the baseline X-35-02 geometry. It maintains the same camber line shape and maximum thickness location as the baseline airfoil, but incorporates additional thickness downstream of the maximum relative thickness, gradually increasing the trailing edge width to achieve the flatback configuration.

Each airfoil model has a chord length of 400 mm and a span of 1248 mm. This geometry results in a blockage ratio of approximately 11% at an angle of attack of 30° , which remained within acceptable limits for the corrections applied in this study. The models were CNC-milled from aluminium to ensure high geometric accuracy and structural rigidity, and were mounted vertically in the test section of the wind tunnel. Figure 2.1 presents the test section of the wind tunnel. The X-35-02 and X-FB-35-10 models were instrumented with 90 and 92 static pressure taps, respectively. The taps were distributed along the upper and lower surfaces with increased density near the leading and trailing edges, allowing for accurate

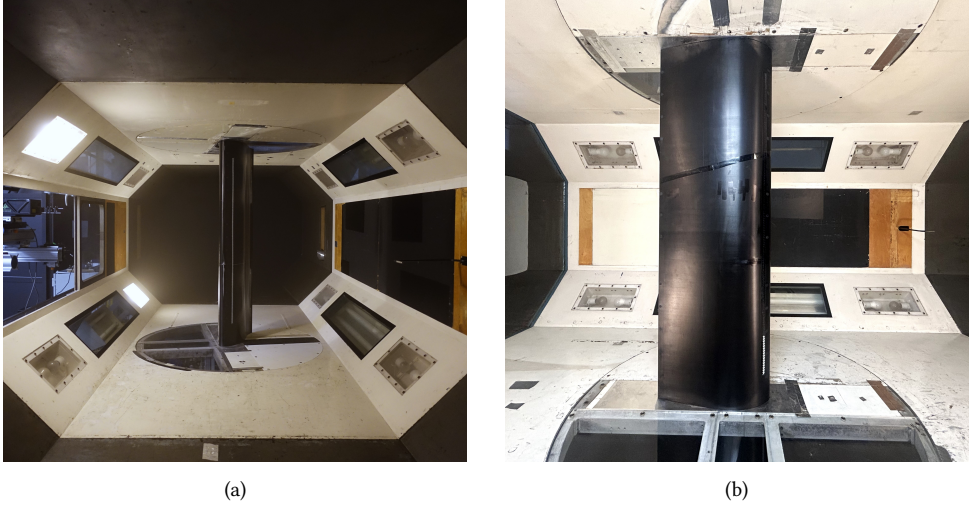


Figure 2.1: Wind tunnel setup.

resolution of steep pressure gradients in these critical regions. The pressure measurement system thereby enabled detailed reconstruction of the surface pressure distribution over a wide range of flow conditions. Furthermore, to simulate leading edge contamination and enforce controlled boundary layer transition, zig-zag trip tapes were applied on the models. The trip configuration consisted of tapes with a thickness of 0.4 mm and a width of 12 mm, placed at 5% of the chord on the suction side and at 10% of the chord on the pressure side. The selected tape geometry yields a roughness Reynolds number of $R_{kk} = 1 \times 10^3$, which is sufficient to trigger transition reliably under the operating conditions of the experiment. The geometries of both airfoils are illustrated in figure 2.2 (a).

A series of single-row VGs strips, produced via plastic injection molding, were installed at various chordwise locations on the suction side of the airfoils. Each strip consisted of counter-rotating, delta-shaped vane pairs designed to generate streamwise vortices and thereby enhance boundary layer momentum near the surface. The vanes had a total height of 5mm, corresponding to a roughness Reynolds number of $R_{kk} = 12.5 \times 10^3$ under the operating conditions of the present study. This height places the VGs well within the conventional range for effective boundary layer energization in low, to moderate Reynolds number wind tunnel testing. The vanes were mounted on a common base plate with a thickness of 1mm. This base plate was attached to the airfoil surface using a 0.05mm double-sided adhesive tape, which ensured sufficient bonding strength while minimizing geometric interference with the near-wall flow. Each vane had a streamwise length of 12mm and was oriented at an incidence angle of 18° relative to the incoming flow. The leading and trailing edges of the base plate were shaped with smooth, rounded contours in order to prevent premature separation or disturbances upstream of the vane locations. The precise geometric parameters of the VGs configuration, including the vane spacing, vane-

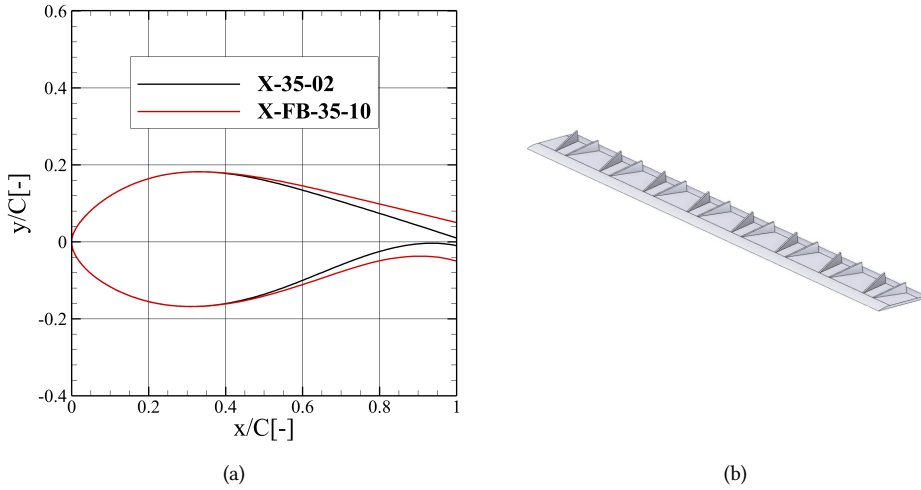


Figure 2.2: Cross-sections of the X-35-02 and X-FB-35-10 airfoils and vortex generator strip.

pair separation, and the detailed shaping of the vane elements, are subject to confidentiality restrictions and therefore cannot be disclosed. Only the essential dimensions required for experimental reproducibility are provided here. An overview of the wind tunnel setup and an example of a VGs strip used in the experiments are shown in figure 2.2 (b).

For static aerodynamic measurements, the angle of attack of the airfoil models was adjusted using a precision turntable installed within the wind-tunnel test section. This setup enabled fine, repeatable adjustments of the inflow incidence angle under steady-state operating conditions. In contrast, a dedicated actuation system was employed for the dynamic experiments. The dynamic pitching mechanism was connected to a shaft positioned at the 1/4-chord location of the models, corresponding to the nominal aerodynamic center. The system was driven by a digitally controlled servomotor, which imposed a prescribed sinusoidal pitching motion on the airfoil. Figure 2.3 shows the pitching mechanism. The mechanism allowed independent control of the mean angle of attack (α_m), oscillation amplitude (A), and actuation frequency (f_q). Dynamic tests were performed at pitching frequencies of 1Hz, 2Hz, and 3Hz, corresponding to reduced frequencies of $k = 0.032$, $k = 0.064$, and $k = 0.096$, respectively. These conditions span a broad range of dynamic responses, enabling the characterization of the airfoil behavior in quasi-unsteady, moderately unsteady, and highly unsteady flow regimes.

Surface pressure measurements were obtained using a multichannel pressure data-acquisition system connected to the pressure taps distributed along the airfoil surface. This system enabled the determination of the pressure coefficient (C_P) and the subsequent computation of the lift (C_L) and pitching moment (C_M) coefficients. A high-speed pressure scanner, capable of sampling all channels at 300Hz, was employed for this purpose. For static measurements, the scanner output was time-averaged to provide steady pressure

distributions. In dynamic experiments, the same sampling frequency was used to resolve temporal variations in the surface pressure field, with data collected over at least 20 full oscillation cycles to ensure statistical convergence and repeatability.

2

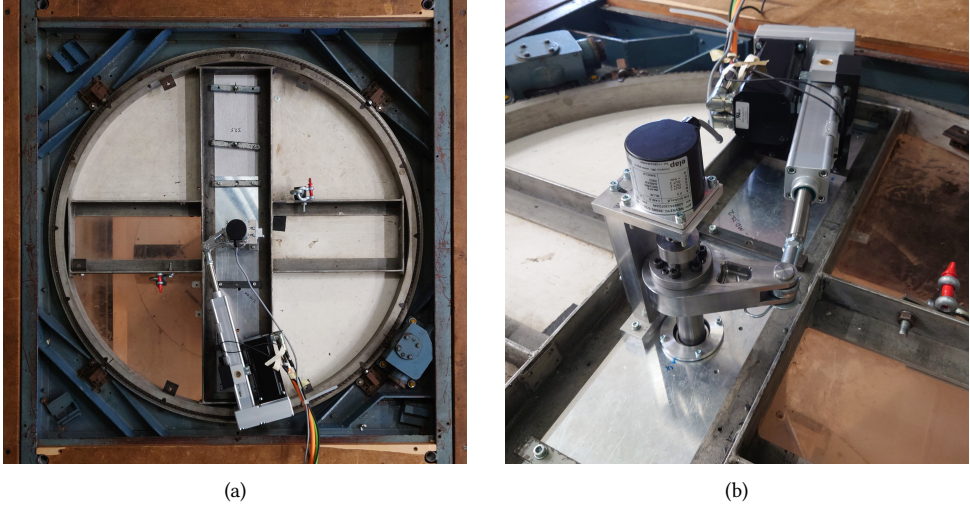


Figure 2.3: Pitching mechanism actuator.

A traversing wake rake equipped with 67 total pressure probes was positioned downstream of the airfoil and connected to the data-acquisition system to determine the aerodynamic drag. This measurement approach was applied exclusively to the X-35-02 airfoil under steady-flow conditions. For separated or unsteady flows, the wake-rake method is known to yield substantial uncertainties due to the high-turbulence levels and strong spatial gradients within the wake [115]. Consequently, its applicability becomes severely limited when large trailing edge separation or vortex shedding is present. In the case of the X-FB-35-10 airfoil, the wake is dominated by a highly turbulent and widened deficit region caused by the flatback trailing edge geometry. As a result, reliable drag estimation using the wake-survey technique was not feasible. Therefore, the drag coefficient for both static and dynamic measurements was instead determined using an integrated surface-pressure method for flows involving separation, unsteady vortex shedding, or thick trailing edge effects. Furthermore, standard wind-tunnel correction procedures were applied to all measured aerodynamic coefficients. These corrections, based on the methodology introduced by Dalton [116], account for solid-blockage, wake blockage, and streamline curvature effects arising from the finite dimensions of the test section. Incorporating these corrections ensures that the reported aerodynamic coefficients represent free-stream conditions and improves the overall fidelity and comparability of the measurements.

As it has been show in section 2.1, the aeroelastic analysis of the reference large wind turbine revealed that the 35% relative thickness airfoil operates within a wide envelope of

Table 2.3: Test matrix of both airfoils for steady conditions.

Airfoil	Reynolds No. [-]	Transition	VGs loc. x/C [-]
X-35-02	1.0E+06	Free/Forced	-
X-35-02	1.0E+06	Free/Forced	0.2, 0.3, 0.4
X-FB-35-10	1.0E+06	Free/Forced	-
X-FB-35-10	1.0E+06	Free/Forced	0.2, 0.3, 0.4

mean angles of attack, oscillation amplitudes, and reduced frequencies. Such conditions reflect the highly unsteady aerodynamic environment experienced by thick airfoils in real atmospheric inflow. However, these operating points cannot be fully reproduced in wind tunnel experiments due to inherent physical and mechanical limitations of the measurement system. In particular, high reduced frequencies lead to significant dynamic inertial loads on the model and pitching mechanism, while the available servomotor power constrains the achievable pitching rates and amplitudes. In addition, the inverse dependence between the achievable Reynolds number and reduced frequency imposes an upper bound on simultaneously matching both parameters in a controlled wind tunnel environment. Despite these constraints, the present study exploits the maximum feasible operational limits of the measurement setup to approximate the widest possible dynamic condition envelope for thick airfoils. This approach ensures that the resulting dataset is both experimentally attainable and representative of the most critical unsteady conditions relevant to modern large-scale wind turbines. The derived reference matrix thus provides a robust foundation for validating computational models and for supporting future aerodynamic and aeroelastic investigations of thick airfoils.

The complete set of operating conditions is summarized in table 2.3 for the static measurements and in table 2.4 for the dynamic cases. In total, 16 static measurements and 192 dynamic measurements were conducted for each airfoil, providing an extensive dataset for analyzing pre-stall, near-stall, and post-stall flow regimes. Owing to limitations in publication space, the present paper reports only a selected subset of these results, while the full dataset is documented in the accompanying thesis. For the dynamic test cases, the mean angles of attack were primarily set to 6° and 10° for both airfoils, enabling consistent comparison across configurations. In addition, the steady-state stall angle was used as a reference point to define near-stall dynamic conditions. Two degrees below the stall angle ($STL-2^\circ$) and five degrees above it ($STL+5^\circ$) were selected as additional mean angles of attack to examine the sensitivity of dynamic stall onset and recovery in the vicinity of the static stall boundary. Moreover, different oscillation amplitudes were prescribed to account for the distinct flow characteristics within the pre-stall and post-stall regimes. For pre-stall dynamic measurements, an oscillation amplitude of 10° was employed to induce sufficiently large variations in incidence while remaining within the attached flow domain. In contrast, a reduced amplitude of 5° was chosen for the post-stall cases to maintain stable

Table 2.4: Test matrix of both airfoils in the dynamic conditions.

Airfoil	Reynolds No. [-]	Transition	VGs loc. x/C [-]	Mean AOA α_m [°]	Amplitude A [°]	Frequency f_q [Hz]	Reduced frequency k [-]
X-35-02	1.0E+06	Free/Forced	-	6°	10°	1, 2, 3	0.032, 0.064, 0.096
X-35-02	1.0E+06	Free/Forced	-	10°	10°	1, 2, 3	0.032, 0.064, 0.096
X-35-02	1.0E+06	Free/Forced	-	STL-2°	10°	1, 2, 3	0.032, 0.064, 0.096
X-35-02	1.0E+06	Free/Forced	-	STL+5°	5°	1, 2, 3	0.032, 0.064, 0.096
X-35-02	1.0E+06	Free/Forced	0.2, 0.3, 0.4	6°	10°	1, 2, 3	0.032, 0.064, 0.096
X-35-02	1.0E+06	Free/Forced	0.2, 0.3, 0.4	10°	10°	1, 2, 3	0.032, 0.064, 0.096
X-35-02	1.0E+06	Free/Forced	0.2, 0.3, 0.4	STL-2°	10°	1, 2, 3	0.032, 0.064, 0.096
X-35-02	1.0E+06	Free/Forced	0.2, 0.3, 0.4	STL+5°	5°	1, 2, 3	0.032, 0.064, 0.096
X-FB-35-10	1.0E+06	Free/Forced	-	6°	10°	1, 2, 3	0.032, 0.064, 0.096
X-FB-35-10	1.0E+06	Free/Forced	-	10°	10°	1, 2, 3	0.032, 0.064, 0.096
X-FB-35-10	1.0E+06	Free/Forced	-	STL-2°	10°	1, 2, 3	0.032, 0.064, 0.096
X-FB-35-10	1.0E+06	Free/Forced	-	STL+5°	5°	1, 2, 3	0.032, 0.064, 0.096
X-FB-35-10	1.0E+06	Free/Forced	0.2, 0.3, 0.4	6°	10°	1, 2, 3	0.032, 0.064, 0.096
X-FB-35-10	1.0E+06	Free/Forced	0.2, 0.3, 0.4	10°	10°	1, 2, 3	0.032, 0.064, 0.096
X-FB-35-10	1.0E+06	Free/Forced	0.2, 0.3, 0.4	STL-2°	10°	1, 2, 3	0.032, 0.064, 0.096
X-FB-35-10	1.0E+06	Free/Forced	0.2, 0.3, 0.4	STL+5°	5°	1, 2, 3	0.032, 0.064, 0.096

actuation and prevent excessive structural loading during deep separation.

PIV measurements were conducted using a stereoscopic PIV setup to resolve the three-component velocity field in a plane around the airfoil. The objective of this setup was to capture detailed off-body flow structures, including separation, reattachment, and vortex dynamics, over a wide range of angles of attack relevant to the unsteady operating conditions examined in this study. The flow within the wind tunnel was seeded with tracer particles consisting of water–glycol droplets with a median diameter of approximately 1 μ m. These droplets were generated using a SAFEX smoke generator, ensuring a homogeneous seeding distribution throughout the measurement domain. Illumination was provided by a Quantel Evergreen Nd:YAG double-pulsed laser system operating at 200mJ pulse energy, 15Hz repetition rate, and a wavelength of 532nm. The laser beam was formed into a light sheet of approximately 2mm thickness on the suction side of the airfoil. The light sheet was introduced through the transparent sidewall of the wind tunnel at mid-span. To minimize specular reflections, particularly from the VGs elements, the VGs array was coated with a matte black paint. Image acquisition was performed using two LaVision Imager sCMOS cameras with a resolution of 2560 $\times$ 2160 pixels, a 16-bit dynamic range, and a pixel pitch of 6.5 μ m. The cameras were mounted beneath the test section at a relative stereoscopic angle, ensuring that both recorded images covered the same Field Of View (FOV) and enabling reconstruction of the out-of-plane velocity component. Each camera was equipped with a 35mm Nikon Nikkor lens operated at an aperture of f/4. Adjustable Scheimpflug adapters were used to maintain an oblique focal plane aligned with the illuminated laser sheet, thus ensuring uniform image sharpness across the measurement plane. This configuration yielded an effective FOV of approximately 300mm $\times$ 280mm.

Vector fields were processed using a multi-pass cross-correlation algorithm, with a

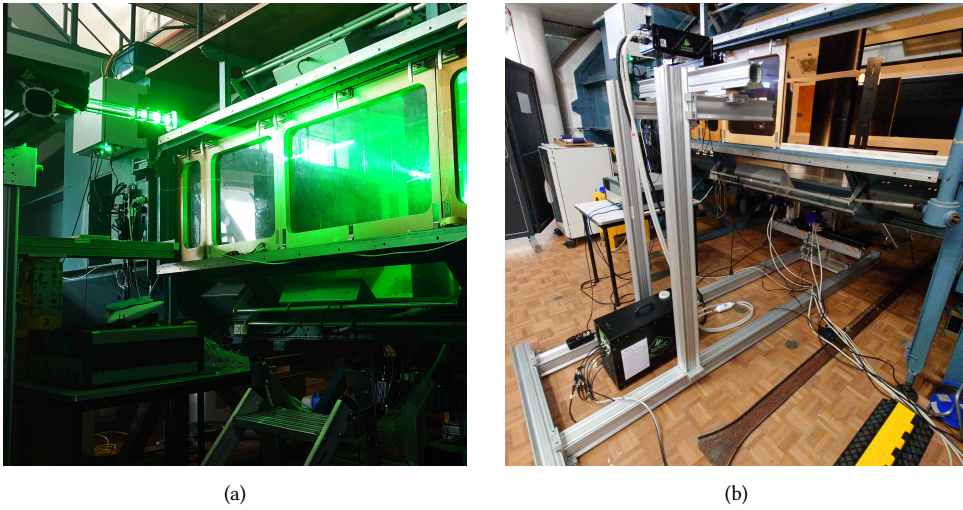


Figure 2.4: PIV measurements setup.

24×24 pixel interrogation window. This multi-pass strategy resulted in a final spatial resolution of approximately 2.9mm in physical space. The acquisition frequency during the experiment was 15Hz in double-frame mode, consistent with the laser repetition rate. A precise geometric calibration was performed using a millimeter-scale target mounted on a rigid foam plate. This calibration plane covered approximately 300mm above the suction side of the airfoil and oriented perpendicular to the airfoil span. The target was used to solve for the mapping functions between image and object coordinates, allowing accurate stereoscopic reconstruction of the three-component velocity vectors. Both cameras were mounted on a traversing platform, allowing the measurement system to be repositioned spanwise while maintaining alignment with the laser sheet. This flexibility permitted detailed mapping of the off-body flow across a range of angles of attack. Due to limitations in the FOV, two chordwise positions were selected for each unsteady measurement case: one encompassing the leading edge region and one covering the trailing edge region. These two measurement windows were arranged such that they overlapped near the airfoil mid-chord, enabling reconstruction of a continuous flow field across the full chord when required. The PIV measurements were acquired in phase-locked mode, using synchronized laser triggering to capture flow fields at specific instants within the pitching motion. For each unsteady operating condition, 250 image pairs were recorded at eight distinct phase angles per oscillation cycle: the upper and lower stagnation points, as well as the $\frac{1}{4}$, $\frac{1}{2}$, and $\frac{3}{4}$ positions during both the US and DS-phases. This strategy ensured adequate statistical convergence of the instantaneous flow fields while enabling detailed analysis of the dynamic stall process. The overall PIV setup is illustrated in figure 2.4.

2.3 UNSTEADY MEASUREMENT CORRECTIONS

2

Accurate interpretation of the unsteady surface pressure measurements requires compensating for the dynamic distortions introduced by the pressure tubing and transducer assembly. Due to the length of the pressure tubes and the associated fluid dynamic response characteristics, a correction procedure was required to compensate for phase lag and amplitude attenuation in the dynamic pressure signals. For this purpose, the method developed by Bergh and Tijdeman [117] was applied. This approach provides a theoretical method for correcting the frequency response of pressure-measurement systems, accounting for tubing compliance, viscous damping, and acoustic resonance effects. The corrected pressure data were subsequently used to compute the aerodynamic coefficients for each airfoil, including the instantaneous pressure coefficient and the derived lift, drag, and moment coefficients. Following this step, standard wind-tunnel correction methods [116] were applied to the aerodynamic coefficients to account for solid blockage, wake blockage, and streamline curvature effects. These corrections help translate the measured forces and moments to equivalent free-stream conditions. It should be noted, however, that the Dalton [116] corrections were originally developed for steady aerodynamic measurements and are not formally validated for unsteady or dynamically varying flow conditions.

Bergh and Tijdeman [117] concluded that varying the frequency of constant fluctuations for a given pressure measurement system with a constant pressure input results in the system's frequency response. Having the frequency response of a system makes it possible to calculate the system's time response to any random input signal. This theory decomposes a pressure measurement system into an N subsection, in which the input pressure of a section is the output pressure of the $N-1$ section. The pressure transfer function for a single pressure measurement system i.e., for every subsystem, is given by:

$$\frac{p_1}{p_0} = \left[\cosh(\varphi L) + \frac{V_v}{V_t} \left(\sigma + \frac{1}{k} \right) n \varphi L \sinh(\varphi L) \right]^{-1} \quad (2.1)$$

$$\varphi = \frac{v}{\alpha_0} \sqrt{\frac{J_0(\alpha)}{J_2(\alpha)}} \sqrt{\frac{\gamma}{n}} \quad (2.2)$$

$$\alpha = i \sqrt{iR} \sqrt{\frac{\rho_s v}{v}} \quad (2.3)$$

$$n = \left(1 + \frac{\gamma - 1}{\gamma} \frac{J_2(\alpha \sqrt{Pr})}{J_0(\alpha \sqrt{Pr})} \right)^{-1} \quad (2.4)$$

Here V_v denotes the pressure transducer volume, V_t is the tube volume, R is the tube radius, L is the length of the tube, i is the imaginary number, J_n is the Bessel function of the first kind of order n , k is the polytropic constant for the volumes, α_0 is the speed of sound. ρ_s is the mean density, α is the shear wave number, μ is the absolute fluid

viscosity, γ is the specific heat ratio, ν is the frequency. Pr is the Prandtl number, σ is the dimensionless volume increase in the pressure transducer due to diaphragm deflection. For several pressure tubes, which are connected in series:

$$\frac{p_N}{p_0} = \frac{p_N}{p_{N-1}} \dots \frac{p_1}{p_0} \quad (2.5)$$

After defining all subsystems, a square-wave step input was used to simulate the system's response in the time-domain, enabling the computation of the overall transfer function following the approach of Lu et al. [115].

To correct the recorded pressure data, introduced by the pressure measurement hardware, the recorded pressure signal is processed using a frequency-domain compensation procedure. In this approach, the entire measurement chain, comprising the pressure tap, tubing, connectors, pressure transducer, and data-acquisition system, is treated as a Linear, Time-Invariant (LTI) system. Under this assumption, the changes imposed by the hardware do not behave like random noise; instead, they act as predictable, frequency-dependent modifications to the signal's amplitude and phase. Because these effects are systematic and repeatable, they can be mathematically undone by applying the inverse of the system's transfer function. This inverse filtering, or deconvolution, restores the signal to what it would have been if the measurement chain had an ideal, flat frequency response.

The process begins with the discretization of the raw recorded pressure signal $x(n)$ at a defined sampling frequency f_s . To maintain the validity of the spectral analysis and prevent aliasing, the sampling frequency is selected to exceed the Nyquist criterion by a sufficient margin. The transition from the time-domain to the frequency-domain is then accomplished using the Fast Fourier Transform (FFT). This transformation is essential because distortions originating from the measurement system are intrinsically frequency-dependent. The FFT produces a set of complex coefficients, $X(k)=FFT(x[n])$, from which the two principal spectral characteristics are extracted:

- **Amplitude Extraction:** The magnitude of each frequency component is obtained as:

$$|X(k)| = \sqrt{\text{Re}[X(k)]^2 + \text{Im}[X(k)]^2} \quad (2.6)$$

providing a measure of the energy distribution over the discrete frequency components. Here, $\text{Re}[X(k)]$ denotes the real part of the coefficient, and $\text{Im}[X(k)]$ denotes the imaginary part.

- **Phase Extraction:** The phase angle $\phi(k)$ is computed using the four-quadrant inverse tangent:

$$\phi(k) = \text{atan2}(\text{Im}[X(k)], \text{Re}[X(k)]) \quad (2.7)$$

which specifies the temporal alignment of the sinusoidal components.

To compensate for the distortions imposed by the measurement chain, a transfer function $H(k)$ is applied. This function characterizes the system's frequency response

and serves as its mathematical inverse during the correction stage. The compensation is achieved through component-wise modification of the amplitude and phase:

- **Amplitude Equalization:** Frequencies attenuated by the system are rescaled using the inverse of the hardware gain, restoring their physical amplitude and producing a flattened effective response.
- **Phase Alignment:** Phase delays or advances introduced by the sensor and electronics are removed by subtracting the system's phase contribution, eliminating temporal displacement and preventing the smearing associated with group delay.

The corrected amplitude $A_{\text{corr}}(k)$ and phase $\phi_{\text{corr}}(k)$ are subsequently recombined into a complex spectrum using Euler's identity:

$$X_{\text{corr}}(k) = A_{\text{corr}}(k) e^{j\phi_{\text{corr}}(k)} \quad (2.8)$$

Finally, the Inverse Fast Fourier Transform (IFFT) is applied to synthesize the corrected frequency-domain data back into a time-domain waveform:

$$x_{\text{corr}}[n] = \text{IFFT}(X_{\text{corr}}(k)) \quad (2.9)$$

The resulting signal provides a high-fidelity representation of the original physical event, effectively decoupled from the transfer characteristics of the measurement equipment. The flowchart of this process is presented in figure 2.5. Figure 2.6 shows the difference between the measured uncorrected pressure and corrected pressure for the phase delay and amplitude on one pressure tap near the trailing edge at the suction side of the X-35-02 airfoil.

Since the connection characteristics, including tube length, cavity volumes, and potential adaptors between the pressure sensors and the pressure-coupling interface within the pressure acquisition system are unknown, equivalent values for the tube length and diameter were defined for this unresolved part of the measurement chain. To verify that these equivalent parameters lead to physically consistent pressure signals, a CFD simulation at 1 Hz was performed in the linear region of the lift curve to validate the dynamic pressure correction methodology. This preliminary step ensured that the correction procedure relied on physically meaningful input parameters. The author strongly recommends incorporating such low-frequency CFD validation, as unsteady measurement correction procedures involve multiple sequential steps, each of which introduces potential sources of uncertainty.

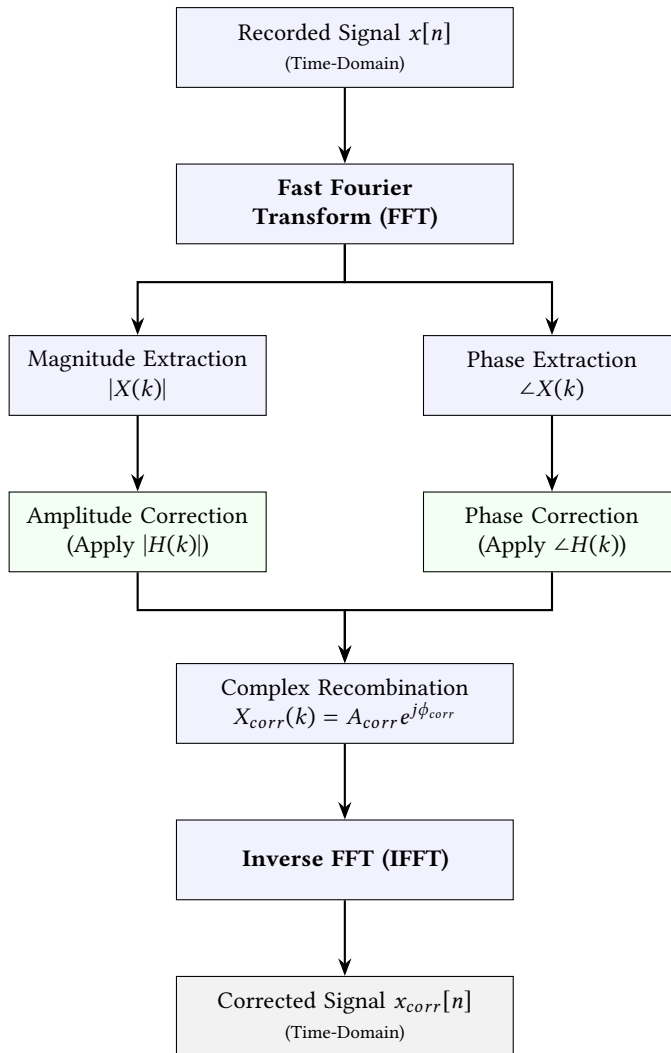


Figure 2.5: Procedural flowchart of the frequency-domain signal compensation process.

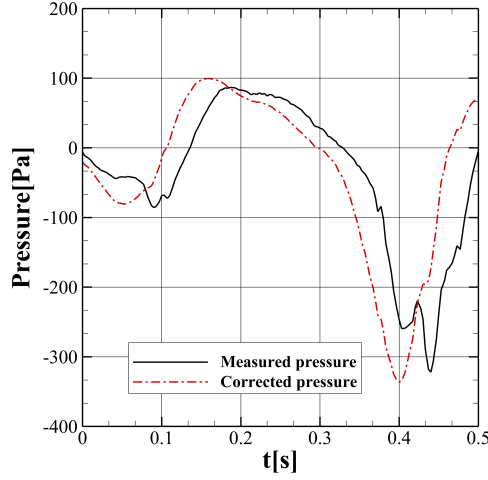


Figure 2.6: Comparison between the measured and the corrected data of a pressure tap, [$Re = 1 \times 10^6$, X-35-02].

2.4 CFD-BASED VALIDATION OF UNSTEADY MEASUREMENT METHODS

To ensure reliable validation results, a high resolution computational mesh was generated using ICEM CFD. An O-grid topology was selected to produce a structured mesh to guaranteeing high mesh quality in the near-wall region. The computational domain was defined as a circular region extending 50 chord lengths from the airfoil to avoid interactions between the far-field boundaries and the local flow. A velocity-inlet boundary condition was prescribed at the upstream boundary, while a pressure-outlet boundary condition was applied at the downstream boundary. The final mesh consisted of 480 cells along the airfoil surface and 204 cells in the radial direction, yielding a total of 97,920 cells. The first cell height was set to 2×10^{-5} m, resulting in a non-dimensional wall distance of approximately $Y^+ \approx 1$, which is sufficient to resolve the viscous sublayer for transitional and unsteady flow conditions.

For the unsteady simulations, a moving-mesh approach was employed to account for the prescribed pitching motion of the airfoil. Particular attention was paid to mesh deformation near the airfoil, as maintaining high element quality during large amplitude oscillations is essential to avoid numerical inaccuracies and artificial dissipation. The mesh configurations at the end of the US-phase and DS-phase positions are shown in figure 2.7, showing the robustness of the deformation strategy throughout the motion cycle.

With the mesh configuration established, the aerodynamic simulations were performed using the commercial solver FLUENT. A transitional GEKO turbulence model [118] was

employed for both RANS and URANS simulations at a Reynolds number of $Re = 1 \times 10^6$ to capture the relevant physical mechanisms associated with dynamic stall. The URANS simulations were carried out using a temporal discretization in which each oscillation cycle was resolved with 2000 uniformly distributed time steps. This temporal resolution was selected to ensure adequate capture of the rapid variations in aerodynamic loading associated with unsteady flow phenomena. For each time step, the solver was allowed to perform up to 100 inner iterations, and convergence was enforced by imposing a strict residual criterion of 1×10^{-8} on the continuity equation. Spatial discretization of the governing equations employed a second-order accurate scheme, to reduce numerical dissipation. Pressure–velocity coupling was achieved through the fully coupled algorithm. Furthermore, the GEKO model was selected because its tunable coefficients allow targeted refinement of separated flow behavior and transitional boundary layer development. Prior to conducting unsteady simulations, the model parameters were calibrated and validated against the corrected steady wind tunnel measurements. As shown in figure 2.8 (a), the model reproduces the linear portion of the lift curve with very good accuracy, although the predicted stall angle of attack is slightly underestimated. Attempts to adjust the parameters to match the measured stall angle resulted in an overprediction of lift in the pre-stall region. Since the maximum angle of attack during the US-phase remained below the steady stall angle, the parameter set providing the most accurate lift behavior near stall, rather than matching the exact stall angle, was selected.

Following the steady validation, the performance of the adjusted GEKO model under dynamic conditions was assessed. Figure 2.8 (b) compares the corrected measured and simulated dynamic lift coefficients of the X-35-02 airfoil at a reduced frequency of $k = 0.032$ (corresponding to 1 Hz) and a mean angle of attack of $\alpha_m = 6^\circ$. The agreement between the

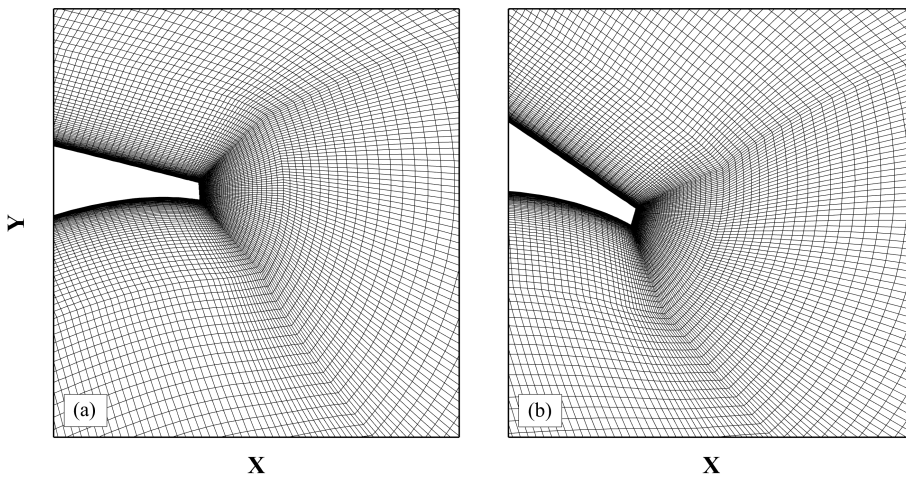


Figure 2.7: Comparison of the dynamic mesh quality of X-35-02 airfoil, [(a): TSA, (b): BSA].

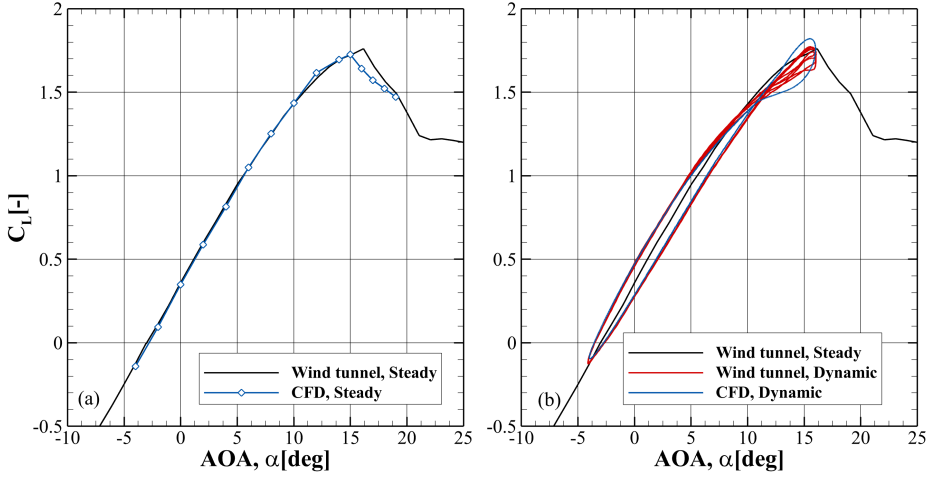


Figure 2.8: Comparison between the measured and the simulation of the lift coefficient, [$Re = 1 \times 10^6$, X-35-02, $\alpha_m = 6^\circ$, $k = 0.032$, (a): Steady, (b): Dynamic].

CFD results and the corrected measurements is very good throughout most of the oscillation cycle. Increased discrepancies appear only near the end of the US-phase, which is attributed to the inherent limitations of the turbulence model as the flow approaches the steady stall condition. Nevertheless, the overall agreement confirms the correct implementation of the dynamic measurement correction procedure and demonstrates that the adjusted GEKO model provides a physically consistent and reliable prediction of the unsteady aerodynamic response of the airfoil.

Part II: Results

3

3

THICK CONVENTIONAL AND FLATBACK AIRFOILS IN STEADY FLOW CONDITIONS

This chapter presents an experimental investigation of two thick wind turbine airfoils, a conventional sharp trailing edge airfoil (X-35-02) and its flatback counterpart (X-FB-35-10), tested in the Low Turbulence Wind Tunnel (LTT) at TU Delft in steady condition. Both airfoils were examined at a Reynolds number of $Re = 1 \times 10^6$ under free and forced transition flow regimes. The influence of trailing edge gap on aerodynamic performance was quantified and discussed. Furthermore, the effect of vortex generator chordwise positioning under varying flow conditions was analyzed to assess its impact on lift, drag, and pressure distribution. The results provide insights into the aerodynamic behavior of thick airfoils in configurations relevant to modern wind turbine applications.

3.1 OBJECTIVE

The primary objective of this chapter is to characterize and compare the steady aerodynamic performance of a conventional and a flatback airfoil under different configurations. These measurements are essential for understanding their behavior in dynamic conditions and providing reference polars for subsequent calibrating of the dynamic stall models discussed later in this thesis. Specifically, steady aerodynamic polars are required to ensure accurate adjustment of the dynamic stall representation. This chapter is organized as follows: first, the clean configurations of both airfoils are tested, and their lift, drag, and pressure coefficients are analyzed. Subsequently, the same airfoils are examined with vortex generators installed under identical conditions. Finally, the key findings are summarized in the concluding section.

3.2 AIRFOILS IN CLEAN CONFIGURATIONS

Steady-state aerodynamic measurements were conducted under both free transition and forced transition flow conditions. Figure 3.1 illustrates the aerodynamic performance comparison between the two airfoils under the aforementioned flow regimes. In the free transition condition, both airfoils exhibit relatively high stall angles of attack, indicating favorable flow attachment characteristics. Notably, the flatback airfoil demonstrates superior performance measures compared to the sharp trailing edge configuration. This improvement can be attributed to the reduction in adverse pressure gradients on the suction side, which results from the increased trailing edge thickness. Consequently, the X-FB-35-10 airfoil achieves a higher stall angle of attack, an increased maximum lift coefficient, and a steeper lift curve slope relative to the X-35-02 airfoil. Furthermore, the flatback design exhibits reduced sensitivity to leading edge roughness, as depicted by the forced transition results, which is a critical advantage for real-world applications where surface contamination is inevitable. Under forced transition conditions, the performance degradation of the X-35-02 airfoil is pronounced. The stall angle of attack decreases dramatically from approximately 16° to 5° , and flow separation on the pressure side initiates at an angle as low as 2° . This leads to an extremely narrow operational range between positive and negative stall angles, severely limiting aerodynamic efficiency. Additionally, heavy flow separation on both sides of the airfoil results in a substantial reduction in lift coefficient compared to the free transition results. These unfavorable characteristics are expected, given the strong pressure gradients present on both sides of the airfoil, a key motivation for the development of flatback geometries. The beneficial effect of increasing trailing edge thickness, and thereby mitigating pressure gradients, is clearly reflected in the performance of the X-FB-35-10 airfoil. By enlarging the trailing edge aft of the maximum thickness

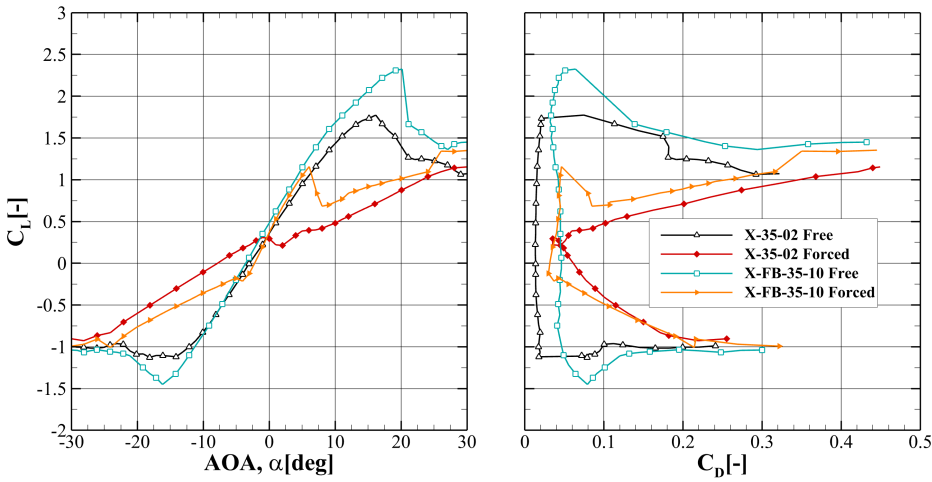


Figure 3.1: Static free and forced transition polar curves, [$Re = 1 \times 10^6$, X-35-02 and X-FB-35-10].

location, the lift coefficient is restored to levels comparable to those observed under free transition conditions. Moreover, the operational range between pressure side and suction side stall angles expands significantly, enhancing overall aerodynamic robustness. Drag polars reveal an increase in drag across a wide angle of attack range for the clean configuration when comparing X-FB-35-10 to X-35-02. However, under forced transition conditions, the flatback airfoil exhibits lower drag than the sharp trailing edge design over a broad range of angles. This finding underscores a critical design trade-off: while trailing edge thickening inherently increases profile drag, the penalty is substantially smaller than the drag increment caused by extensive separation bubbles on both surfaces of the conventional airfoil. Thus, the flatback concept provides a clear aerodynamic advantage under field operational conditions, for the airfoils investigated here.

Under forced transition conditions, both airfoils exhibit a noticeable increase in lift after stall. This post-stall lift increase is mainly due to the formation and persistence of a flow separation bubble on the suction side of the airfoil. The bubble maintains a relatively constant size between the trailing edge and the region of maximum airfoil thickness across a wide range of angles of attack, effectively delaying complete flow detachment and sustaining additional lift. As the angle of attack continues to increase, the pressure distribution and overall flow stability on the pressure side improve. This effect is further enhanced by the pronounced back-camber near the trailing edge, which acts similarly to a trailing edge flap. At higher angles of attack, this flap-like behavior becomes more effective, contributing significantly to the observed lift increase in the post-stall regime. This aerodynamic behavior is commonly observed in thick airfoils specifically designed for wind turbine applications. However, this beneficial effect is not indefinite. Eventually, the separation bubble on the suction side bursts and propagates toward the leading edge,

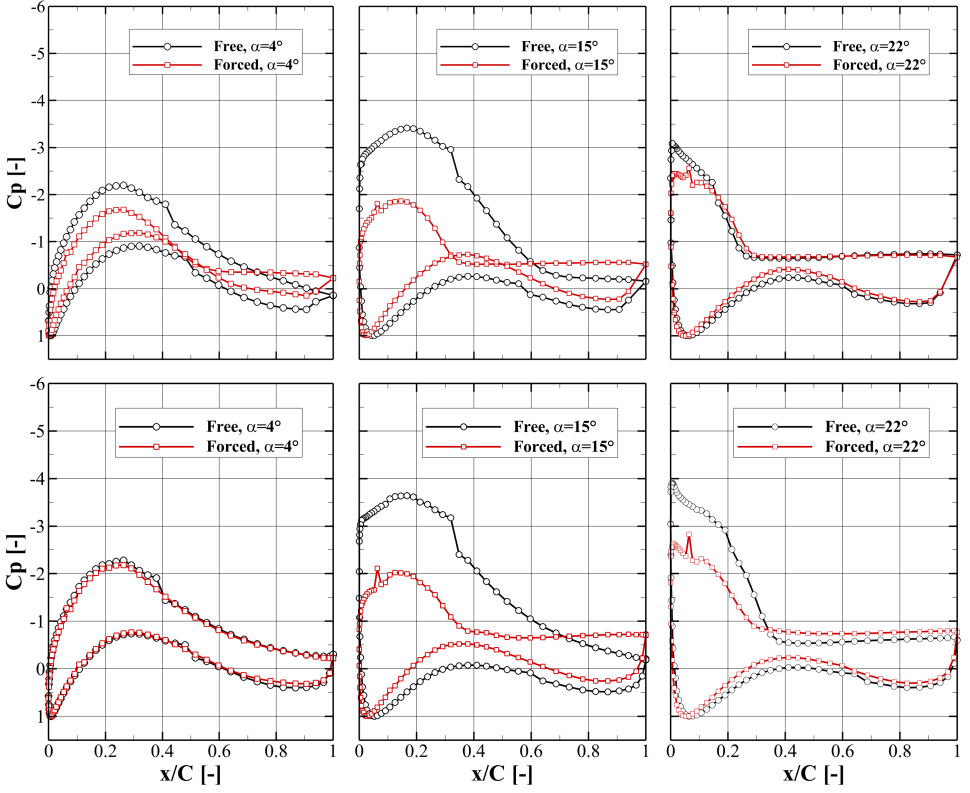


Figure 3.2: Pressure coefficient distribution comparison in free and forced transition flow regimes, [$Re = 1 \times 10^6$, Top: X-35-02, Bottom: X-FB-35-10].

triggering a rapid deterioration in aerodynamic performance. This transition marks the onset of deep stall, characterized by a sharp drop in lift and increased flow instability. On the other hand, this phenomenon is not depicted in the current figure.

Figure 3.2 presents a comparison of the pressure coefficient distributions for the X-35-02 and X-FB-35-10 airfoils under both free transition and forced transition flow regimes at various angles of attack. These pressure profiles provide valuable information into the effect of the trailing edge thickness on the overall aerodynamic behavior of these airfoils.

In the free transition regime, the observed discontinuity in the pressure distribution is associated with the formation of a laminar separation bubble on the suction side of the airfoil. This bubble typically develops when the boundary layer separates under an adverse pressure gradient and subsequently reattaches after the transition to turbulence. The influence of the zig-zag tape, applied to enforce the transition, is clearly visible in the pressure distribution on the suction side. Downstream of the tape location, a distinct pressure increase can be observed, which is related to the abrupt change in the state of the

boundary layer.

Both airfoils show broadly similar aerodynamic behavior under the free transition flow regime. Due to the strong adverse pressure gradients on the airfoil surface and the relatively low Reynolds numbers commonly encountered in airfoil aerodynamics, the laminar boundary layer tends to separate, forming a laminar separation bubble, followed by transition to turbulence and subsequent reattachment. As the angle of attack increases, the chordwise position of this bubble, i.e. the transition point, progressively shifts toward the leading edge. This upstream movement indicates an earlier onset of transition and a growing instability in the boundary layer, which eventually onsets the stall. At low angles of attack, the flow remains largely attached to the airfoil surfaces, ensuring stable aerodynamic performance. However, as the angle of attack approaches approximately 15° , the trailing edge separation begins to form on the suction side of the X-35-02 airfoil. At this stage, the separation is not yet extensive enough to induce full stall. In contrast, the X-FB-35-10 airfoil benefits from its reduced adverse pressure gradient on the suction side, a characteristic clearly visible at $\alpha = 15^\circ$. With further increases of the angle of attack, the trailing edge separation becomes more pronounced on both airfoils, ultimately leading to stall as flow detachment propagates toward the mid-chord region. Under forced transition conditions, the aerodynamic response differs significantly. The X-35-02 airfoil exhibits early flow separation on the suction side at angles as low as $\alpha = 4^\circ$, and this separated region enlarges progressively with increasing the angle of attack. Conversely, the X-FB-35-10 airfoil shows no indication of separation at the same angle, highlighting its superior resistance to premature stall. As expected, the size of the separated flow region on the suction side of both airfoils grows with increasing the angle of attack, eventually dominating the flow-field at high angles. A notable observation is the substantial difference in maximum suction peaks between free transition and forced transition flow regimes at the presented angles of attack. The X-FB-35-10 airfoil consistently exhibits slightly higher suction peaks in both flow regimes, primarily due to its reduced adverse pressure gradient aft of the maximum relative thickness. In addition, the pressure coefficient on the suction side of the flatback airfoil near the trailing edge, just before the pressure recovers to the freestream level, is slightly higher compared to that of the X-35-02 airfoil. This localized pressure behavior reflects the influence of the increased trailing edge thickness, which effectively modifies the pressure recovery characteristics and enhances the overall aerodynamic loading. These characteristics collectively contribute to the superior lift performance of the flatback airfoil, manifested in its higher lift coefficient and steeper lift curve slope relative to the sharp trailing edge configuration.

Figure 3.3 shows the surface flow visualization for the X-35-02 airfoil at an angle of attack of 6° and a Reynolds number of $Re = 1 \times 10^6$ under both free transition and forced transition flow regimes. For reference, the leading edge of the airfoil is positioned on the left side of the image, while the trailing edge is on the right. In the free transition case, the formation of a laminar separation bubble is clearly visible as a bright strip on the suction side of the airfoil. Downstream of this bubble, the flow becomes fully turbulent. It is also

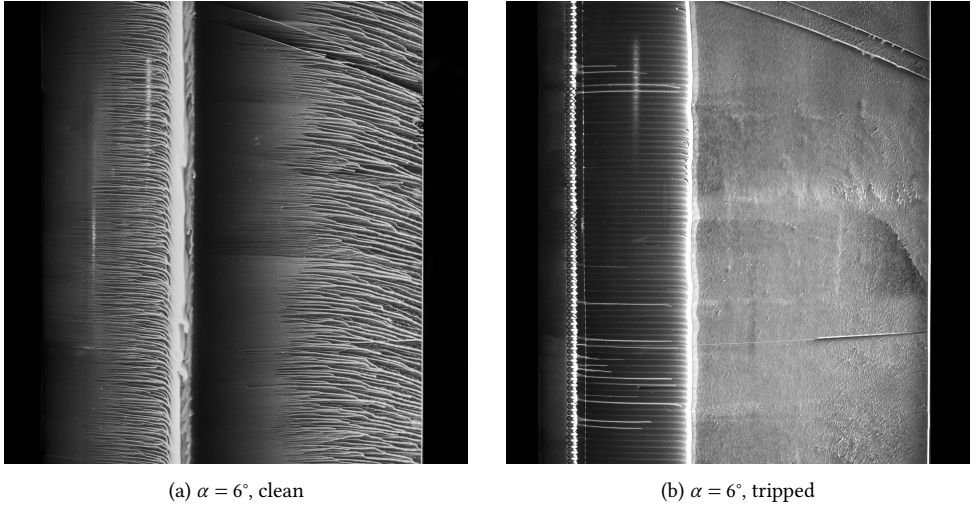


Figure 3.3: Surface flow visualization on the suction side of the airfoil, in free and forced transition conditions, [$Re = 1 \times 10^6$, X-35-02].

worth noting the streaking of surface paint near the trailing edge, caused by gravity and the lack of sufficient flow momentum, as the airfoil models are mounted vertically in the wind tunnel. In contrast, the forced transition case reveals a markedly different flow pattern. The separated region is clearly identifiable and extends from the trailing edge toward the location of maximum airfoil thickness. As discussed earlier, this separated region persists over a wide range of angles of attack and remains significant even at higher incidences, ultimately contributing to the full stall at higher angles of attack.

3.3 AIRFOILS WITH VORTEX GENERATORS

Additional steady-state measurements were performed under the same transition conditions, this time including cases with VGs, in order to establish reference polars for subsequent dynamic analyses and to assess the influence of VGs in conjunction with the geometric differences between the conventional sharp trailing edge and flatback airfoils. On the other hand, the installation of VGs introduces significant momentum into the boundary layer by generating two counter-rotating vortices per vane pair. These vortices energize the near-wall flow and induce a periodic three-dimensional flow structure on the suction side of the airfoil downstream of the VGs rake along the span. This spanwise variation in flow characteristics has a direct impact on the measured drag. Since drag in these steady cases is determined using a wake rake, the recorded drag distribution exhibits a distinct wavy

pattern, reflecting the alternating regions of upwash and downwash created by the vortex pairs. Figure 3.4 illustrates the spanwise drag distribution measured by the wake rake for the X-35-02 airfoil in the free transition regime at an angle of attack of 6° and a Reynolds number of $Re = 1 \times 10^6$, within the attached portion of the lift curve. The VGs array is positioned at 20% of the chord length along the span of the model. The drag minimum occurs at the centerline of each vane pair, where the induced downwash reduces boundary layer thickness and stabilizes the flow. Conversely, the drag maximum is observed between adjacent vane pairs, where the counter-rotating vortices generate upwash, increasing the local boundary layer thickness and, consequently, the wake deficit. For consistency, the average drag value presented in this study for steady cases is computed across four VGs pairs.

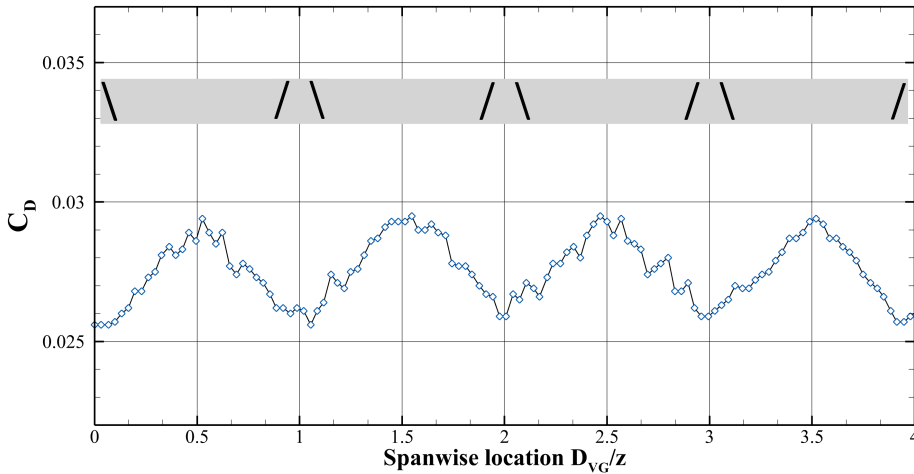


Figure 3.4: Spanwise drag distribution in free transition flow regime, [$Re = 1 \times 10^6$, X-35-02, $\alpha = 6^\circ$].

Figures 3.5 and 3.6 present the aerodynamic characteristics of the X-35-02 and X-FB-35-10 airfoils under free and forced transition flow regimes at $Re = 1 \times 10^6$, for various chordwise positions of the VGs. The figures reveal that, under free transition conditions, positioning the VGs closer to the leading edge generally increases the stall angle of attack, slightly reduces the lift curve slope, and leads to an increase in drag. These trends can be explained by the strength and size of the vortices generated above the airfoil surface. When VGs are installed further downstream, the boundary layer is already thicker, which limits the amount of high-energy flow that the vanes can direct into the boundary layer. Consequently, the vortices formed in these positions are weaker, reducing their ability to counteract adverse pressure gradients at high angles of attack and making the airfoil more susceptible to flow separation. The observed increase in drag is primarily attributed to the form drag of the VGs and earlier transition of the boundary layer to turbulence compared to

the clean configuration. It is important to note that the stall angle of attack for both airfoils remains comparable across different VGs positions; however, the performance improvement achieved by the X-35-02 airfoil is more pronounced than that of the flatback configuration. Additionally, the stall characteristics differ significantly between the two designs. The X-35-02 airfoil exhibits an abrupt stall in VGs-equipped configurations, whereas the flatback airfoil demonstrates a smoother stall progression. This difference is closely related to the pressure gradient distribution on the suction side. Due to its inherently lower adverse pressure gradient, the flatback airfoil experiences a gradual upstream movement of the

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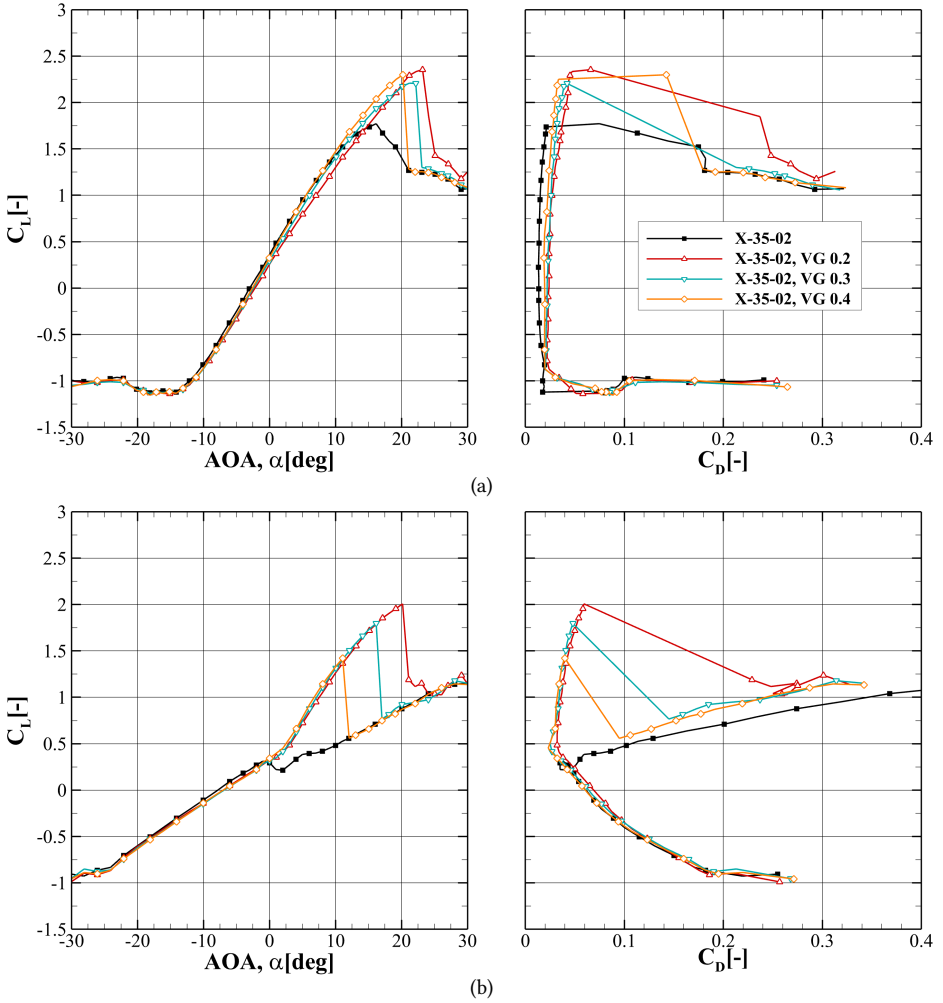


Figure 3.5: Steady polar curves with VGs, [$Re = 1 \times 10^6$, X-35-02, (a): Free transition, (b): Forced transition].

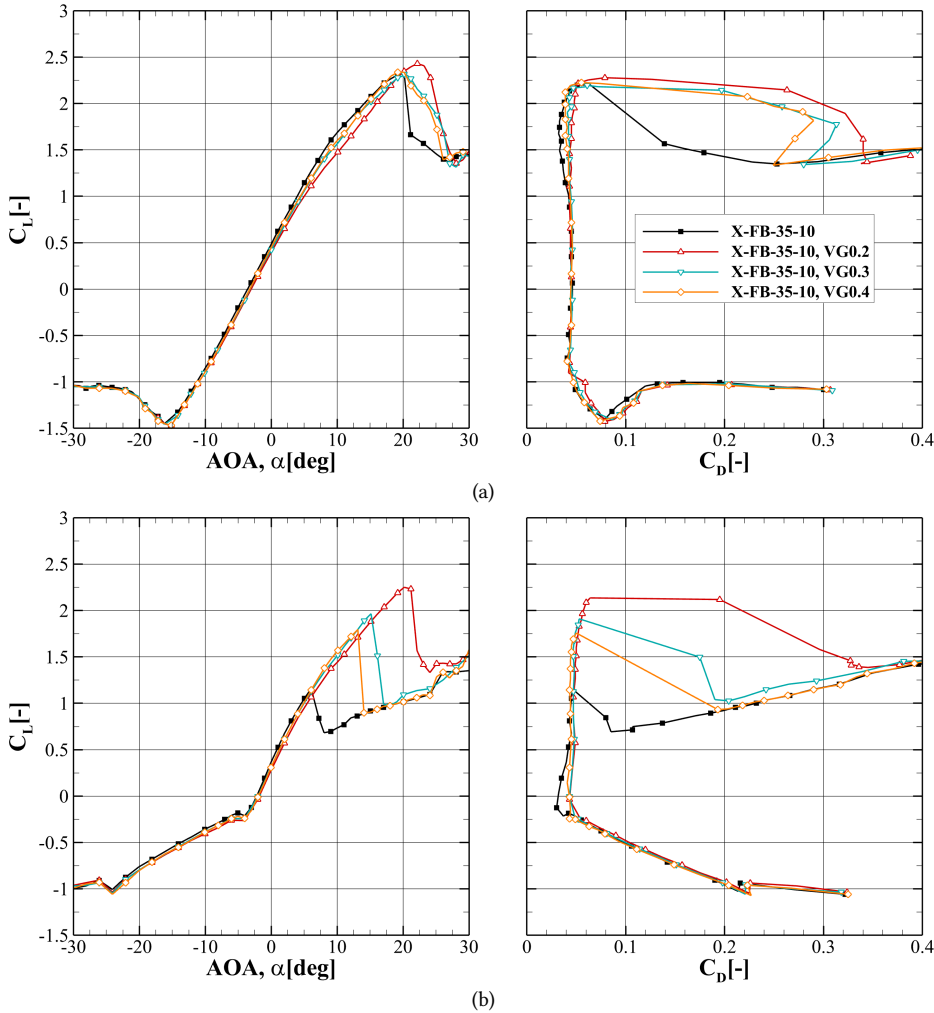


Figure 3.6: Steady polar curves with VGs, [$Re = 1 \times 10^6$, X-FB-35-10, (a): Free transition, (b): Forced transition].

trailing edge separation, resulting in a more progressive stall. In contrast, the X-35-02 airfoil, which naturally has a higher pressure gradient, experiences a more sudden loss of lift. As the VGs lose effectiveness at higher angles of attack, the trailing edge separation rapidly propagates toward the leading edge, causing an abrupt stall. Under forced transition conditions, the aerodynamic benefits of VGs become even more evident. For the X-35-02 airfoil, the addition of VGs significantly increases the lift coefficient, lift curve slope, and stall angle of attack, while simultaneously reducing sensitivity to leading edge roughness compared to the no-VGs configuration. The elimination of large-scale flow separation

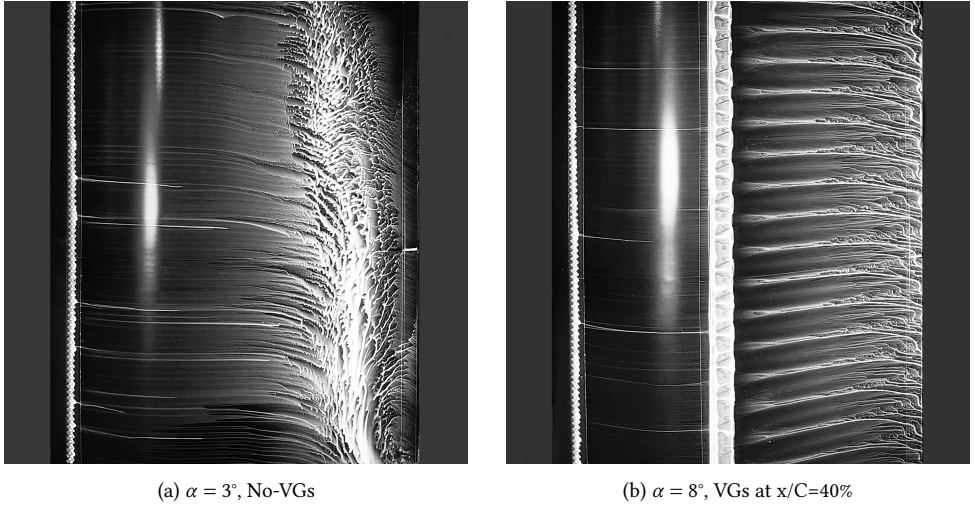


Figure 3.7: Surface flow visualisation on the suction side of the airfoil, in forced transition condition, [$Re = 1 \times 10^6$, X-35-02].

around the airfoil also leads to a substantial reduction in drag relative to the clean case. Because the form drag of this airfoil is relatively high, the influence of VGs position on drag is minimal when the devices are moved closer to the leading edge. A similar trend is observed for the X-FB-35-10 flatback airfoil; however, one notable difference is that the stall angle of attack for the flatback design remains higher than that of the X-35-02 for certain VGs positions under forced transition conditions. This advantage is attributed to the lower adverse pressure gradient on the suction side of the flatback airfoil, which enhances its resistance to flow separation. These findings confirm the superior aerodynamic characteristics of the flatback airfoil, even when combined with VGs, compared to the conventional sharp trailing edge thick airfoil design, which are investigated here. It is also important to highlight that the magnitude of lift loss during stall increases as the VGs are positioned closer to the leading edge in both free and forced transition regimes. This behavior could potentially lead to aerodynamic instabilities in wind turbine operation and should therefore be carefully considered during rotor design. Finally, the drag of the flatback airfoil remains largely unaffected by the presence of VGs in either transition regime, a result of its inherently high form drag, which dominates the overall drag characteristics.

Figure 3.7 illustrates the surface flow visualization for the X-35-02 airfoil at two different angles of attack under forced transition conditions at $Re = 1 \times 10^6$, comparing configurations with and without VGs installed at 40% of the chord length. As in previous visualizations, the leading edge is located on the left side of the image, while the trailing edge is on the right. In the absence of VGs, flow separation is already evident near the trailing edge at an angle of attack as low as $\alpha = 3^\circ$. However, when VGs are installed, the flow remains attached to the airfoil surface even at significantly higher angles of attack, such as $\alpha = 8^\circ$.

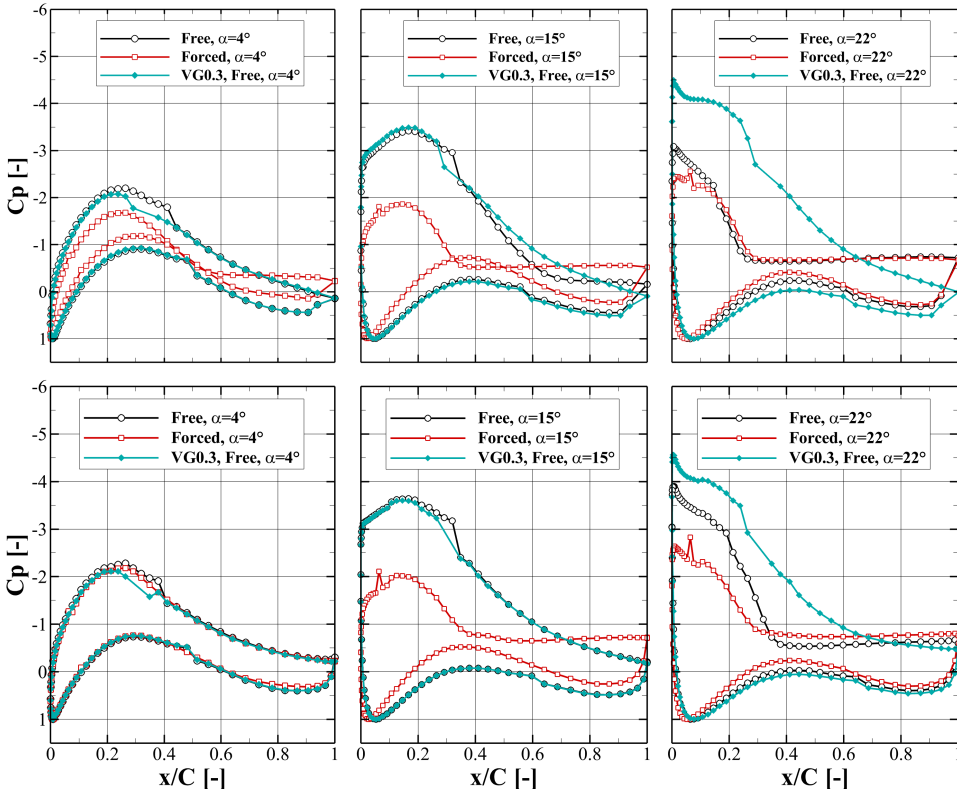


Figure 3.8: Pressure coefficient distribution comparison in free and forced transition flow regimes, $[Re = 1 \times 10^6]$, Top: X-35-02, Bottom: X-FB-35-10, $VGs_{(x/C)} = 0.3$].

Downstream of the VGs array, the surface streamlines associated with the induced vortices are clearly visible, confirming the three-dimensional nature of the flow in this region. It should be noted that the images correspond to the mid-span section of the model, where wall interference effects are minimal. The streaking of surface paint near the trailing edge is primarily due to gravitational effects and the reduced flow momentum in this region, rather than any interaction between the model and the wind tunnel walls.

Figure 3.8 compares the pressure coefficient distributions for the X-35-02 and X-FB-35-10 airfoils under free and forced transition flow regimes in clean configuration with VGs installed at 30% of the chord length under free transition flow. The results are shown for angles of attack of $\alpha = 4^\circ$, $\alpha = 15^\circ$, and $\alpha = 22^\circ$ at a Reynolds number of $Re = 1 \times 10^6$. Similar to the effect of zig-zag tape, the presence of VGs is evident in the pressure distribution as a distinct pressure discontinuity or downstream of the VGs location. The results indicate that the implementation of VGs on the X-35-02 airfoil effectively eliminates the large-scale flow separation and the associated aft pressure plateau. Remarkably, the flow remains

attached up to an angle of attack of $\alpha = 22^\circ$, despite the presence of strong adverse pressure gradients. This behavior explains the abrupt stall characteristics previously discussed, as a high pressure gradients builds up above the airfoil. For the X-FB-35-10 flatback airfoil, the pressure distribution remains largely comparable to that of the free transition regime at angles of attack of $\alpha = 4^\circ$ and $\alpha = 15^\circ$, owing to its inherently lower adverse pressure gradient. At $\alpha = 22^\circ$, a small separation region appears near the trailing edge, caused by leakage of the trailing edge separation into the suction side. However, this separated region grows at a much slower rate compared to the X-35-02 airfoil, resulting in smoother stall characteristics. Although both airfoils share similar geometry up to the point of maximum thickness, the suction peak of the flatback airfoil equipped with VGs is higher than that of the X-35-02 airfoil at $\alpha = 15^\circ$, and $\alpha = 22^\circ$. Additionally, in VGs-equipped configurations, both airfoils exhibit a pressure plateau near the suction peak region as the angle of attack increases, extending up to approximately 25% of the chord length. Overall, the trends observed under steady conditions with VGs are consistent with previous findings reported for thinner airfoils by Timmer et al. [19] and Baldacchino et al. [20] for the X-35-02 airfoil.

3.4 CONCLUSIONS

This chapter has presented the results of a wind tunnel measurement campaign on two thick wind turbine airfoils, a conventional design (X-35-02) and a flatback variant (X-FB-35-10), conducted in the LTT wind tunnel at TU Delft. These airfoils are considered as representative of thick profiles, which are used at the inboard section of large-scale, pitch-regulated, horizontal-axis wind turbines. The study examined the influence of trailing edge thickness, leading edge boundary layer tripping, and VGs chordwise positioning under different flow conditions. The steady aerodynamic measurements reveal that the flatback airfoil (X-FB-35-10) exhibits superior performance compared to the conventional airfoil. Specifically, it achieves a higher stall angle of attack, a greater maximum lift coefficient, a steeper lift curve slope, and reduced sensitivity to leading edge roughness. Increasing the trailing edge thickness mitigates adverse pressure gradients, stabilizes the boundary layer, and enhances resistance to flow separation. The installation of VGs further improves performance by increasing the maximum lift coefficient and stall angle of attack, while reducing drag at high angles of attack and decreasing sensitivity to surface roughness. This improvement is more pronounced for the sharp trailing edge airfoil than for the flatback variant. Notably, the flatback airfoil demonstrates a smooth stall behavior under free transition conditions with VGs, despite its high stall angle of attack. For the sharp trailing edge airfoil, installing VGs under forced transition conditions significantly reduces drag and improves overall aerodynamic performance. In contrast, without VGs, its performance is inadequate and unsuitable for real-world applications; therefore, it is strongly recommended not to use this configuration without vortex generators. Conversely,

adding VGs to the flatback airfoil results in only a negligible drag increase. Overall, the combination of a flatback airfoil with vortex generators provides a robust and efficient solution for thick wind turbine blade sections, outperforming conventional airfoil designs in terms of aerodynamic stability and adaptability to the thinner airfoils.

4

DYNAMIC STALL CHARACTERISTICS OF THICK WIND TURBINE AIRFOILS

4

This chapter explores the dynamic stall characteristics of thick wind turbine airfoils, focusing on both flatback and conventional designs. Two airfoils with a maximum thickness of 35% were selected for the study, featuring trailing edge thicknesses of 2% and 10%, respectively. The experimental campaign was carried out in the wind tunnel using surface pressure measurements on clean and tripped configurations. Tests were performed at a Reynolds number $Re = 1 \times 10^6$ and covered a range of reduced frequencies from 0.032 to 0.096. The analysis examines how trailing edge thickness, boundary layer tripping, mean angle of attack, and reduced frequency influence the dynamic stall behavior of these airfoils.

4.1 OBJECTIVE

As it has been discussed before, previous research has extensively examined the dynamic stall and flow physics of thin airfoils [21]. However, in modern large wind turbines, thicker airfoils are commonly employed toward the mid-span of the blade to address structural and manufacturing constraints. These sections are subjected to significant dynamic loading, making their aerodynamic behavior critical for accurate load prediction and structural integrity. Despite their importance, the current understanding of the dynamic stall characteristics of thick airfoils, particularly the flatback airfoils remains very limited. This knowledge gap poses a serious challenge for wind turbine design, as it can lead to substantial errors in unsteady load calculations, resulting in suboptimal designs and, in extreme cases, structural failures. Therefore, improving the understanding of the unsteady aerodynamic behavior of thick airfoils is essential for reliable load assessment, analysis, and certification of wind turbines. This chapter aims to broaden the knowledge in this field. To achieve this, a dedicated wind tunnel campaign was conducted to study the X-35-02 and its flatback counterpart X-FB-35-10 airfoils with different trailing edge thickness under a range of reduced frequencies and mean angles of attack, covering the linear, pre-stall, stall, and post-stall regions of the lift curve. Both clean and tripped flow conditions were considered to capture the effects of surface roughness. This chapter is organized as follows: first, the influence of mean angle of attack on the dynamic stall characteristics of both airfoils under different flow conditions is analyzed. Next, the evolution of pressure coefficient distributions during the US and DS-phases is discussed for various mean angles of attack. Subsequently, the effect of reduced frequency on the dynamic response of the airfoils is examined. Finally, the main findings are summarized in the concluding section.

4.2 RESULTS AND DISCUSSION

EFFECTS OF THE AIRFOIL GEOMETRY AND MEAN ANGLE OF ATTACK

Figures 4.1 and 4.2 illustrate the dynamic normal force coefficient polars of the X-35-02 and X-FB-35-10 airfoils under both free and forced transition flow regimes. The results correspond to a reduced frequency of $k = 0.064$ and several mean angles of attack. For each airfoil, four representative mean angles of attack were selected to capture distinct aerodynamic regimes, i.e., linear region of the lift curve, upper portion of the linear region, which typically includes the normal operating range of the airfoils, dynamic stall onset region, and post-stall region. Dynamic cycles are distinguished using two line styles: the US-phase, corresponding to the increasing angle of attack, is shown with a solid line,

whereas the DS-phase, corresponding to the decreasing angle of attack, is represented by a dash-dot-dot line. Additionally, shaded bands adjacent to each US and DS curve indicate the standard deviation of the measured cycles, thereby providing insight into the variability and repeatability of the experimental data.

When the flow remains fully attached to both surfaces of the airfoil throughout the dynamic loop, the standard deviation of the normal force coefficient C_N during the US and/or DS-phases is relatively small. This results in well-defined and smooth trajectories in the dynamic C_N polars. In contrast, the formation of separation bubbles on one or both sides of the airfoil introduces significant unsteadiness in the aerodynamic response. The intermittent growth and collapse of these bubbles cause fluctuations in the instantaneous normal force coefficient, leading to an increased standard deviation of C_N during the affected phase(s). Consequently, the dynamic polars exhibit a noisy or scattered appearance in at least one segment of the loop, reflecting the underlying flow instability.

Figure 4.1 (a) shows the variation of the dynamic normal force coefficient at a mean angle of attack of 6° . In the US-phase, both airfoils exhibit a similar trend, where the dynamic C_N remains lower than the corresponding static value. This reduction is primarily related to the lag in pressure adaptation as the flow responds to the increasing angle of attack. Moreover, the X-FB-35-10 airfoil demonstrates consistently higher C_N values and a broader Normal Force Range (NFR) compared to the X-35-02 airfoil. This behavior can be explained by the more favorable pressure gradient distribution over the suction side of the X-FB-35-10 airfoil, which enhances its aerodynamic performance during dynamic stall conditions.

As the airfoils approach the Top Stagnation Angle (TSA), flow separation initiates near the trailing edge on the suction side; however, this separation remains insufficient to trigger full stall. At the onset of the DS-phase, the standard deviation of the dynamic normal force coefficient increases, particularly for the X-35-02 airfoil. This behavior is primarily due to its higher adverse pressure gradients on the suction side and its closer proximity to the static stall angle compared to the X-FB-35-10 airfoil. Under these conditions, the separation bubble expands rapidly, introducing significant flow instabilities and unsteady aerodynamic effects. These adverse phenomena contribute to a reduction in the Mean Dynamic Normal Slope (MDNS) for the X-35-02 airfoil relative to the flatback configuration. During the DS-phase, C_N exceeds the corresponding static polar values as a result of the lag in flow pattern adaptation. As the angle of attack continues to decrease, the separation bubble gradually contracts and eventually vanishes before reaching the Bottom Stagnation Angle (BSA), restoring more stable flow conditions. Consequently, the standard deviation of C_N diminishes. The combined effect of pressure adaptation lag during both the US and DS-phases, without the occurrence of full stall, produces characteristic counter-clockwise hysteresis oval loops.

When the mean angle of attack is increased to 10° , the X-35-02 airfoil exceeds its static stall angle by the end of the US-phase, as shown in figure 4.1 (c). At the TSA, the adverse pressure gradient on the suction side becomes sufficiently strong to cause the trailing edge

separation bubble to burst and propagate toward the leading edge. Unlike the previous case at ($\alpha_m = 6^\circ$), this event results in a pronounced drop in C_N , marking the onset of dynamic stall. As the angle of attack decreases during the DS-phase, the separation bubble gradually contracts and eventually disappears. This recovery is reflected by a reduction in the standard deviation of normal force toward the end of the DS-phase. However, due to the initial size and extent of the separation region, the bubble persists longer than in the lower mean-angle case, maintaining a low dynamic C_N slope over a significant portion of the DS-phase. Before reaching the BSA, the flow reattaches completely, and the dynamic C_N approaches its static counterpart. This behavior is primarily driven by the reduced pitching rate ($d\alpha/dt$) and the diminishing pressure adaptation lag near the BSA. The subsequent

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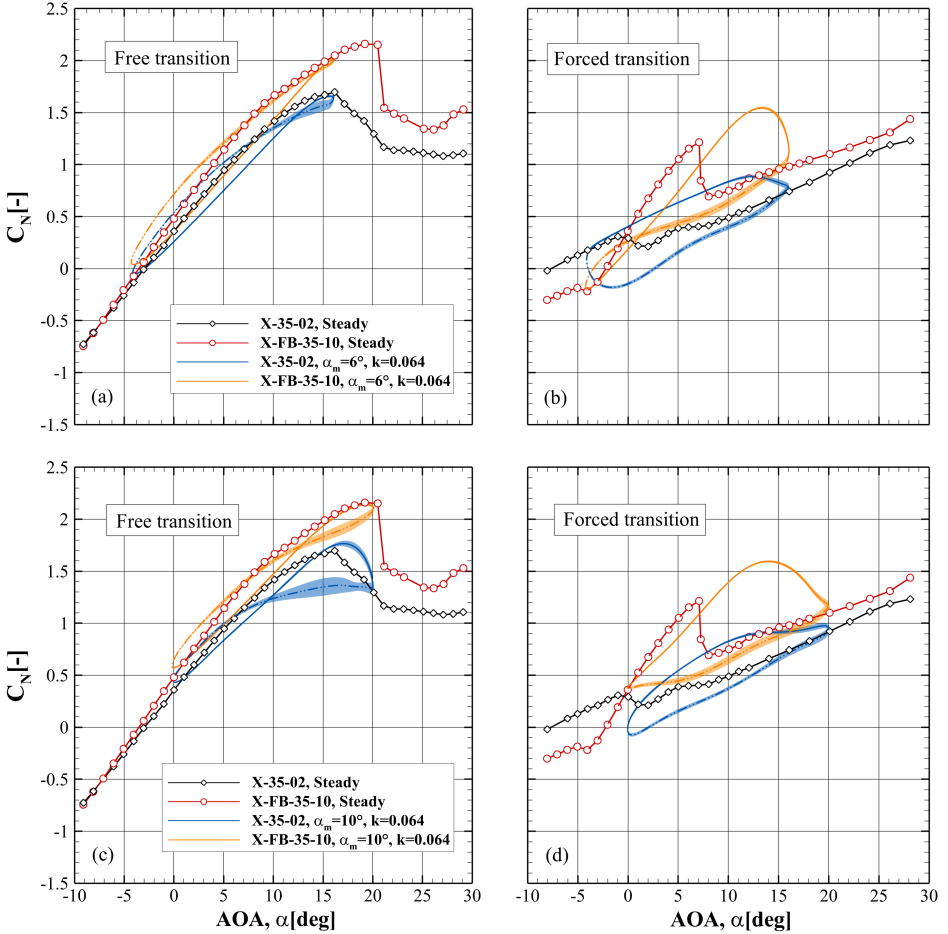


Figure 4.1: Comparison between the static and dynamic polar curves, in free and forced transition flow regimes, [$Re = 1 \times 10^6$, X-35-02 and X-FB-35-10, $\alpha_m = 6^\circ$, $\alpha_m = 10^\circ$].

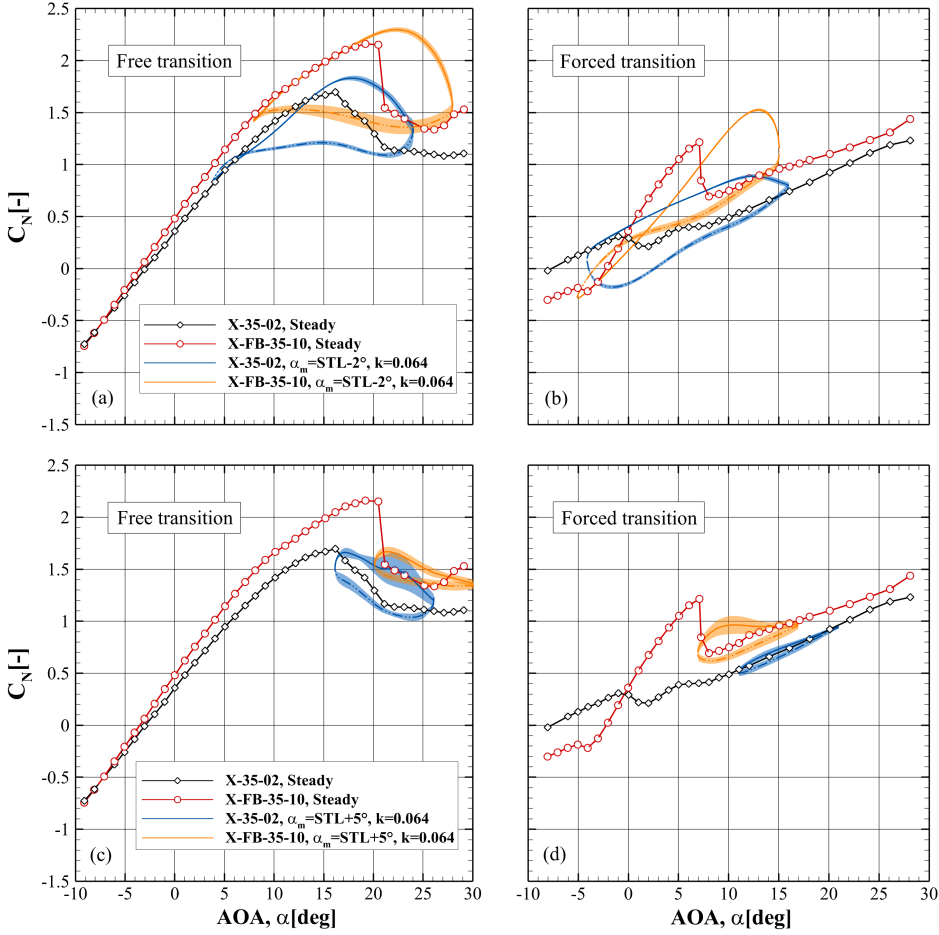


Figure 4.2: Comparison between the static and dynamic polar curves, in free and forced transition flow regimes, [$Re = 1 \times 10^6$, X-35-02 and X-FB-35-10, $\alpha_m = STL - 2^\circ$, $\alpha_m = STL + 5^\circ$].

US-phase begins under fully attached flow conditions, with C_N remaining below the static values, consistent with the previously observed trend. As a result, the dynamic hysteresis loops exhibit an eight-shaped pattern. In contrast, the X-FB-35-10 airfoil displays a cycle behavior similar to that of the X-35-02 airfoil at $\alpha_m = 6^\circ$, but with slightly more pronounced trailing edge separation at the TSA. Nevertheless, this separation remains insufficient to induce full stall, and only a minor reduction in C_N is observed at the TSA. Owing to its more stable flow characteristics, the flatback airfoil maintains a wider NFR and a higher MDNS compared to the X-35-02 airfoil. These observations clearly indicate that the flatback design delays the onset of dynamic stall relative to the conventional airfoil, resulting in higher normal force overshoots and improved aerodynamic performance under unsteady

conditions.

The dynamic results for the case with a mean angle of attack $\alpha_m = STL-2^\circ$ are shown in figure 4.2 (a). Under these conditions, the occurrence of dynamic stall causes both airfoils to exhibit characteristic eight-shaped hysteresis loops in the dynamic normal force coefficient response. The X-FB-35-10 airfoil demonstrates a longer C_N US-phase compared to the X-35-02 airfoil, which can be attributed to its more stable flow characteristics and delayed stall onset. In contrast, the flatback airfoil exhibits a more abrupt stall behavior than the X-35-02 airfoil. This difference is primarily due to its higher stall angle of attack, which induces stronger adverse pressure gradients on the suction side. These intensified gradients accelerate the expansion of the separation bubble toward the leading edge, resulting in a sharper loss of aerodynamic loading once stall occurs. Furthermore, the minimum C_N observed in this case is lower than that recorded at the end of the DS-phase, while the NFR remains larger for the flatback airfoil, indicating its ability to sustain higher aerodynamic loads over a broader range of angles of attack. Regarding the MDNS, the X-35-02 airfoil exhibits a slightly higher value than the flatback configuration. This reflects the more gradual stall progression of the X-35-02 airfoil, whereas the flatback design prioritizes load-carrying capacity and delayed stall onset at the expense of a steeper post-stall recovery.

Figures 4.1 (b) and (d) present the dynamic polars of both airfoils at mean angles of attack of 6° and 10° , in forced transition flow regime respectively. For the X-35-02 airfoil, these mean angles of attack fall within the post-stall region, resulting in extensive flow separation over both the suction and pressure surfaces by the end of the DS-phase. Consequently, the C_N hysteresis loops exhibit a flattened, oval shape, indicating limited recovery of aerodynamic loading during the pitching cycle. In both cases, the TSA and BSA lie beyond the positive and negative static stall angles, and the MDNS remains very close to the static curve, reflecting the dominance of fully separated flow conditions. In contrast, the X-FB-35-10 airfoil demonstrates a markedly different behavior. Dynamic stall occurs for both mean angles of attack because the BSA lies within the linear, stable portion of the static polar, allowing the US-phase to begin with an attached flow on the suction surface. Flow separation initiates only near the end of the US-phase, where a noticeable deviation in the dynamic C_N becomes evident. This large trailing edge separation bubble subsequently collapses by the BSA for both $\alpha_m = 6^\circ$ and $\alpha_m = 10^\circ$, restoring attached flow conditions. As a result, the MDNS of the flatback airfoil remains significantly higher than that of the X-35-02 airfoil, and the C_N overshoot values are substantially greater under forced transition conditions. It is also noteworthy that during the DS-phase, the C_N response for both airfoils tends to follow the slope of the post-stall steady-state C_N curve up to approximately mid-stroke, particularly in the case of $\alpha_m = 10^\circ$. This behavior depicts the strong influence of static post-stall aerodynamics on the dynamic response when large-scale separation dominates the flow-field.

Figure 4.2 (c) illustrates the dynamic stall behavior of both airfoils under post-stall conditions. The resulting hysteresis loops exhibit an oval shape and rotate clockwise, in contrast to the linear portion of the static polar. It is evident that the X-35-02 airfoil

undergoes a smoother and more gradual stall process compared to the abrupt stall observed for the flatback airfoil. For the X-35-02 airfoil, trailing edge separation progresses steadily toward the leading edge during stall onset, whereas the flatback airfoil experiences a more sudden and extensive separation event. Consequently, at the onset of the US-phase of the X-35-02 airfoil, the trailing edge separation bubble is smaller than that of the flatback airfoil, resulting in dynamic normal force values approaching the maximum steady values. On the other hand, the X-FB-35-10 airfoil exhibits a significantly larger separation bubble at the BSA, compared to the X-35-02 airfoil. Under deep stall conditions, the trailing edge vortex of the flatback airfoil merges with the suction side separation bubble, forming a multi-bubble separation zone. This highly separated region responds weakly to further increases in angle of attack, resulting in a smaller deviation of dynamic normal force during the US-phase. This results in a higher NFR value for the X-35-02 airfoil than the flatback airfoil. During the DS-phase, the persistence of the large separation bubble on the flatback airfoil, combined with pressure adaptation lag, causes the dynamic C_N to drop below its static counterpart. In contrast, the separation bubble on the X-35-02 airfoil retracts more rapidly, allowing its dynamic C_N to recover earlier, approximately from the first quarter of the DS-phase onward. This difference highlights the stronger hysteresis effects and slower flow reattachment associated with the flatback configuration under post-stall conditions.

And finally, figures 4.2 (b) and (d) present the dynamic polars of both airfoils under a forced transition flow regime at mean angles of attack corresponding to $STL - 2^\circ$ and $STL + 5^\circ$, respectively. For the $STL - 2^\circ$ condition, the results are generally consistent with those shown in figure 4.1 (b), with only a slight difference in the Maximum Lift Overshoot (MLOS). This variation can be attributed to the 1° increase in mean angle of attack, which slightly modifies the pressure distribution and the onset of trailing edge separation during the pitching cycle. In contrast, the behavior at $STL + 5^\circ$ differs significantly due to the presence of deep-stall conditions. At this higher mean angle of attack, both airfoils exhibit nearly flat, clockwise-oriented dynamic loops around the steady-state polar. This characteristic shape results from the formation of a large separation bubble that spans most of the suction side of the airfoil, effectively suppressing any substantial aerodynamic recovery during the cycle. Notably, the X-35-02 airfoil displays a slight increase in normal force at the beginning of the US-phase. This phenomenon is associated with the proximity of the BSA to the static stall angle of attack, which allows partial flow attachment on the suction side during the initial portion of the US-phase. This temporary reattachment enhances lift briefly before full separation dominates the remainder of the cycle, leading to a flattened hysteresis loop with minimal overshoot.

Figures 4.3 and 4.4 illustrate the pressure distributions corresponding to the dynamic polars discussed earlier, evaluated at three representative angles of attack: one-quarter, one-half, and three-quarters of both the US and DS-phases for both airfoils. The top rows of figures 4.3 and 4.4 correspond to the case shown in figure 4.1 (a), which represents a mean angle of attack of $\alpha_m = 6^\circ$. At an instantaneous angle of attack of approximately 1° during the US-phase, the pressure distribution reveals no evidence of trailing edge flow

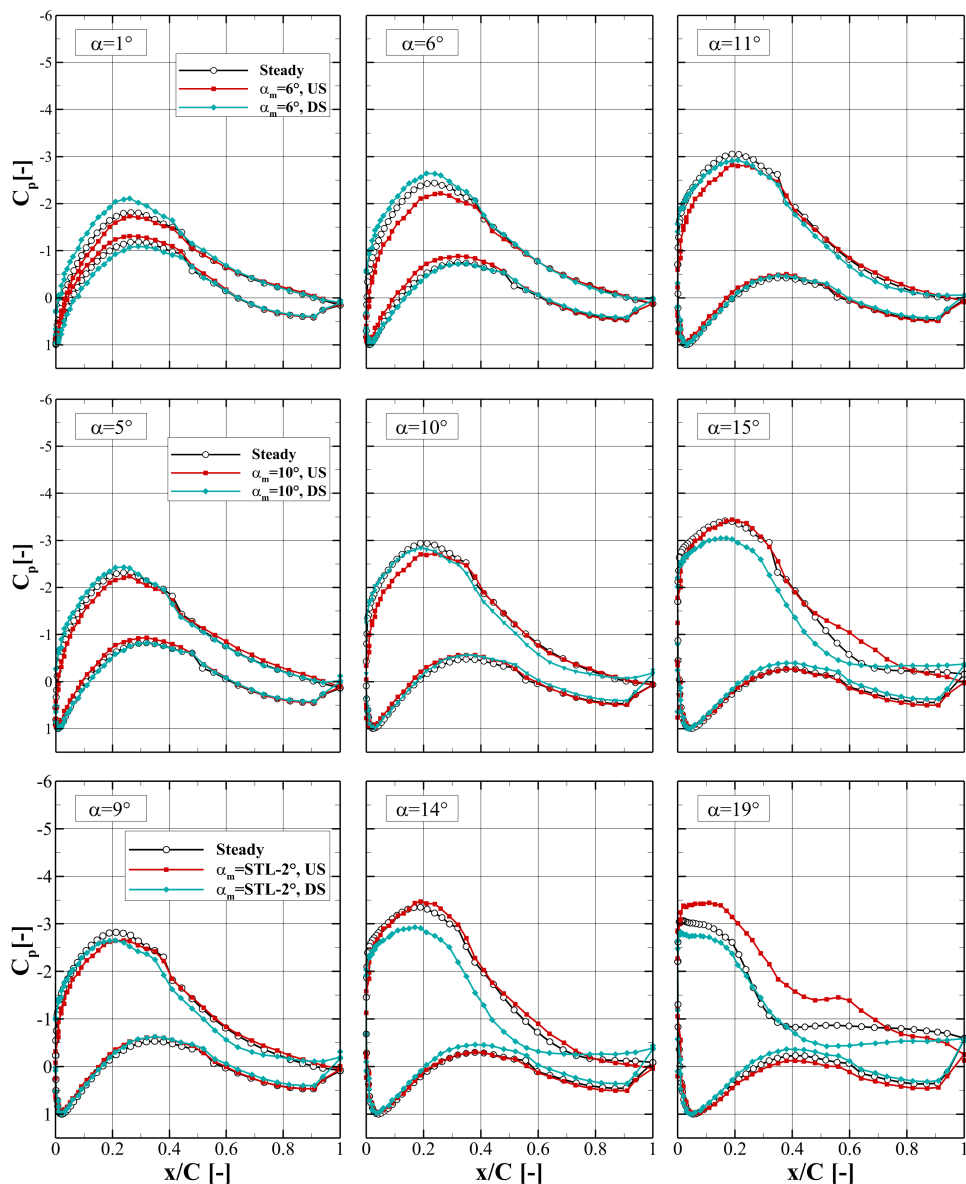


Figure 4.3: Dynamic pressure coefficient comparison in free transition flow regime, [$Re = 1 \times 10^6$, X-35-02, $k = 0.064$].

separation. The suction peak on the upper surface is noticeably lower than that of the corresponding static pressure distribution. This reduction is primarily attributed to two factors: (i) the pressure adaptation lag caused by the unsteady nature of the pitching motion, and (ii) the decrease in relative velocity over the suction surface as the airfoil rotates upward. If the flow remains fully attached, this reduction in suction peak persists across the subsequent US-phase snapshots. A gradual increase in suction peak pressure is observed as the angle of attack rises during the US-phase, although the values remain below the static reference due to the continuing influence of unsteady effects. Conversely, during the DS-phase, the pressure behavior differs significantly. The maximum suction peak exceeds the corresponding static value at lower angles of attack, particularly at $\alpha = 1^\circ$ and $\alpha = 6^\circ$. This enhancement in suction, increases the pressure gradient along the suction side of the airfoil slightly. Such behavior explains the higher dynamic normal force values observed during the DS-phase compared to the steady-state polar. At higher angles of attack, such as $\alpha = 11^\circ$, a short pressure plateau appears near the trailing edge for the X-35-02 airfoil, indicating the onset of trailing edge separation. This localized separation region plays a critical role in shaping the aerodynamic response during the subsequent stages of the pitching cycle.

With an increase in the mean angle of attack to 10° , corresponding to the case shown in figure 4.1 (c), the X-FB-35-10 airfoil exhibits a behavior broadly similar to that observed at lower mean angles, as previously described. However, a notable difference emerges at the beginning of the DS-phase, where a distinct flat suction peak plateau develops near the leading edge, as shown in figure 4.4. This feature reflects the increased unsteadiness and stronger adverse pressure gradients associated with higher mean angles of attack, compared to the $\alpha_m = 6^\circ$. In contrast, the TSA of the X-35-02 airfoil exceeds its static stall angle of attack, resulting in a more pronounced dynamic stall event relative to the flatback case. This difference underscores the greater susceptibility of the conventional airfoil to abrupt flow separation and the formation of large-scale vortical structures under unsteady conditions. The middle rows of figures 4.3 and 4.4 provide further insight into these phenomena by illustrating the corresponding pressure distributions at the mean angle of attack of $\alpha_m = 10^\circ$. As expected, during the US-phase, the suction peak remains lower than the static pressure coefficient for most conditions, reflecting the combined effects of pressure adaptation lag and reduced relative velocity over the suction surface. An exception occurs for the X-35-02 airfoil $\alpha = 15^\circ$, where the dynamic normal force coefficient temporarily exceeds its steady-state counterpart. This overshoot is indicative of delayed flow separation and the transient storage of circulation during the pitching motion. During the DS-phase, the onset of flow separation becomes clearly visible on the suction side, exposed as a flat pressure plateau extending from the mid-chord region toward the trailing edge for the X-35-02 airfoil. This plateau corresponds to the development of a trailing edge separation bubble, which gradually diminishes in size as the angle of attack decreases. Concurrently, the suction peak near the leading edge, initially observed as a relatively flat plateau for the X-FB-35-10 airfoil, undergoes a progressive transformation into a more curved profile and shifts

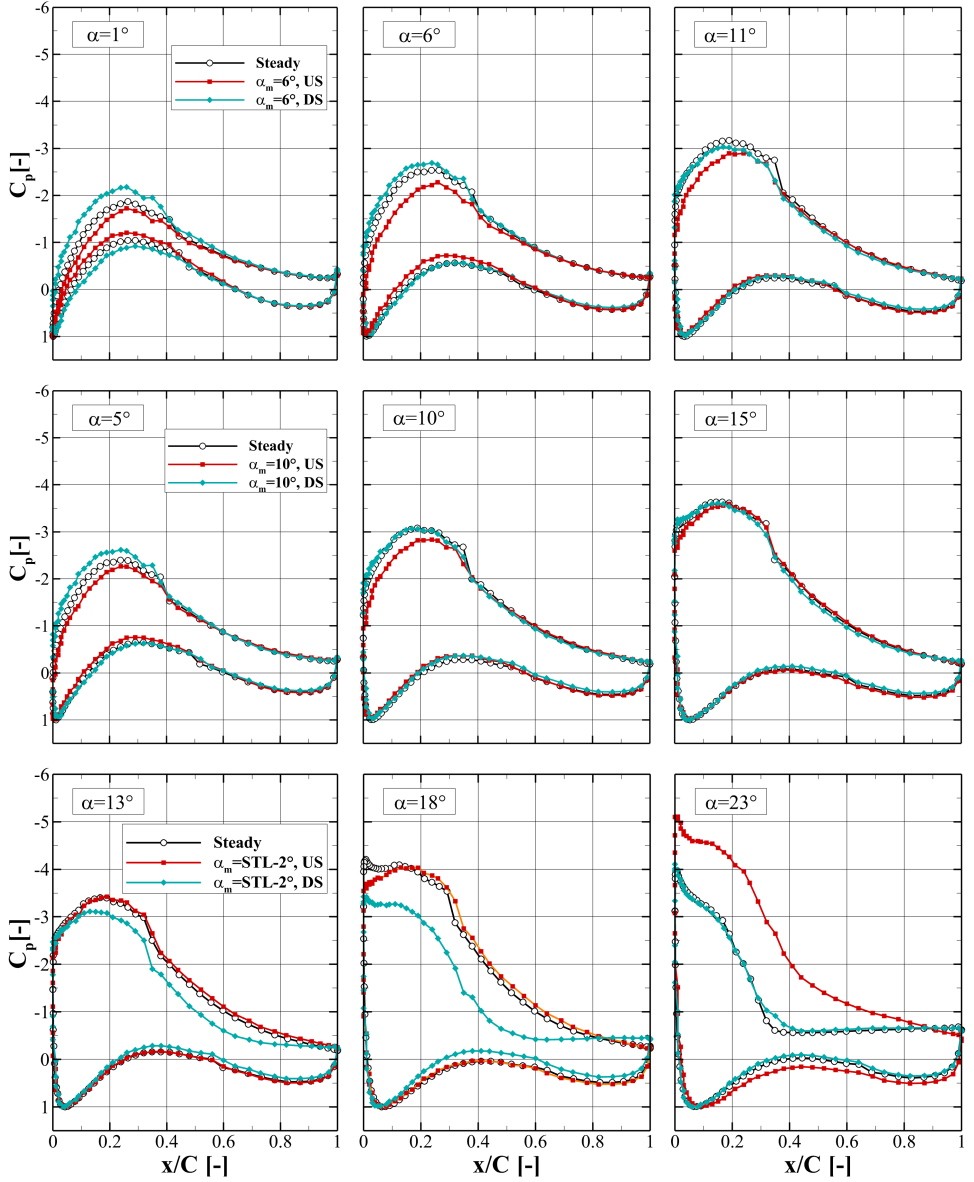


Figure 4.4: Dynamic pressure coefficient comparison in free transition flow regime, [$Re = 1 \times 10^6$, X-FB-35-10, $k = 0.064$].

downstream toward the trailing edge as the angle of attack decreases. These changes in pressure distribution highlight the complex interplay between unsteady pressure recovery, separation bubble dynamics, and aerodynamic load variation throughout the pitching cycle. Such observations are critical for understanding the mechanisms governing dynamic stall progression and their implications for aerodynamic performance under forced transition conditions.

At a mean angle of attack of $\alpha_m = STL-2^\circ$, which corresponds to the case illustrated in figure 4.2 (a), the pressure distribution during the US-phase exhibits notable changes as the instantaneous angle of attack increases. Specifically, the suction peak intensifies, shifts progressively toward the leading edge, and broadens in extent, as shown in the bottom rows of figures 4.3 and 4.4. This evolution reflects the combined influence of unsteady aerodynamic effects and the increasing adverse pressure gradient as the airfoil approaches the TSA. It is also evident that the pressure gradient on the suction surface of the flatback airfoil is consistently higher than that of the conventional X-35-02 airfoil, which contributes to its distinct dynamic stall characteristics. As previously discussed, the flow separation process accelerates rapidly near the end of the US-phase. This acceleration is driven by two primary factors: (i) the stagnation of the angle of attack as the airfoil approaches the TSA, which reduces the beneficial effect of increasing circulation, and (ii) the presence of a strong adverse pressure gradient on the suction side of the airfoil. Together, these conditions promote the upstream propagation of the separation front from the trailing edge toward the leading edge on both airfoils. However, the extent and rate of this propagation differ between the two configurations due to their geometric characteristics and pressure recovery behavior. During the DS-phase, the reattachment process becomes a key differentiator between the two airfoils. Owing to its higher suction peak and the presence of multiple vortex systems, specifically the trailing edge vortex and the suction-side vortex system, the X-FB-35-10 airfoil experiences a delayed reattachment compared to the X-35-02 airfoil. This delay results in a more persistent separation bubble on the flatback configuration, which influences the aerodynamic loading and contributes to the observed hysteresis in the dynamic normal force coefficient. In contrast, the X-35-02 airfoil exhibits a relatively faster recovery of attached flow, leading to an earlier restoration of pressure gradients and a steeper increase in C_N during the latter portion of the DS-phase. These differences underscore the critical role of airfoil geometry in governing unsteady flow behavior under near-stall and post-stall conditions.

EFFECT OF THE REDUCED FREQUENCY

Figure 4.5 (a) shows the influence of reduced frequency on the dynamic normal force coefficient characteristics of the X-35-02 airfoil within the attached flow region of the normal force polar under free transition conditions at $\alpha_m = 6^\circ$. The results clearly demonstrate

that as the reduced frequency increases, the pressure adaptation lag and the phase shift between aerodynamic loading and airfoil motion becomes more pronounced. This lag exposed as a reduction in C_N during the US-phase and a corresponding increase during the DS-phase. Furthermore, the trailing edge separation observed near the TSA persists for a longer duration at higher reduced frequencies compared to lower-frequency cases. This persistence indicates that the unsteady pressure recovery process is significantly delayed when the pitching motion accelerates, thereby amplifying the hysteresis effect. Another important observation is that the dynamic normal force coefficient aligns with its static counterpart only at the lowest reduced frequency considered $k = 0.032$. At higher values of the reduced frequency, the deviation from the static curve becomes increasingly evident, primarily due to the intensified pressure lag and delayed flow reattachment. In addition to these trends, both the MDNS and the NFR exhibit a systematic reduction with increasing reduced frequency. This reduction implies that the aerodynamic stiffness of the airfoil decreases under highly unsteady conditions, which can have critical implications for rotor blade performance and load prediction in large wind turbines. Note that, as the TSA is equivalent to the steady stall angle of attack, a minor dynamic stall is observed across all reduced frequencies.

Similar dynamic characteristics are observed for the X-FB-35-10 airfoil at $\alpha_m = 10^\circ$ within the attached segment of the normal force polar, as shown in figure 4.7 (c). However, a key distinction lies in the presence of more pronounced normal force fluctuations near the TSA. These fluctuations are primarily attributed to the geometric characteristics of the flatback airfoil, which promote the formation of complex vortex systems, i.e., trailing edge vortices and suction side vortical structures, which form near the trailing edge. The interaction of these vortices with the pressure field near the trailing edge amplifies local unsteadiness, resulting in greater variability in aerodynamic loading compared to the X-35-02 airfoil.

Figure 4.7 (a) depicts the influence of reduced frequency on the aerodynamic response of the X-FB-35-10 airfoil under a free transition flow regime at a mean angle of attack of $\alpha_m = 6^\circ$. The natural static stall angle of this airfoil is considerably higher than that of the X-35-02 airfoil. As a result, the TSA remains well below the static stall limit throughout the examined cases. This configuration provides a representative example of the unsteady aerodynamic response of thick flatback airfoils operating within the linear region of the lift curve. Because the BSA lies in the linear region of the lift curve, the behavior during the US-phase is broadly comparable to that observed for the X-35-02 airfoil at the same mean angle of attack across all investigated reduced frequencies. Interestingly, the aerodynamic response exhibits a localized dip in lift near the TSA for reduced frequencies of $k = 0.032$ and $k = 0.064$. This behavior suggests the onset of unsteady flow effects, even though the operating range remains within the nominally linear portion of the lift curve. This reduction is accompanied by minor fluctuations in lift at the onset of the DS-phase. Notably, this temporary loss of lift recovers rapidly, and the dynamic normal force subsequently exceeds the corresponding static polar values, indicating a pronounced dynamic overshoot

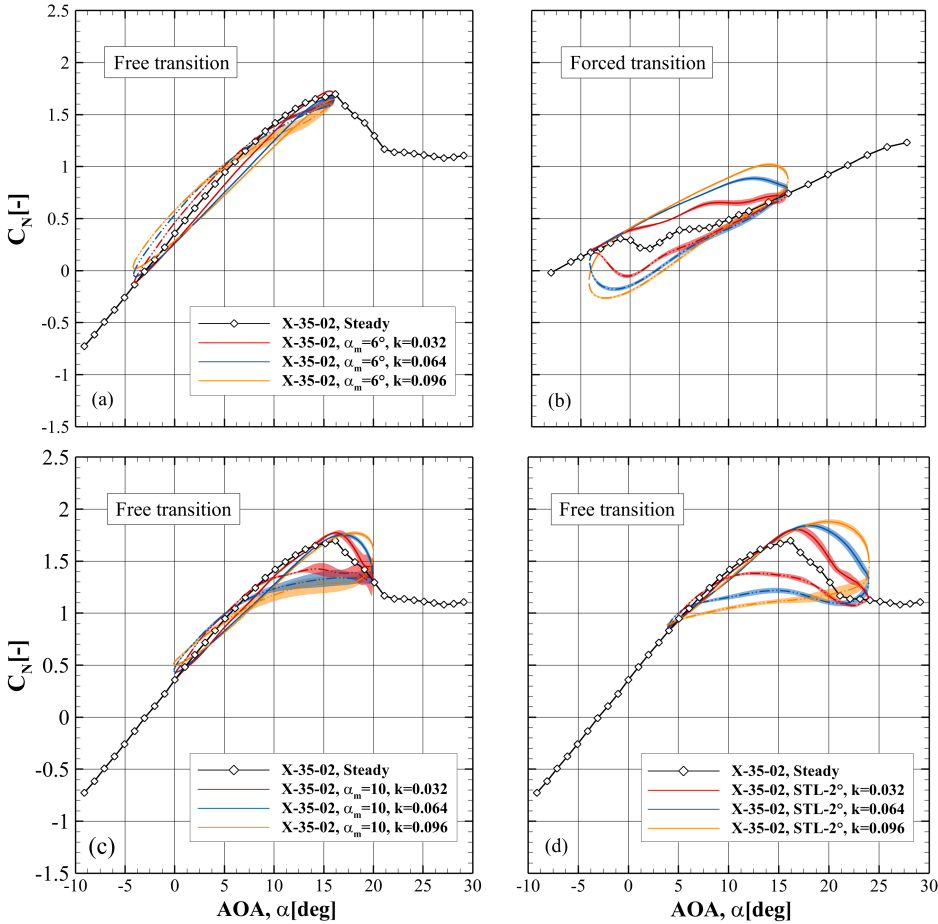


Figure 4.5: Effect of the reduced frequency on the normal coefficient, in free and forced transition flow regimes, $[Re = 1 \times 10^6, X-35-02]$.

effect. In contrast, at the higher reduced frequency of $k = 0.096$, no lift drop occurs at the TSA. Instead, the aerodynamic response forms a counterclockwise hysteresis loop, which is characteristic of thin airfoils operating within the linear region of the lift curve. Similar to the previous case, the MDNS decreases progressively with increasing reduced frequency.

As the mean angle of attack increases to $\alpha_m = 10^\circ$ and the TSA surpasses the static stall angle in free transition flow regime, the characteristics of the dynamic cycles undergo significant changes with increasing reduced frequency. This evolution is primarily driven by two coupled mechanisms: the intensification of pressure adaptation lag and the delayed development of the trailing edge separation bubble above the suction side of the airfoil. Together, these effects modify both the onset of dynamic stall and the recovery pattern of the aerodynamic loads. The influence of these phenomena is clearly illustrated in figure 4.5

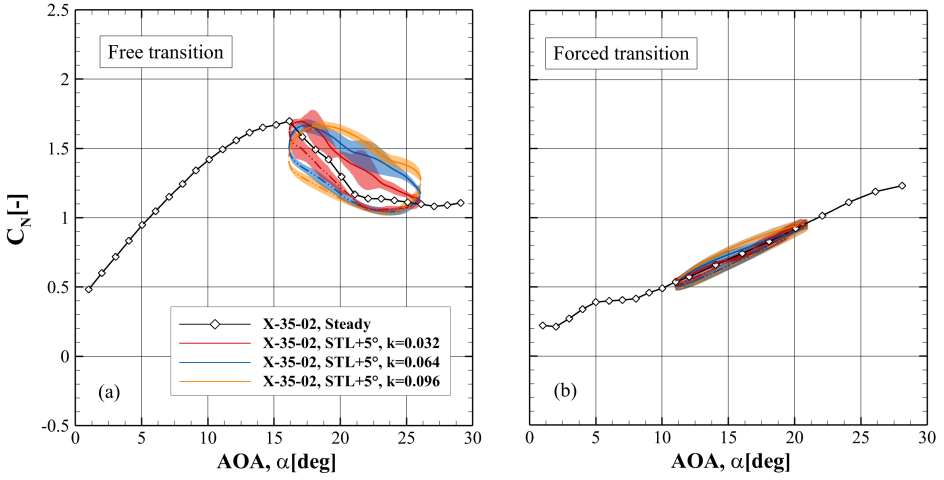


Figure 4.6: Effect of the reduced frequency on the normal coefficient, in free and forced transition flow regimes, $[Re = 1 \times 10^6, X-35-02]$.

(c). One notable observation is that dynamic stall recovery occurs at lower angles of attack as the reduced frequency increases. This trend is due to the stronger pressure lag at higher reduced frequencies, which decelerates the reattachment process once the airfoil enters the DS-phase. Conversely, the onset of dynamic stall is delayed under these conditions, as the flow remains partially attached for a longer portion of the US-phase despite the increasing angle of attack. This delay reflects the inability of the boundary layer to respond instantaneously to rapid pitch changes, as the reduced frequency increased. Another important effect concerns the size and persistence of the trailing edge separation bubble near the TSA. At higher reduced frequencies, the bubble develops over a shorter chordwise extent compared to lower-frequency cases. This reduction in bubble length is associated with the rapid variation in angle of attack, which limits the time available for full separation to evolve. Consequently, the dynamic normal force exhibits a sharper drop at higher angles of attack relative to low-frequency conditions, amplifying the hysteresis loop.

Figure 4.7 (b) shows the effect of reduced frequency on the aerodynamic response of the X-35-02 airfoil under a forced transition flow regime at a mean angle of attack of $\alpha_m = 6^\circ$. The results indicate that the dynamic hysteresis loops exhibit a capsule shape and rotate in a clockwise direction. Because the BSA is located below the negative stall angle, the flow remains fully attached to the suction side of the airfoil at the beginning of the US-phase. As the angle of attack increases during the US-phase, a trailing edge separation bubble develops and gradually expands, due to the adverse pressure gradient increase near the trailing edge, leading to a reduction in lift. As the reduced frequency increases, the expansion rate of the trailing edge separation bubble diminishes, due to the greater pressure adaptation lag at higher reduced frequencies, which delays the onset of separation on the suction side of the airfoil. Consequently, the magnitude of the lift drop during the

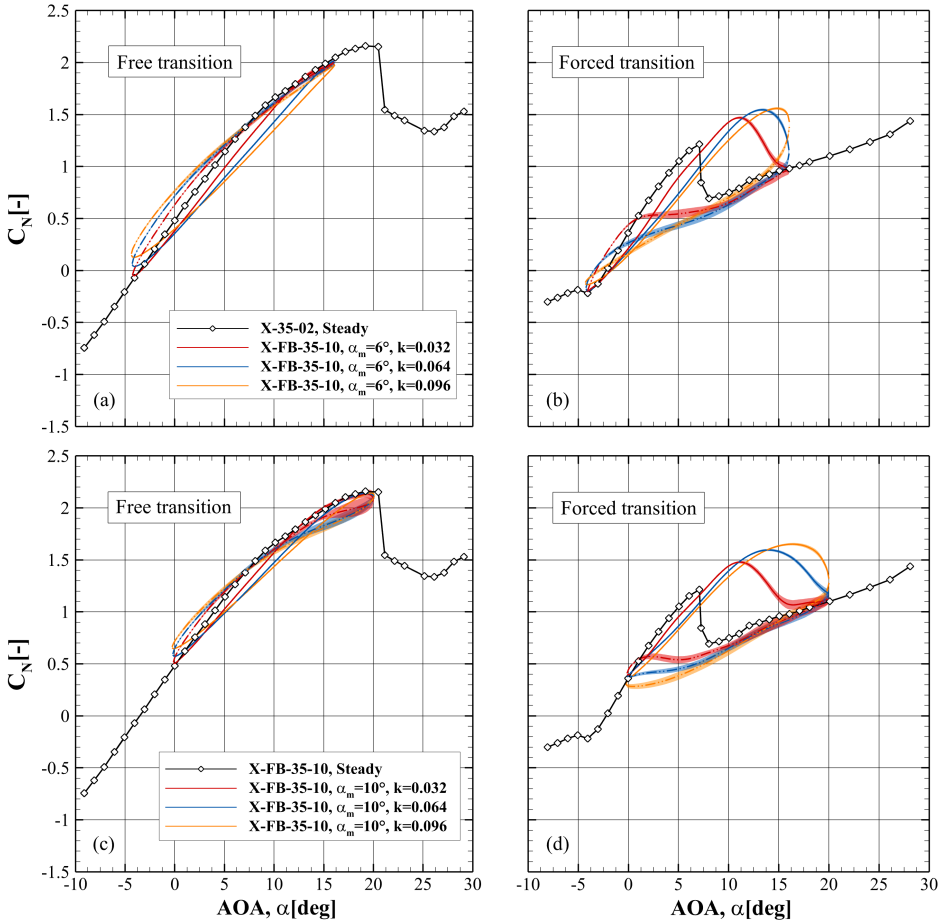


Figure 4.7: Effect of the reduced frequency on the normal coefficient, in free and forced transition flow regimes, [$Re = 1 \times 10^6$, X-FB-35-10].

US-phase is reduced. This tendency is further evidenced by the progressive reduction in the amplitude of lift fluctuations, i.e., quantified through the standard deviation, within the US-phase as the reduced frequency increases. Note that the dynamic normal force closely follows the steady-state lift slope at negative angles of attack, indicating minimal unsteady effects in this region. Similarly, during the DS-phase, the dynamic normal force exhibits a slope comparable to that of the steady-state polar. Furthermore, both the MDNS and the NFR increase with increasing reduced frequency, as a result of flow stabilization. This trend is consistent with observations made for the same airfoil at higher mean angles of attack, specifically at $\alpha_m = 10^\circ$ and $\alpha_m = STL - 2^\circ$, which are not shown here.

The effect of reduced frequency on the dynamic stall characteristics of both airfoils at a mean angle of attack of $\alpha_m = STL - 2^\circ$ in free transition flow regime is illustrated in

figures 4.5 (d) and 4.8 (a). The results reveal that the maximum dynamic normal force coefficient overshoot increases progressively with increasing reduced frequency. This amplification of overshoot is primarily linked to the intensified pressure adaptation lag at higher reduced frequency values, which alters the timing and extent of flow separation during the pitching cycle at higher mean angles of attack. At elevated reduced frequencies, the pressure lag delays the development of the trailing edge separation bubble on the suction side of the airfoil during the US-phase. Consequently, the flow remains attached over a larger portion of the chord and up to higher angles of attack, which in turn produces a more pronounced C_N overshoot before stall onset. The figures also indicate that dynamic stall occurs prior to reaching the TSA, and the dynamic stall angle of attack shifts closer to the TSA as the reduced frequency increases. At low reduced frequencies, the normal

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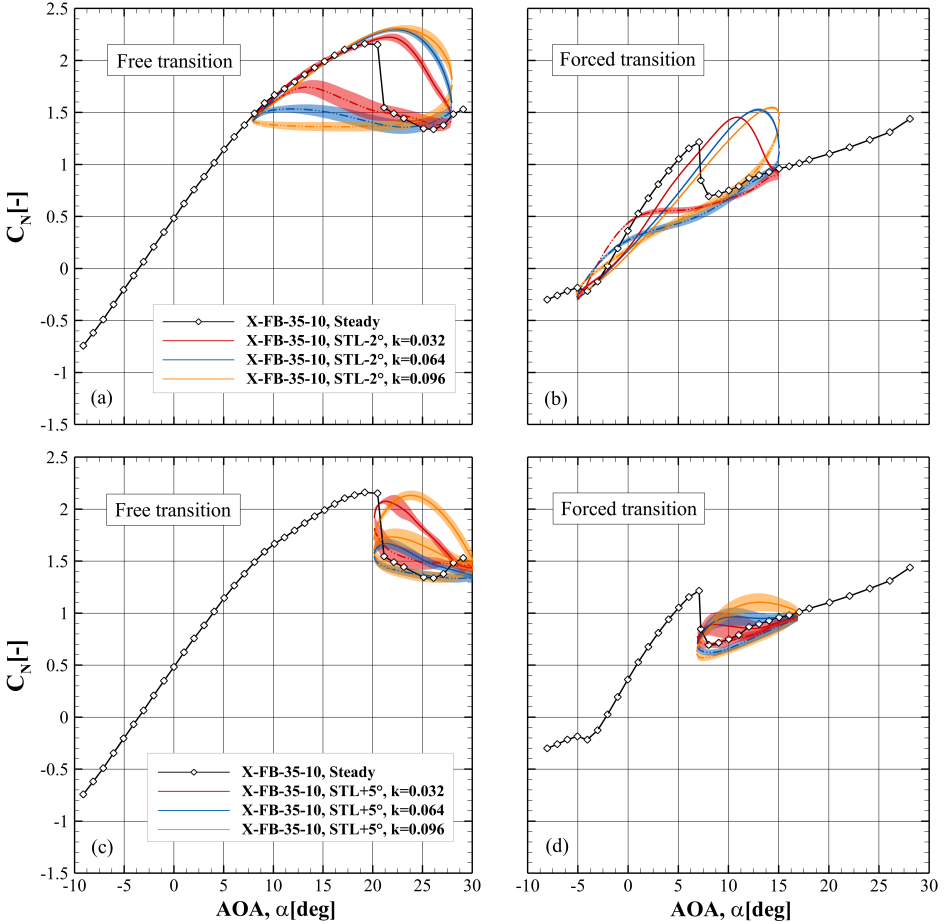


Figure 4.8: Effect of the reduced frequency on the normal coefficient, in free and forced transition flow regimes, [$Re = 1 \times 10^6$, X-FB-35-10].

force coefficient typically drops to values comparable to the static curve following dynamic stall, indicating a more complete loss of aerodynamic loading. In contrast, as the reduced frequency rises, this post-stall drop becomes less severe. The reduced magnitude of the drop is attributed to the slower and less extensive development of the trailing edge separation bubble, which is constrained by the limited time available for full separation to evolve under rapid pitch motion. Compared to the dynamic loops observed in the linear portion of the normal force curve, both the NFR and the MDNS exhibit a noticeable increase as the reduced frequency rises, indicating greater aerodynamic stiffness and an enhanced capacity to sustain aerodynamic loads under highly unsteady conditions. Furthermore, the flatback airfoil consistently exhibits higher NFR and MDNS values compared to the X-35-02 airfoil, primarily due to its ability to sustain a higher maximum dynamic C_N . It is also noteworthy that the flatback airfoil demonstrates a longer dynamic stall recovery period than the X-35-02 airfoil. This extended recovery is associated with the presence of multiple vortex systems, specifically a trailing edge vortex and additional suction side vortices, which interact to delay complete flow reattachment and prolong the hysteresis cycle.

Figures 4.7 (b) and (d) and 4.8 (b) present the dynamic response of the X-FB-35-10 airfoil in forced transition flow regime at the mean angle of attack of $\alpha_m = 6^\circ$, $\alpha_m = 10^\circ$, and $\alpha_m = STL-2^\circ$ respectively. In all of these cases the dynamic stall occurs and the normal force coefficient drops to the static curve level of the static curve after the occurrence of the dynamic stall for all reduced frequencies and tracks it at lower angles of attack. With increasing the reduced frequency, the drop of the C_N is delayed, which leads to a higher dynamic stall angle of attack and higher lift overshoot, as it is seen for the free transition flow cases. Note that for the $\alpha_m = 10^\circ$ case, despite exceeding the mean angle of attack than the static stall angle of attack, it is intriguing to observe that the dynamic stall occurs and increasing the reduced frequency can effectively postpone the onset of dynamic stall. Note the effect of negative dynamic stall due to pressure side flow separation at the end of the DS-phase for the $\alpha_m = STL-2^\circ$ case.

The dynamic stall characteristics of both airfoils in the post-stall region under free transition flow regime are illustrated in figures 4.6 (a) and 4.8 (c). For the X-35-02 airfoil at a reduced frequency of $k = 0.032$, the dynamic normal force coefficient forms a loop closely aligned with the static curve, indicating limited hysteresis. As the reduced frequency increases, these loops gradually transform into a more oval shape, and the US-phases exhibit greater stability. This trend is primarily due to the intensification of pressure adaptation lag, which delays flow separation and modifies the aerodynamic response during rapid pitch motions. Despite this stabilization, the presence of a large trailing edge separation bubble above the suction side of the airfoil remains a dominant feature in the post-stall regime. During the US-phase, the bubble begins to expand progressively, and its growth point can be identified as the location of the maximum normal force coefficient within the US-phase. Notably, this point shifts toward higher angles of attack as the reduced frequency increases, reflecting the delayed onset of full separation under accelerated

pitching conditions. These dynamic loops rotate clockwise. While the reduced frequency does not significantly alter the NFR, it primarily influences the phase shift of extremum points within the cycle. In contrast, the MDNS decreases systematically with increasing reduced frequency, indicating a reduction in aerodynamic stiffness. Furthermore, the standard deviation of the normal force coefficient within the loops also diminishes as the reduced frequency rises, suggesting a more uniform aerodynamic response under highly unsteady conditions. The dynamic behavior of the X-FB-35-10 airfoil differs markedly due to its distinct static stall characteristics. At a reduced frequency of $k = 0.032$, the US-phase normal coefficient exceeds the static value initially but subsequently drops back to near-static levels as a separation bubble forms on the suction side of the airfoil. Conversely, the DS-phase closely follows the static curve, dominated by a large separation bubble persisting in the post-stall region. Increasing the reduced frequency to $k = 0.064$ results in a noticeable reduction in NFR, reflecting the influence of pressure lag on load variation. At $k = 0.096$, the US-phase exhibits highly irregular patterns, commonly referred to as bifurcation flow. This phenomenon occurs in extremely unstable flow conditions, where the aerodynamic system is sensitive to initial conditions and small pressure perturbations. Consequently, multiple separation bubbles of varying sizes and locations may form near the end of the DS-phase, leading to cycle-to-cycle variability in aerodynamic response. These bubbles interact with trailing edge vortices and suction side vortical structures, creating complex flow topologies that prevent the initiation of a repeatable pattern. The configuration of the US-phase is heavily influenced by instantaneous pressure recovery and vortex dynamics, resulting in a variety of hysteresis loop shapes rather than a single deterministic cycle. This behavior signifies nonlinear aerodynamic bifurcation, where the system shifts from a stable periodic response to a quasi-chaotic regime at high reduced frequency conditions.

Figures 4.6 (b) and 4.8 (d) present the dynamic stall characteristics of the X-35-02 and X-FB-35-10 airfoils under a forced transition flow regime within the post-stall region. For the X-35-02 airfoil, the dynamic hysteresis loops remain closely aligned with the static normal force curve, indicating that unsteady effects are less pronounced in this configuration. In contrast, the X-FB-35-10 airfoil exhibits a more pronounced sensitivity to changes in reduced frequency. As the reduced frequency increases, both the NFR and the MDNS show a slight increase. This effect arises from the persistence of a small attached flow region on the suction side of the airfoil at the onset of the US-phase, a condition driven by the proximity of the BSA to the steady-state stall angle of attack. Higher reduced frequencies allow this partially attached flow region to persist to greater angles of attack before complete separation occurs, effectively delaying full stall and enhancing the dynamic load response. The X-35-02 airfoil, however, responds differently to variations in reduced frequency due to the presence of extensive flow separation over the suction surface in the post-stall regime. In this case, the dynamic loops, which initially appear flat and elongated, gradually evolve into slightly oval-shaped loops as the reduced frequency increases. This indicates that, for thick conventional airfoils operating under deep-stall and tripped flow conditions, the dynamic response tends to flatten and remain concentrated around the

steady-state lift curve.

4.3 CONCLUSIONS

The main goal of this chapter was to investigate the differences in dynamic stall characteristics between thick flatback and non-flatback wind turbine airfoils. To achieve this, an experimental campaign was conducted in the LTT wind tunnel at TU Delft. Two airfoils were examined: the conventional X-35-02 and the flatback X-FB-35-10. Both were tested under a range of mean angles of attack, reduced frequencies, and flow conditions, including free and forced transition regimes. Surface pressure measurements were employed to accurately determine the unsteady aerodynamic coefficients throughout the oscillatory motion. The dynamic measurements revealed that the flatback airfoil exhibited more stable hysteresis loops compared to the sharp trailing edge airfoil. Specifically, it showed fewer lift instabilities, larger lift overshoots, and higher MDNS within the attached flow region of the lift curve. These improvements were observed under both free and forced transition conditions and are attributed to the more favorable suction side pressure gradient of the flatback geometry. However, the stability of dynamic lift decreased as the reduced frequency increased, when light trailing edge separation was present. Under dynamic stall conditions, the development of the trailing edge separation bubble over the flatback airfoil occurred more gradually than over the sharp trailing edge airfoil, effectively delaying the onset of dynamic stall. Increasing the reduced frequency amplified the pressure development lag on the suction side, which slowed the progression of separation and enhanced the dynamic lift overshoot for both airfoils. This stabilizing effect was more pronounced for the flatback airfoil. Consequently, the magnitude of lift drop at stall onset diminished as reduced frequency increased. The nature of steady stall, whether abrupt or gradual, was found to influence the dynamic stall behavior, particularly during the DS-phase. At low reduced frequencies, the flatback airfoil exhibited a more abrupt dynamic stall compared to its sharp trailing edge counterpart. In the post-stall regime, the X-35-02 airfoil displayed high sensitivity to leading edge boundary layer tripping and extensive flow separation, causing its dynamic lift to remain close to the static curve under forced transition conditions, resulting in a relatively flat hysteresis footprint. In contrast, the flatback airfoil exhibited significant dynamic lift fluctuations under forced transition, and similar oscillations were observed for both airfoils in free transition conditions. Overall, the findings of this chapter demonstrate that while flatback and non-flatback airfoils share certain dynamic stall characteristics, notable differences exist that have practical implications for wind turbine design. The use of flatback airfoils delays the onset of dynamic stall, reducing sensitivity to wind fluctuations and mitigating vortex-induced vibrations during turbulent inflow conditions. These effects can enhance rotor blade stability, improve energy capture, and increase the overall reliability of large wind turbines. Furthermore, the structural and steady aerodynamic

advantages of flatback airfoils make them an attractive option for mid-span blade sections. Nevertheless, additional research is required to fully quantify their influence on unsteady loads and to optimize their integration into modern wind turbine designs.

5

VORTEX GENERATOR EFFECTS ON DYNAMIC STALL OF THICK AIRFOILS

5

An experimental investigation into the dynamic stall behavior of thick wind turbine airfoils, with a particular focus on the influence of vortex generators is presented in this chapter. The same airfoils as the previous chapters, with a maximum relative thickness of 35% were examined: a flatback configuration and a sharp trailing edge variant. The experiments were conducted in the Low Turbulence Tunnel at TU Delft at a Reynolds number of $Re = 1 \times 10^6$, enabling controlled analysis under representative flow conditions. The study systematically evaluates the impact of several parameters on dynamic stall characteristics, including the presence and chordwise positioning of vortex generators, ranging from 20% up to 40% of the chord, trailing edge geometry, leading edge boundary layer tripping, mean angle of attack, and reduced frequency within the range of 0.032 to 0.096. Aerodynamic coefficients were derived from time-resolved surface pressure measurements, enabling a detailed analysis of unsteady aerodynamic behavior. The findings provide valuable insights into the role of VGs in mitigating dynamic stall effects on thick airfoils, offering essential data for improving aerodynamic models, validating dynamic stall prediction methods, and supporting the design of next generation wind turbine blades.

5.1 OBJECTIVE

While previous studies have extensively explored the dynamic stall and flow physics of thin airfoils, the role of vortex generators in dynamic stall mitigation remains relatively undiscovered. However, the current understanding of the dynamic behavior of thick airfoils, particularly flatback and conventional airfoils, in combination with vortex generators is still limited. This lack of knowledge poses a significant risk, as it can lead to substantial errors in dynamic load estimations, resulting in suboptimal blade designs and potential structural failures. This chapter aims to advance the understanding of dynamic stall phenomena by experimentally investigating the combined effects of trailing edge thickness and vortex generators on thick wind turbine airfoils. To this end, a wind tunnel campaign was conducted to study two airfoils a flatback and a sharp trailing edge variant, under various flow conditions. The tests included multiple vortex generators chordwise positions, reduced frequencies, and mean angles of attack spanning different regions of the lift curve, including the linear, pre-stall, stall, and post-stall regimes. Both clean and tripped flow conditions were considered to assess the influence of surface roughness. This chapter is structured as follows:

- First, the effect of mean angle of attack on both airfoils is analyzed under free and forced transition conditions, with vortex generators installed at 30% of the chord.
- Next, the development of pressure coefficient distributions during the US and DS-phases is examined at various mean angles of attack for both airfoils with the same vortex generators configuration.
- Subsequently, the influence of reduced frequency and vortex generators chordwise position on dynamic stall behavior is evaluated.
- Finally, the key findings are summarized in the concluding section.

5.2 RESULTS AND DISCUSSION

EFFECTS OF THE AIRFOIL GEOMETRY AND MEAN ANGLE OF ATTACK

The dynamic normal force coefficient polars for the X-35-02 and X-FB-35-10 airfoils equipped with VGs positioned at 30% of the chord length is shown in figures 5.1 and 5.2. The results correspond to sinusoidal oscillatory motions at a reduced frequency of $k = 0.064$ and a Reynolds number of $Re = 1 \times 10^6$, under both free and forced transition flow conditions. For each airfoil, four representative mean angles of attack were considered: $\alpha_m = 6^\circ$,

$\alpha_m = 10^\circ$, $\alpha_m = STL - 2^\circ$, and $\alpha_m = STL + 5^\circ$. These values were selected to capture the main aerodynamic regimes, namely the linear portion of the lift curve, the upper section approaching stall, the region associated with dynamic stall onset, and the post-stall regime.

The dynamic cycles are visualized the same as in the previous chapter as closed hysteresis loops, where the US and DS-phases are distinguished by line style: solid lines represent the US-phase, while dash-dot-dot lines indicate the DS-phase. In addition, shaded bands adjacent to each curve denote the standard deviation of the measured cycles. Note that in attached flow regimes, the standard deviation of the C_N polars during both US and DS-phases remains relatively low, reflecting the stability of the aerodynamic forces. In contrast, when the flow becomes separated, the standard deviation increases significantly. This increase is attributed to the unsteady nature of large-scale flow separation and the intermittent formation and convection of separation bubbles, which induce pronounced cycle-to-cycle fluctuations in the aerodynamic loads.

Figure 5.1 (a) illustrates the dynamic hysteresis loops of the normal force coefficient for the X-35-02 and X-FB-35-10 airfoils equipped with VGs positioned at 30% of the chord length, at a mean angle of attack of $\alpha_m = 6^\circ$. Most of the dynamic cycles are located within the linear portion of the lift curve, with only the angles of attack near the TSA entering the non-linear region. Despite approaching the TSA, there remains a considerable margin to the steady stall angle, ensuring that the flow remains fully attached across the airfoil surfaces. Consequently, the dynamic loops form counterclockwise oval shapes around the steady-state normal force polar curves, a characteristic feature of attached flow oscillations. Such loops reflect a relatively stable aerodynamic response, where the unsteady forces are governed mainly by pressure adaptation rather than large-scale flow separation. The presence of VGs reinforces this stability by delaying transition and maintaining attached flow over a wider range of angles of attack. The results reveal a behavior consistent with cases without VGs in the linear region of the polar curve. The dynamic C_N during the US-phase is slightly lower than the steady-state value, while during the DS-phase it becomes higher. This asymmetry is primarily caused by the pressure adaptation lag, which delays the aerodynamic response relative to the instantaneous angle of attack. As the flatback airfoil exhibits a naturally higher steady lift and a steeper lift slope compared to the sharp trailing edge X-35-02 airfoil, with VGs installed, the dynamic normal force coefficient for the flatback airfoil remains consistently higher at every angle of attack throughout the oscillatory cycle. Additionally, during the DS-phase, the flatback airfoil exhibits a slight increase in the standard deviation of the normal force coefficient C_N . This variability is primarily linked to the trailing edge vortex system, which occasionally migrates toward the upper surface near the trailing edge area, introducing localized flow instabilities and amplifying unsteady aerodynamic loads. The blunt trailing edge geometry of the flatback airfoil inherently promotes stronger vortex shedding compared to the sharp trailing edge design of the X-35-02 airfoil. When combined with residual vortices generated by the VGs, these interactions create a more complex and unstable wake structure during the DS-phase. Although these effects remain relatively modest at low mean angles of

attack, they highlight the flatback airfoil's sensitivity to trailing edge vortex dynamics under oscillatory conditions. Such sensitivity can lead to increased dynamic variability and potentially higher fatigue loads in practical applications. Notice that the MDNS and NRF values of both airfoils are similar.

In the free transition flow regime with VGs, both airfoils exhibit similar aerodynamic characteristics at a mean angle of attack of $\alpha_m = 10^\circ$, as it is presented in figure 5.1 (b). During the US-phase, the flow remains largely attached up to the TSA, and the normal force coefficient stays slightly below the corresponding steady-state polar. This behavior is mainly due to the pressure adaptation lag, which delays the aerodynamic response relative to the instantaneous angle of attack. As the airfoils approach the TSA and the pitching rate decreases, flow separation initiates near the trailing edge on both airfoils. During the subsequent DS-phase, the separated region gradually diminishes as the angle of attack decreases, eventually disappearing at approximately two-thirds of the DS-phase. Notably, the dynamic C_N curve rises above the steady-state polar during this phase, again as a consequence of the pressure adaptation lag. The flatback airfoil demonstrates more pronounced flow instabilities in the vicinity of the TSA, which can be attributed to its closer proximity to the steady stall angle and the influence of the trailing edge vortex system, which is available at any angle of attack. A comparison with the baseline configuration without VGs reveals several important trends. The inclusion of VGs increases the slopes of the C_N curves during both US and DS-phases, as well as the MDNS and the NRF for both airfoils. Furthermore, the difference between the dynamic hysteresis loops of the two airfoils is reduced when VGs are present, as the X-35-02 cycle transitions toward a more oval-shaped loop, indicating a more symmetric and stable aerodynamic response.

Figure 5.2 (a) shows the free transition flow regime with VGs, at a mean angle of attack of $\alpha_m = STL-2^\circ$. The results reveal that both airfoils exhibit characteristic figure-eight-shaped dynamic hysteresis loops. The implementation of VGs reduces the disparity between the steady-state polars of the two airfoils, comparing to the cases in clean configuration without VGs, leading to more comparable dynamic cycle characteristics. This indicates that VGs promote a more uniform aerodynamic response by mitigating differences in stall behavior between the airfoils. Despite this similarity, notable distinctions remain in the unsteady behavior of the two airfoils. The standard deviation of the normal force coefficient during the US-phase is significantly higher for the flatback airfoil compared to the X-35-02 airfoil. This increased variability is mainly linked to the presence of a trailing edge vortex and the onset of trailing edge separation prior to reaching the TSA. In both cases, the dynamic C_N temporarily drops below the value corresponding to the BSA, reflecting the influence of transient flow separation. It is important to note that the separation bubble persists over the suction side of the airfoil during this phase, and flow reattachment occurs only near the BSA on the US-path. During the DS-phase, the flatback airfoil again exhibits a higher standard deviation than the X-35-02 airfoil. This behavior results from the interaction of multiple vortex systems, including the trailing edge vortex, the suction side separation bubble, and residual vortices generated by the VGs. These complex interactions amplify the unsteady

aerodynamic loads and contribute to greater cycle-to-cycle variability. Furthermore, the installation of VGs leads to an increase in the NFR for both airfoils compared to the baseline configuration without VGs, indicating an overall enhancement in load-carrying capability under dynamic stall conditions.

By increasing the mean angle of attack to $\alpha_m = STL + 5^\circ$ for the same flow and airfoil configuration, both airfoils exhibit clockwise dynamic hysteresis loops, as depicted in figure 5.2 (c). This reversal in loop orientation compared to lower angles of attack reflects the dominance of separated flow dynamics and the strong influence of stall hysteresis at this operating condition. The flatback airfoil demonstrates a noticeably higher NFR than the X-35-02 airfoil, which can be linked to its inherently smoother steady stall characteristics and greater capacity to sustain lift under partially separated conditions. As the DS-phase

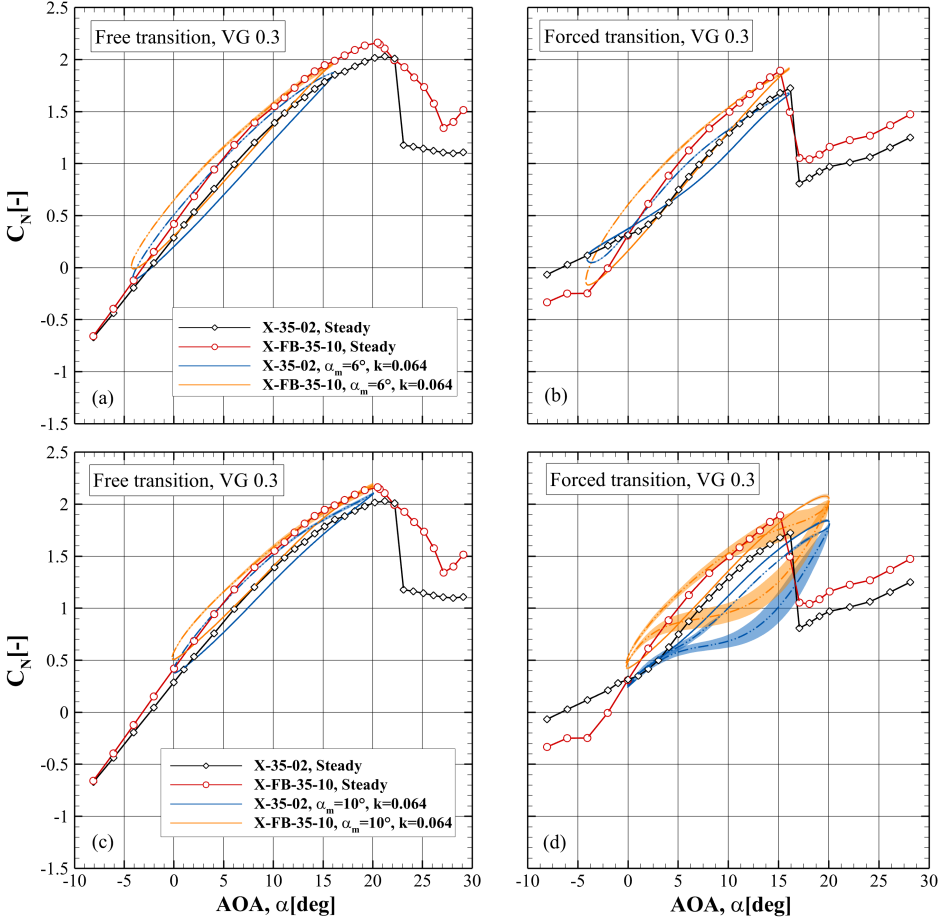


Figure 5.1: Comparison between the static and dynamic normal force coefficients in clean and with VGs configurations, [$Re = 1 \times 10^6$, X-35-02 and X-FB-35-10, $k = 0.064$].

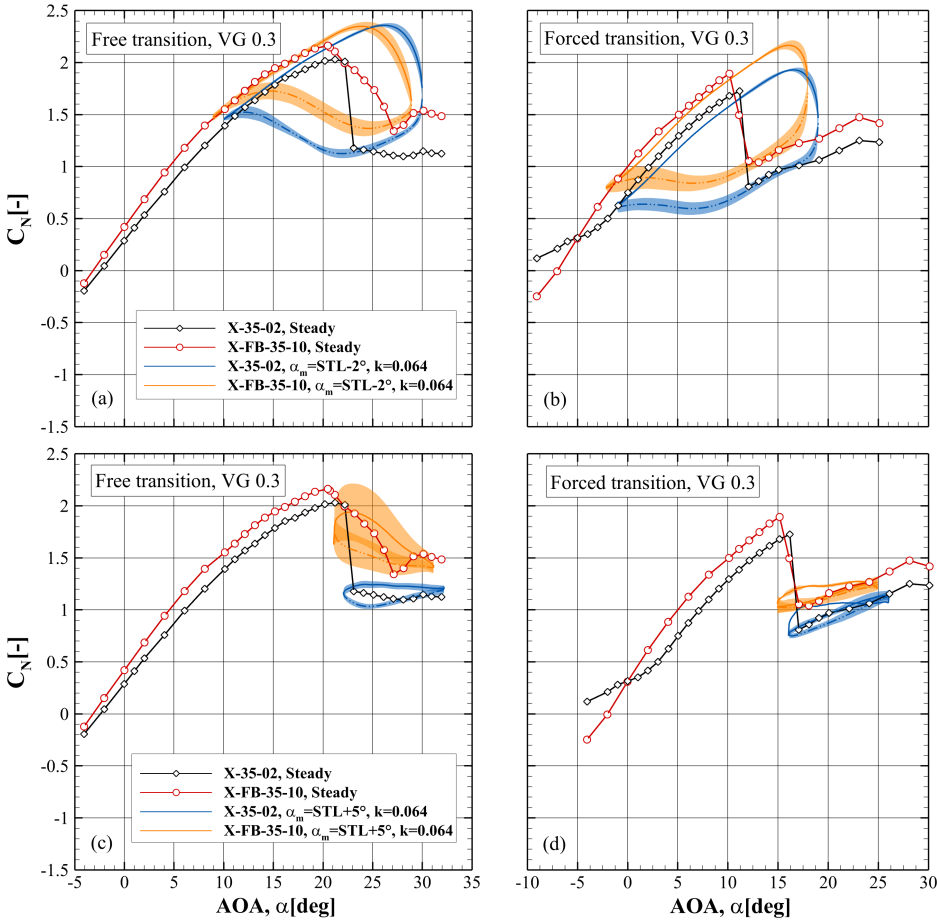


Figure 5.2: Comparison between the static and dynamic normal force coefficients in clean and with VGs configurations, [$Re = 1 \times 10^6$, X-35-02 and X-FB-35-10, $k = 0.064$].

progresses toward the BSA, partial flow reattachment occurs, although the recovery is incomplete and highly sensitive to instantaneous flow conditions. Due to the unstable nature of the separated flow, the size and position of the separation bubble at the BSA vary from cycle to cycle. These variations influence the development of the bubble during the subsequent US-phase, often resulting in bifurcated flow structures over the suction surface, which introduces additional complexity to the aerodynamic response and amplifies unsteady load fluctuations. A comparison with the baseline configuration without VGs reveals contrasting trends for the two airfoils. For the flatback airfoil, the presence of VGs increases the NFR, indicating an increase load fluctuations under oscillatory stall conditions. Conversely, for the X-35-02 airfoil, the NFR decreases when VGs are installed, suggesting a larger flow separation around the airfoil in this condition. Furthermore, the

MDNS exhibits opposite behavior: it increases for the X-35-02 airfoil but decreases for the flatback airfoil. Finally, the results indicate that when the BSA approaches the steady stall angle of attack, a reduction in the size of the separation bubble during the US-phase can lead to a slight increase in the dynamic C_N . This phenomenon shows the sensitivity of post-stall aerodynamics to local flow reattachment and vortex dynamics, which are strongly influenced by VGs placement and airfoil geometry.

Figure 5.1 (b) presents the dynamic hysteresis loops of the normal force coefficient for both airfoils at a mean angle of attack of $\alpha_m = 6^\circ$ with VGs, in forced transition flow regime. For the flatback airfoil, the TSA and BSA are positioned very close to the steady-state stall angle of attack. This proximity results in a dynamic response that closely resembles the behavior observed under free transition conditions at the same mean angle of attack. The hysteresis loop remains relatively stable and oval-shaped, indicating that the flow remains largely attached throughout the oscillatory cycle, with only minor deviations near the stall boundary. In contrast, the X-35-02 airfoil exhibits a distinctly different aerodynamic behavior. During the DS-phase, the BSA surpasses the steady negative stall angle of attack, initiating a negative dynamic stall event. This phenomenon transforms the shape of the hysteresis loop into a characteristic figure-eight pattern, which is indicative of alternating phases of flow separation and reattachment. The occurrence of negative dynamic stall significantly reduces both the MDNS and the NFR for the X-35-02 airfoil compared to the flatback configuration. Despite the onset of stall, the transition remains remarkably smooth, with minimal fluctuations in aerodynamic forces. This is mainly due to the steady negative stall mechanism of this airfoil. As the angle of attack decreases, a large separation bubble rapidly forms between the trailing edge and the region of maximum thickness on the pressure side of the airfoil. Interestingly, the size of this separation bubble remains relatively constant with decreasing the angle of attack. Consequently, the normal force coefficient declines at a nearly constant rate, reflecting a stable but separated flow condition. At significantly lower angles of attack, typically between $\alpha = -20^\circ$ and $\alpha = -30^\circ$, a second negative stall event occurs, this time initiated by leading edge flow separation. As a result of the smooth nature of the negative stall of this airfoil, where only the lift slope is altered, the fluctuation of the negative dynamic stall force remains low during both the dynamic stall and recovery phases. This explains the very low standard variations of the normal force coefficient during the negative dynamic stall.

The dynamic cycles of the normal force coefficient for both airfoils at a mean angle of attack of $\alpha_m = 10^\circ$, with VGs, in forced transition flow regime are shown in figure 5.1 (d). At this condition, both airfoils exhibit dynamic stall cycles, although the nature of stall remains relatively smooth compared to abrupt stall events typically observed at higher angles of attack. During the US-phase, the flow over both airfoils begins in an attached state and remains attached until reaching the TSA. This indicates that the onset of the stall is delayed and does not occur suddenly, which is consistent with the stabilizing influence of VGs. The absence of sharp force drops during the US-phase suggests that the pressure adaptation lag, rather than large-scale flow separation, governs the aerodynamic response

in this regime. As the DS-phase begins, bifurcation flow becomes evident, particularly over the suction surface of both airfoils. For the flatback airfoil, flow recovery during some cycles occurs rapidly, causing the dynamic C_N to rise above the steady-state polar at the early stages of the DS-phase, indicating a very small trailing edge separation on some cycles. The smooth stall behavior is linked to the trailing edge separation bubble size that forms at the onset of the DS-phase but remains confined downstream of the VGs. The vortices generated by the VGs act as a barrier, preventing the separated region from expanding fully over the airfoil surface. Consequently, the dynamic stall process remains smooth. Toward the end of the DS-phase, the flow starts to reattach, leading to a noticeable increase in C_N before reaching the BSA.

At a mean angle of attack of $\alpha_m = STL - 2^\circ$, under forced transition flow conditions with VGs, as it is shown in figure 5.2 (b), both airfoils exhibit dynamic stall behavior. In both airfoils, flow separation initiates slightly before reaching the TSA. However, in contrast to the free transition cases, the dynamic normal force coefficient decreases gradually during the stall. This smoother stall progression is primarily linked to the lower stall angle of attack observed under forced transition conditions, which results in reduced maximum lift and a weaker adverse pressure gradient over the suction surface near the TSA. On the other hand, the presence of VGs helps stabilize the boundary layer, delaying full separation and contributing to a more controlled aerodynamic response. Notably, both the NFR and the MDNS are higher in the forced transition cases than in their free transition counterparts, which results in a greater dynamic load variation. At post-stall conditions, i.e., $\alpha_m = STL + 5^\circ$ in figure 5.2 (d), the dynamic hysteresis loops for both airfoils exhibit a clockwise orientation and remain closely aligned with the steady-state polar curves. This behavior is indicative of large-scale flow separation dominating the cycle, with limited reattachment during the DS-phase. A prominent separation bubble forms over the suction surface of both airfoils, suppressing lift recovery and constraining the dynamic response. Although the overall flow remains highly separated, a slight bifurcation flow effect is observable in both airfoils, pointing to the onset of small-scale flow instabilities and vortex dynamics.

Figures 5.3 and 5.4 present the pressure coefficient distributions for the X-35-02 and X-FB-35-10 airfoils equipped with VGs positioned at 30% of the chord length. The data correspond to three representative mean angles of attack of $\alpha_m = 10^\circ$, $\alpha_m = STL - 2^\circ$, and $\alpha_m = STL + 5^\circ$, at a reduced frequency of $k = 0.064$, under both free and forced transition flow regimes. For each case, pressure distributions are shown at three distinct points within the US and DS-phases: one-quarter, one-half, and three-quarters of the respective cycle. These snapshots provide detailed insight into the evolution of the flow-field around the airfoils during dynamic oscillations and help explain the behavior of the dynamic normal force coefficient under varying aerodynamic conditions.

At a mean angle of attack of $\alpha = 10^\circ$ in forced transition flow, both airfoils exhibit attached flow during the US-phase, as indicated by the pressure gradients and well-defined suction peaks. Note that, for the X-35-02 airfoil, a localized flow separation is observed on the pressure side at $\alpha = 5^\circ$, corresponding to the lower part of the US-phase. This

separation is caused by a combination of a steep adverse pressure gradient and a thick, turbulent boundary layer, which together reduce the lift generation. As the angle of attack increases, this separation is eliminated, and the flow reattaches, restoring lift. At $\alpha = 5^\circ$, no trailing edge separation is observed on either airfoil during the US-phase. The suction peak is lower than in the corresponding steady-state pressure distribution, which can be attributed to pressure adaptation lag and a reduction in relative velocity over the suction surface due to the pitching motion. This explains why the dynamic C_N remains below the static value at this point in the cycle. As the angle of attack increases throughout the US-phase, the suction peak gradually intensifies, reflecting the increasing lift. During the DS-phase, the pitching-down motion of the airfoil reduces the relative velocity on the pressure side, leading to a decrease in pressure coefficient. This trend is clearly visible in the DS-phase C_p distributions. Toward the end of the US-phase and into the DS-phase, the combination of angle of attack stagnation and a strong adverse pressure gradient causes flow separation to migrate from the trailing edge toward the leading edge on both airfoils. However, this separation remains confined downstream of the VGs location, forming a flat pressure plateau that extends from the trailing edge to the VGs position. Note that despite the presence of flow separation on the suction side of the airfoil, the suction peak remains high. This low pressure area is extended up to the VGs chord position. This, combined with the attached high pressure flow on the pressure side, slows the rate of lift loss during stall, as previously illustrated in figure 5.1 (d). As the angle of attack decreases further during the DS-phase, the size of the separated region gradually shrinks. Notably, the flatback airfoil exhibits a higher suction peak during the DS-phase compared to the X-35-02 airfoil, indicating its ability to retain more lift under partially separated conditions.

At a mean angle of attack of $\alpha_m = STL - 2^\circ$ under free transition flow conditions, the pressure coefficient distributions reveal important details about the flow evolution during the dynamic cycle. During the US-phase, the suction peak gradually intensifies and shifts toward the leading edge as the angle of attack increases for both airfoils. This movement reflects the growing adverse pressure gradient and the tendency of the boundary layer to accelerate toward the stall onset region. Both airfoils exhibit the formation of a suction pressure peak plateau upstream of the VGs, yet despite the presence of a very steep pressure gradient, on the order of 5–6, at the end three-quarter of the US-phase, no trailing edge separation is observed. This indicates that the VGs effectively stabilize the boundary layer and delay the onset of large-scale flow detachment. It is noteworthy that the pressure gradient aft of the VGs and the suction peak on the X-35-02 airfoil are slightly higher than those of the flatback airfoil at the end of the US-phase, indicating a stronger local acceleration and thinner boundary layer on the sharp trailing edge configuration. As the cycle progresses into the DS-phase, the situation changes significantly. At the TSA, the very high suction side pressure gradient triggers rapid expansion of the separation bubble, which grows from the trailing edge toward the leading edge on both airfoils. By the beginning of the DS-phase, the bubble extends upstream of the VGs location, and its effect becomes clearly visible in the pressure coefficient distribution at one-quarter of the DS-phase. The

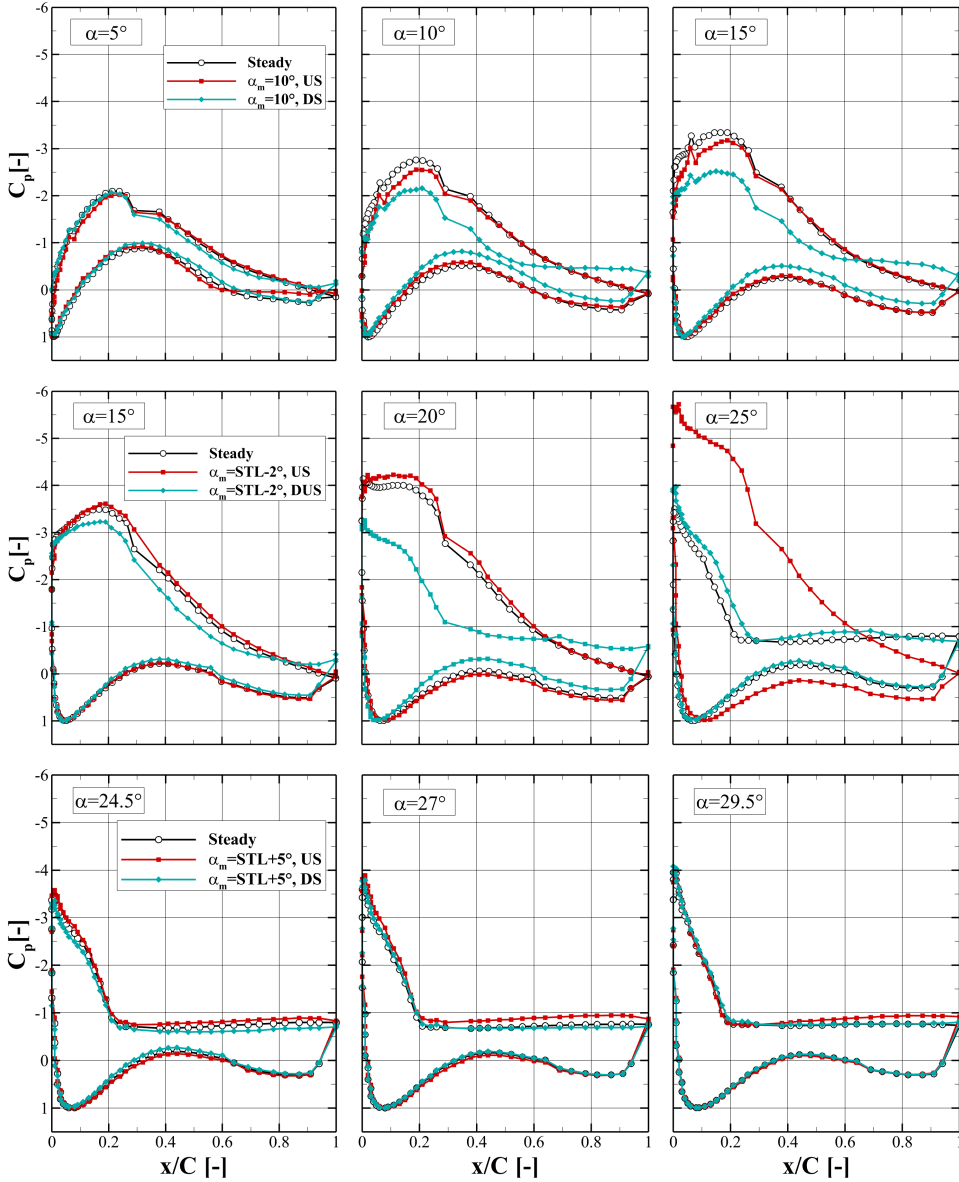


Figure 5.3: Steady and dynamic Pressure coefficient distribution comparison, [$Re = 1 \times 10^6$, X-35-02, $VGs_{(x/C)} = 0.3$, $k = 0.064$, Top: forced transition, Middle: free transition, Bottom: free transition].

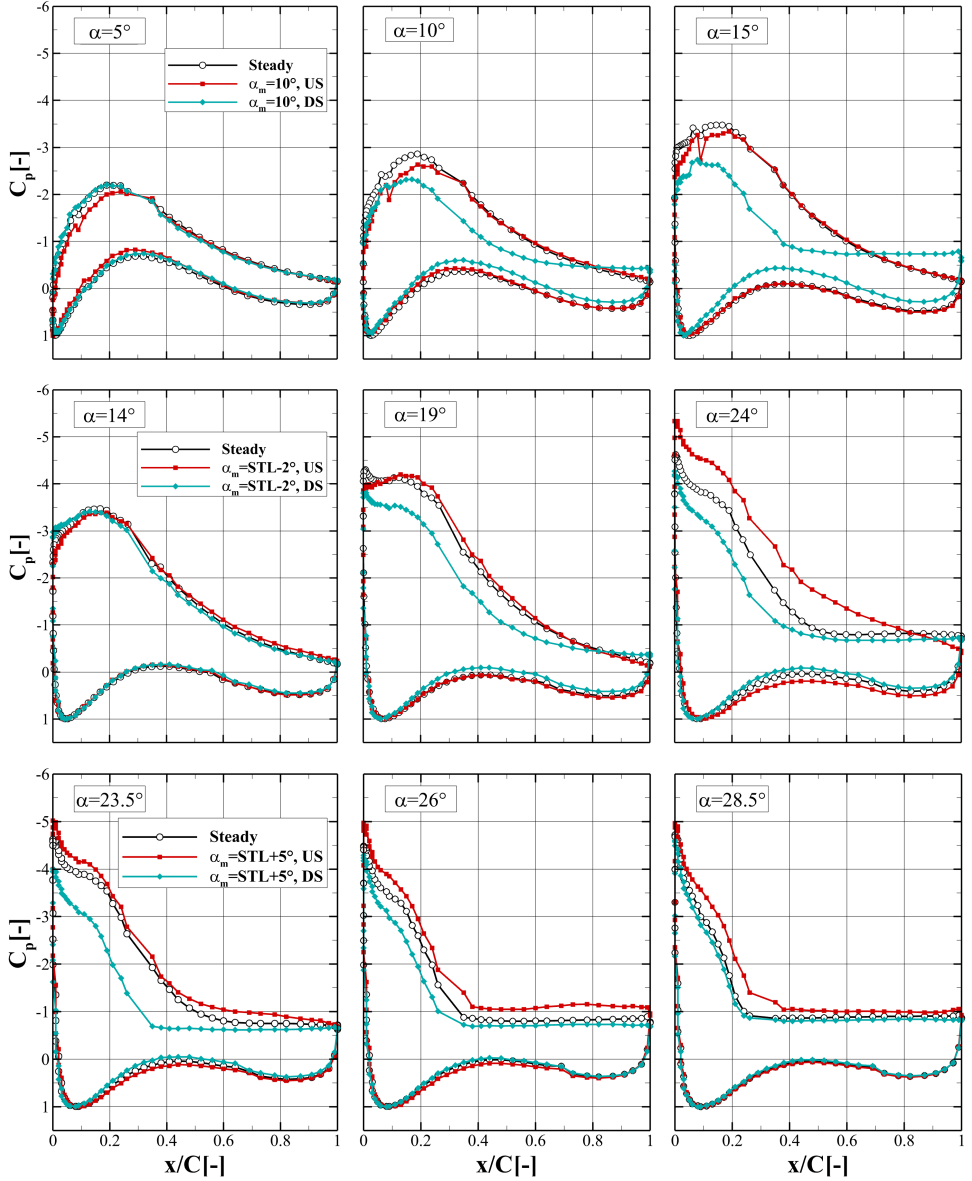


Figure 5.4: Steady and dynamic Pressure coefficient distribution comparison in forced transition flow regime, [$Re = 1 \times 10^6$, X-FB-35-10, $VGs_{(x/C)} = 0.3$, $k = 0.064$, Top: forced transition, Middle: free transition, Bottom: free transition].

effectiveness of the VGs is visible in the pressure distribution as the flow starts to reattach to the surface of the airfoils at the lower part of the DS-phase. This reattachment process is particularly effective for the flatback airfoil, which exhibits faster recovery compared to the X-35-02 airfoil. The difference can be linked to the lower adverse pressure gradient on the suction side of the flatback airfoil, as confirmed by the comparative C_p distributions at the mean angle of attack. As it is observed, during the US-phase, VGs play a crucial role in delaying trailing edge separation and facilitating earlier reattachment in the DS-phase. This results in enhanced aerodynamic stability and decreased unsteady load fluctuations. The faster recovery observed on the flatback airfoil indicates that the effectiveness of VGs is significantly influenced by airfoil geometry. Thicker trailing edge designs benefit from reduced pressure gradients and more favorable reattachment characteristics.

At a mean angle of attack of $\alpha_m = STL + 5^\circ$ under free transition flow conditions, the C_p distributions for the X-35-02 airfoil reveal only minor differences between the steady and dynamic cases. This similarity indicates the presence of a large, stable separation bubble on the suction side of the airfoil, which dominates the flow-field throughout the oscillatory cycle. The size and position of this separation bubble remain essentially unchanged as the angle of attack increases or decreases, indicating that the flow is fully separated and insensitive to small variations in instantaneous angle of attack. The slight variations observed in the C_p distribution near the leading edge, on both the suction and pressure sides, can be attributed to changes in local relative velocity caused by the pitching motion of the airfoil during the US and DS-phases. In contrast, the X-FB-35-10 airfoil exhibits a different behavior under the same conditions. During the early part of the US-phase, the size of the separation bubble above the suction surface decreases slightly, as evident from the C_p distribution at $\alpha = 23.5^\circ$. Although this change appears modest, it has a significant impact on the aerodynamic response, particularly on the NFR and the amplitude of force fluctuations, as illustrated in figure 5.2 (c). From the mid-US-phase onward, the size of the separated region stabilizes and remains nearly constant for this specific cycle; however, due to flow instabilities occurring at the beginning of the US-phase, the extent of separation varies from cycle to cycle. This variability introduces substantial unsteady aerodynamic effects, resulting in higher force fluctuations and increased standard deviation in the dynamic C_N polars for the flatback airfoil compared to the X-35-02 configuration.

EFFECT OF THE REDUCED FREQUENCY

Figures 5.5, 5.6, 5.7, and 5.8 present the influence of reduced frequency on the dynamic normal force coefficient characteristics for the X-35-02 and X-FB-35-10 airfoils, both equipped with VGs positioned at 30% of the chord length. The results are shown for both free and forced transition flow regimes, allowing for a comparative analysis of unsteady aerodynamic behavior under varying oscillation rates.

For the X-35-02 airfoil at a mean angle of attack of $\alpha_m = 6^\circ$ in free transition flow as presented in figure 5.5 (a), the results indicate a clear increase in pressure adaptation lag with increasing reduced frequency. Although the TSA remains well below the static stall angle, the dynamic C_N decreases during the US-phase and increases during the DS-phase with increasing the reduced frequency. This behavior reflects the growing phase delay between the aerodynamic response and the instantaneous angle of attack. Unlike typical dynamic stall cases, the maximum C_N at TSA decreases with increasing reduced frequency, proving that higher oscillation rates suppress peak lift generation in the attacked part of the lift curve. Most part of the dynamic cycle remains within the linear portion of the normal force curve, with only the final quarter of the US-phase entering the nonlinear regime. Importantly, no signs of trailing edge flow separation or associated instabilities are observed at TSA, indicating stable flow conditions. The dynamic C_N at the BSA matches the static value only at a reduced frequency of $k = 0.032$. At higher frequencies, the pressure lag increases, causing the dynamic normal force to deviate from the steady-state value. As the reduced frequency increases, both the MDNS and the NFR decrease, indicating a dampening of aerodynamic responsiveness. Additionally, the ovality of the dynamic cycles increases, leading to a greater discrepancy between the US and DS-phase C_N values at any given angle of attack. A similar trend is observed for the X-FB-35-10 airfoil at the same mean angle of attack, as shown in figure 5.7 (a). The primary difference lies in the presence of slight C_N fluctuations during the first half of the DS-phase, which are attributed to the trailing edge vortex system inherent to the flatback geometry. These fluctuations introduce mild unsteadiness but do not significantly alter the overall cycle shape or aerodynamic performance. Comparing these results to the baseline cases without VGs at the same mean angle of attack show an improvement in increasing the reduced frequency response on the flow stability and reduction of force fluctuations during the DS-phase for both airfoils. The stabilizing effect of VGs is particularly beneficial at higher reduced frequencies. At a mean angle of attack of $\alpha_m = 10^\circ$, both airfoils exhibit similar dynamic cycle behavior in response to changes in reduced frequency. This trend is also observed for the flatback airfoil at $\alpha_m = 6^\circ$ under forced transition conditions. At $\alpha_m = 10^\circ$, nonlinear effects become more prominent during the first half of the DS-phase, especially for the X-35-02 airfoil. These effects are linked to the stronger pressure gradients encountered at higher angles of attack. However, increasing the reduced frequency mitigates these nonlinearities by enhancing flow stability over the suction side of the airfoil, thereby smoothing the dynamic response and reducing aerodynamic variability.

A similar behavior is observed for the X-35-02 airfoil at a mean angle of attack of $\alpha_m = 6^\circ$ under forced transition flow conditions, as illustrated in figure 5.5 (b). During the US-phase, increasing the reduced frequency does not significantly alter the overall cycle shape, but subtle changes in force development can be detected. However, during the DS-phase, a negative dynamic stall occurs. This effect becomes more pronounced as the reduced frequency increases, causing the onset of negative stall to shift toward lower angles of attack, which is mainly driven by the combined influence of pressure adaptation

lag and delayed expansion of the separation region on the pressure side of the airfoil, both of which intensify with increasing reduced frequency. An intriguing observation is that negative dynamic stall consistently occurs at angles of attack below the steady-state negative stall angle for all tested reduced frequencies. This behavior can be linked to the distinctive “S-shaped” contour of the pressure side, which induces a high sensitivity of flow separation to leading edge boundary layer tripping. Under dynamic conditions, this sensitivity is amplified, making the airfoil particularly susceptible to early onset of negative stall. Depending on the intended application, this characteristic may represent a critical design consideration, as it could influence both aerodynamic performance and structural loading under oscillatory inflow conditions.

Dynamic stall occurs on both airfoils when the mean angle of attack is increased to

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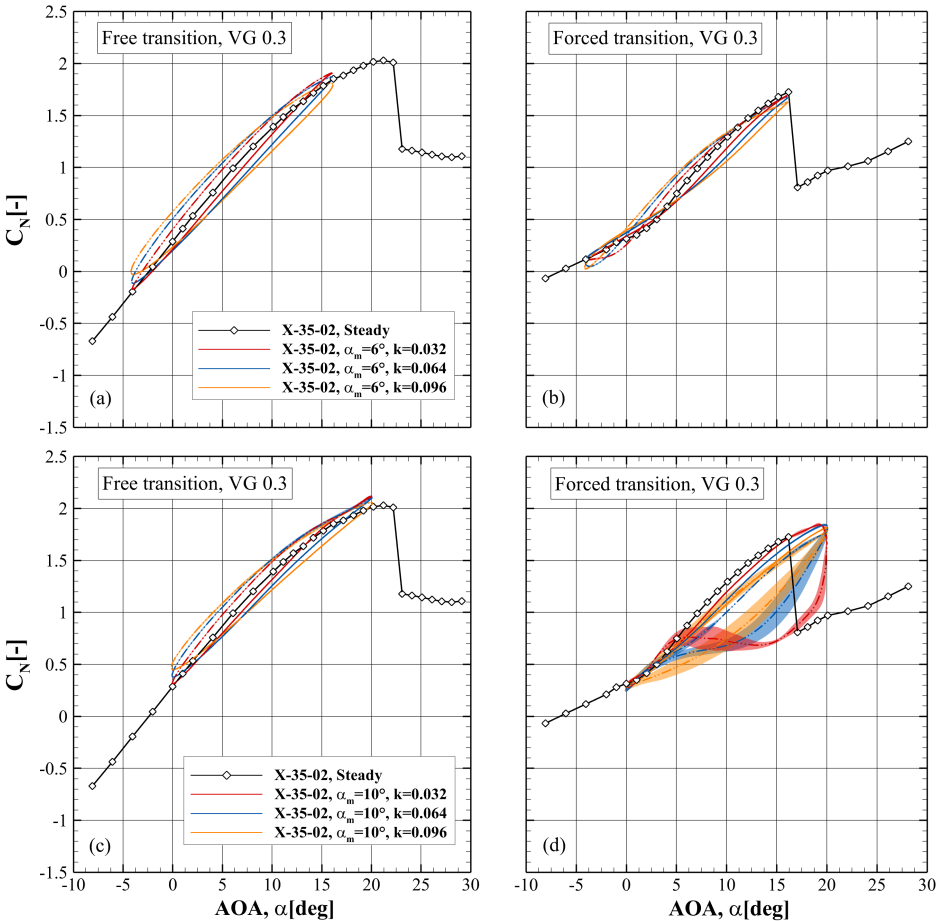


Figure 5.5: Effect of the reduced frequency on the normal coefficient in free and forced transition flow regimes, [$Re = 1 \times 10^6$, X-35-02, $VGs_{(x/C)} = 0.3$].

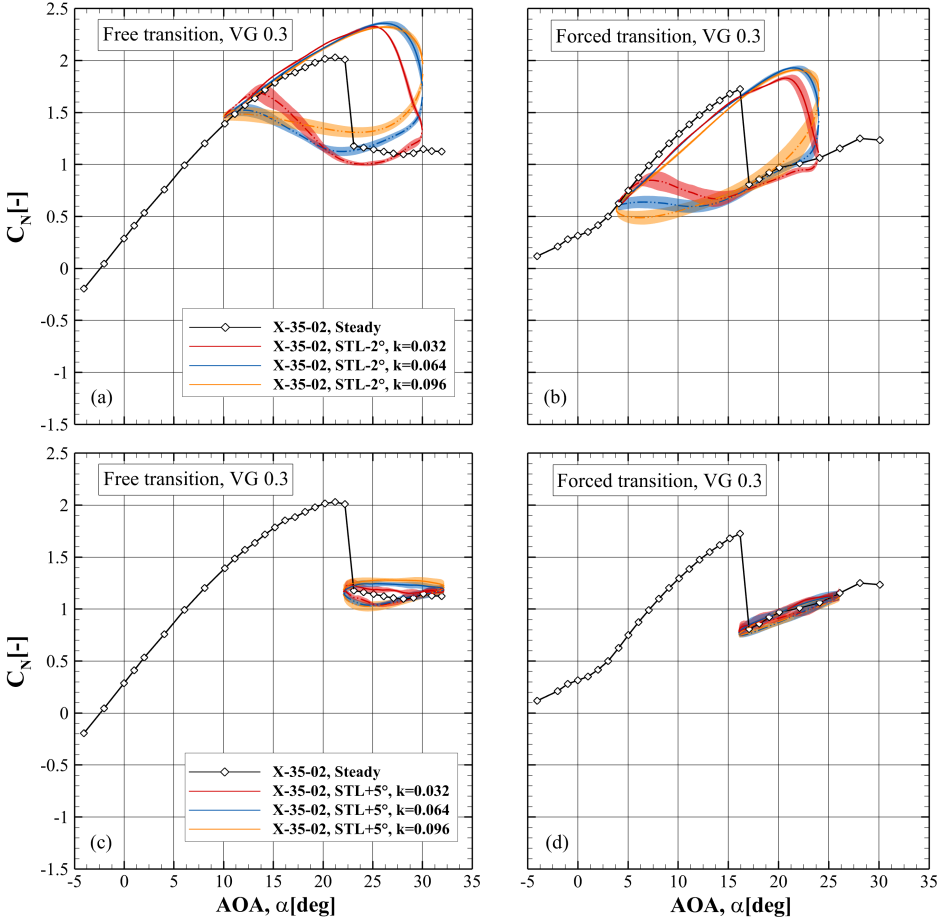


Figure 5.6: Effect of the reduced frequency on the normal coefficient in free and forced transition flow regimes, $[Re = 1 \times 10^6, X-35-02, VG_{s(x/C)} = 0.3]$.

$\alpha_m = STL-2^\circ$ under free transition flow conditions, as shown in figures 5.6 (a) and 5.8 (a). At this operating point, the TSA reaches approximately 30° , accompanied by a very steep adverse pressure gradient on the suction side and a relatively low pressure adaptation lag. These factors combine to produce the most abrupt drop in C_N at the lowest reduced frequency $k = 0.032$. In this case, the onset of dynamic stall occurs before the TSA is reached and the dynamic stall angle of attack is the lowest among the other reduced frequencies and the dynamic C_N continues to decrease well below the static post-stall polar curve, resulting in a significantly higher NFR. Similar to the behavior observed in configurations without VGs, the severity of stall decreases as the reduced frequency increases. The stall transition becomes smoother, and the hysteresis loops exhibit less abrupt force variation. Interestingly, the maximum C_N overshoot occurs at $k = 0.064$, which can be explained

by the reduction in the dynamic C_N slope during the US-phase as the reduced frequency increases. Furthermore, increasing the reduced frequency shifts the dynamic stall angle of attack to higher values, with the maximum dynamic stall angle observed at $k = 0.096$. This trend indicates that higher pitching rates delay stall onset and partially stabilize the flow. Another important observation is that, similar to the no-VGs configuration, increasing the reduced frequency delays the reattachment phase during the DS-phase. However, the presence of VGs mitigates some of the adverse effects by confining separation and promoting partial reattachment earlier in the cycle. A particularly noteworthy effect is the reduction of flow instabilities and force fluctuations during the recovery phase after dynamic stall, which becomes evident at higher reduced frequencies. On the other hand, this stabilizing effect can only be seen for the flatback airfoil. Nevertheless, from the onset of dynamic stall to the TSA, the flatback airfoil exhibits higher force fluctuations compared to the sharp trailing edge X-35-02 airfoil. These fluctuations, however, diminish with increasing reduced frequency, thus the combined benefits of VG-induced flow control and higher oscillation rates in improving aerodynamic stability.

Dynamic stall occurs for both airfoils under forced transition flow conditions at mean angles of attack of $\alpha_m = 10^\circ$ and $\alpha_m = STL - 2^\circ$. Overall, the influence of reduced frequency exhibits similar trends in both cases, as shown in figures 5.5 (d), 5.6 (b), 5.7 (d), and 5.8 (b). Nevertheless, some subtle differences emerge when comparing the results at $\alpha_m = 10^\circ$ to those at $\alpha_m = STL - 2^\circ$. At $\alpha_m = 10^\circ$, dynamic stall initiates near the TSA, and increasing the reduced frequency slightly decreases the maximum overshoot of the dynamic normal force coefficient. Interestingly, in these cases, the dynamic C_N remains below the steady-state value during the US-phase, which contrasts with the behavior observed at $\alpha_m = STL - 2^\circ$ for both airfoils. Additionally, the amplitude of force fluctuations during flow reattachment in the second half of the DS-phase is significant only at a reduced frequency of $k = 0.032$ and disappears at higher reduced frequencies. This trend indicates that the characteristic frequency of separation bubble oscillations changes during the reattachment process as the reduced frequency increases. Furthermore, bifurcation flow is observed for both airfoils at $\alpha_m = 10^\circ$ and the reduced frequency of $k = 0.064$ and $k = 0.096$ during the DS-phase, which indicates the unstable formation of the separation bubble at TSA. For the $\alpha_m = STL - 2^\circ$ cases, the dynamic C_N typically drops to the steady-state value immediately after dynamic stall occurs at low and intermediate reduced frequencies. Similar to clean configurations under forced transition, the dynamic C_N then follows the slope of the steady polar until reattachment begins. In these cases, increasing the reduced frequency results in a steeper MDNS, indicating a stronger coupling between unsteady aerodynamic forces and oscillatory motion. When comparing the forced transition results of the flatback airfoil with VGs to cases without VGs at $\alpha_m = 10^\circ$ and $\alpha_m = STL - 2^\circ$, a noticeable increase in force fluctuation amplitude during the DS-phase reattachment is observed across all reduced frequencies. This behavior can be linked to the higher pressure gradients induced by VGs compared to clean configurations. Elevated pressure gradients during dynamic stall enlarge the separation bubble and accelerate the rate of flow separation expansion, both of which

amplify unsteady force fluctuations.

For the X-35-02 airfoil at a mean angle of attack of $\alpha_m = STL+5^\circ$ under free transition flow conditions, the steady stall behavior with VGs remains abrupt. Consequently, the separation bubble located on the suction side near the bottom stagnation angle is not significantly influenced by the presence of VGs. When the reduced frequency is increased, a slight enhancement in the ovality of the clockwise dynamic stall cycles is observed. This modification marginally affects the NFR. In addition, a higher reduced frequency tends to diminish cycle instability and reduce the force fluctuations, as illustrated in figure 5.6 (c). Under forced transition flow conditions for the same airfoil and angle of attack, the influence of reduced frequency exhibits a similar trend to cases without VGs. Due to the presence of a large separated flow region, the dynamic stall cycles appear relatively flat, and

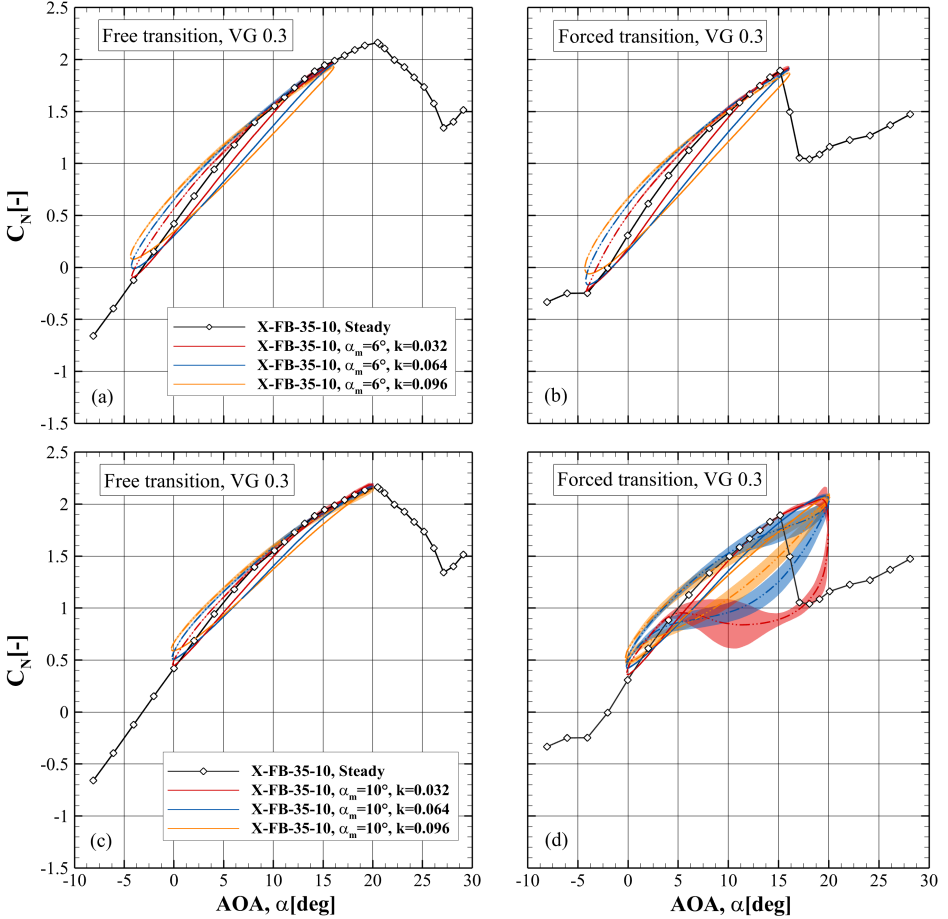


Figure 5.7: Effect of the reduced frequency on the normal coefficient in free and forced transition flow regimes, [$Re = 1 \times 10^6$, X-FB-35-10, $VGs_{(x/c)} = 0.3$].

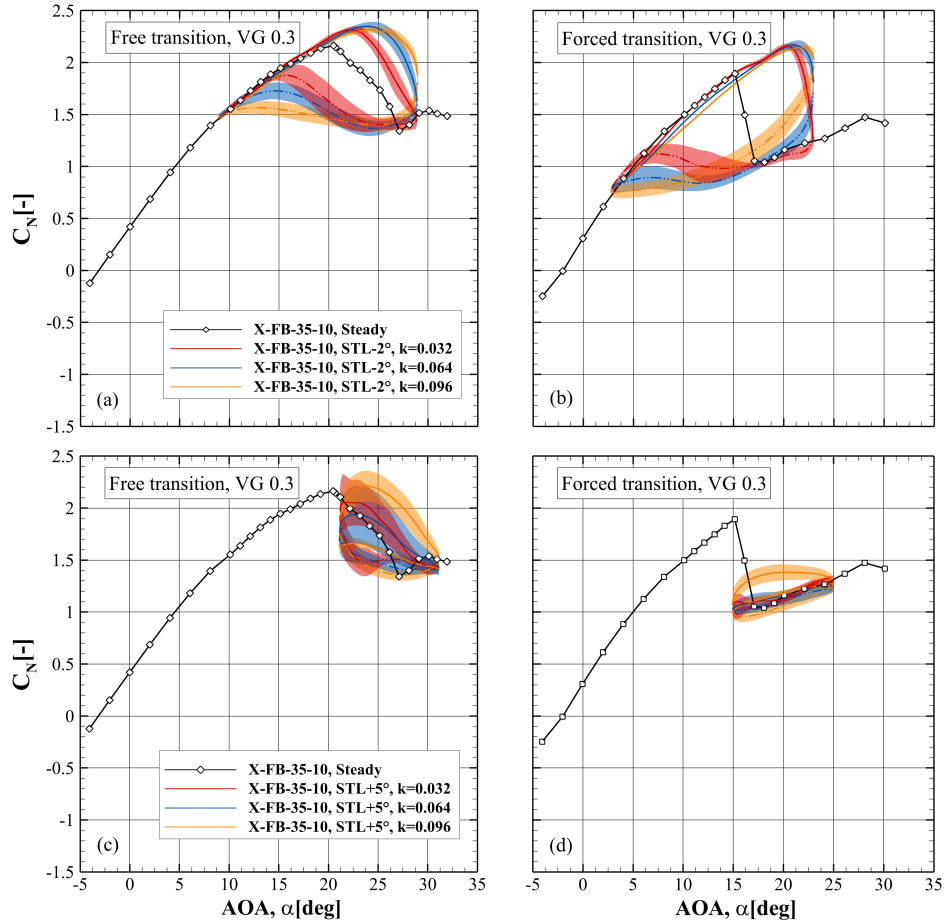


Figure 5.8: Effect of the reduced frequency on the normal coefficient in free and forced transition flow regimes, $[Re = 1 \times 10^6, \text{X-FB-35-10}, VG_{s(x/c)} = 0.3]$.

variations in reduced frequency do not significantly alter their shape, as shown in figure 5.6 (d). For the flatback airfoil, the steady stall behavior with VGs is considerably smoother in free transition flow compared to the sharp trailing edge configuration. As a result, at $\alpha_m = STL + 5^\circ$ the dynamic stall cycles exhibit a larger normal force range than those of the X-35-02 airfoil, as presented in figure 5.8 (c). At the BSA, the separation bubble on the suction side reaches its minimum size, which leads to an increase in the dynamic normal force coefficient. Increasing the reduced frequency delays the expansion of the separation bubble, allowing a larger portion of the flow to remain attached for a longer duration. This delay results in a higher dynamic C_N and shifts the angle of attack corresponding to the maximum dynamic C_N toward higher values. Consequently, the ovality of the dynamic stall cycle increases with reduced frequency. Furthermore, the amplitude of force

fluctuations grows with increasing reduced frequency, particularly during the US-phase, due to instabilities within the separation bubble. However, once the separation bubble reaches its maximum size near the TSA, the subsequent changes during the DS-phase are relatively minor. This behavior is primarily governed by flow reattachment hysteresis and lag effects, which are not strongly influenced by the reduced frequency. The flatback airfoil under forced transition flow conditions behaves similarly to the X-35-02 airfoil in free transition flow, owing to the comparable steady stall characteristics, as shown in figure 5.8 (d). It should be noted that the bifurcation phenomenon observed during the US-phase occurs at a reduced frequency of $k = 0.096$ for the flat back airfoil in both free and forced transition cases.

EFFECT OF THE VGs CHORD POSITION

5

Figure 5.9 illustrates the influence of VGs chordwise position on the dynamic normal force coefficient behavior of the X-35-02 airfoil under free transition flow conditions at a mean angle of attack of $\alpha_m = 6^\circ$ for two reduced frequencies of $k = 0.032$ and $k = 0.096$, which represent the lower and upper bounds of the investigated dynamic range. Given the significant role that reduced frequency plays in governing unsteady aerodynamic phenomena, the effect of VGs positioning is systematically evaluated at both extremes. This approach enables a comprehensive assessment of how VGs placement interacts with dynamic flow conditions to capture the sensitivity of the airfoil's dynamic response to VGs location under varying degrees of flow unsteadiness. Note, the same color coding used for the dynamic cycles is also applied to the corresponding steady-state polars for each VGs chord position.

Across all VGs configurations, the dynamic cycles remain confined within the attached portion of the polar curve, indicating that flow separation is effectively suppressed. As the VGs are positioned closer to the leading edge, a noticeable reduction in the dynamic C_N is observed during both the US and DS-phases of the cycles. This trend correlates strongly with the behavior of the steady-state lift curves. Forward placement of the VGs leads to a decrease in the steady lift coefficient slope, which in turn influences the dynamic response by lowering the peak C_N attained at the TSA. Consequently, the overall NFR is diminished. Among the tested configurations, the most pronounced effect is observed when the VGs are located at 20% of the chord length, where the reduction in dynamic C_N and NFR is most significant. A similar dynamic response pattern is also evident at a higher mean angle of attack of $\alpha_m = 10^\circ$ for the X-35-02 airfoil, as well as for the flap-back variant at $\alpha_m = 10^\circ$, both under free transition flow conditions, as presented in figure 5.11 (a) and (b).

For the flatback airfoil, the dynamic behavior at a mean angle of attack of $\alpha_m = 10^\circ$ shows notable differences compared to the X-35-02 airfoil, as illustrated in figures 5.11 (c) and (d). These differences arise primarily from the more complex vortex system generated

near the blunt trailing edge, which significantly influences the unsteady flow characteristics. At this operating condition, the TSA lies close to the steady stall angle of attack, making the onset of flow instabilities particularly evident for the weakest VGs configuration, namely, VGs positioned at 40% of the chord and a reduced frequency of $k = 0.032$. Under these circumstances, pronounced oscillations and irregularities in the dynamic normal force coefficient can be observed at the TSA. As the VGs are shifted toward the leading edge, these flow instabilities progressively diminish and eventually disappear at the TSA, suggesting that forward VGs placement enhances flow attachment and delays the onset of dynamic stall. However, the influence of VGs chordwise position becomes less pronounced as the reduced frequency increases. At higher reduced frequencies, the unsteady aerodynamic response is dominated by rapid oscillatory effects, which tend to overshadow the stabilizing contribution of VGs, thereby reducing their overall effectiveness in controlling flow separation.

Under forced transition flow conditions, the stall angle of attack is notably lower, which reveals more nuanced effects of VGs chordwise placement. The results for the X-35-02 airfoil at a mean angle of attack of $\alpha_m = 6^\circ$, as shown in figures 5.10 (a) and (b), demonstrate that shifting the VGs toward the leading edge effectively suppresses the onset of dynamic stall. This suppression leads to improved flow attachment and a significant reduction in force fluctuations, thereby enhancing aerodynamic stability. Additionally, the results indicate that the sensitivity of load fluctuations to VGs positioning, in case of dynamic stall occurrence, diminishes at higher reduced frequencies, proving that unsteady flow dynamics become increasingly dominant in such regimes. At a higher mean angle of attack of $\alpha_m = 10^\circ$, the influence of VGs chordwise location becomes more pronounced, as illustrated in figures 5.10 (c) and (d). Forward VGs placement results in an increase

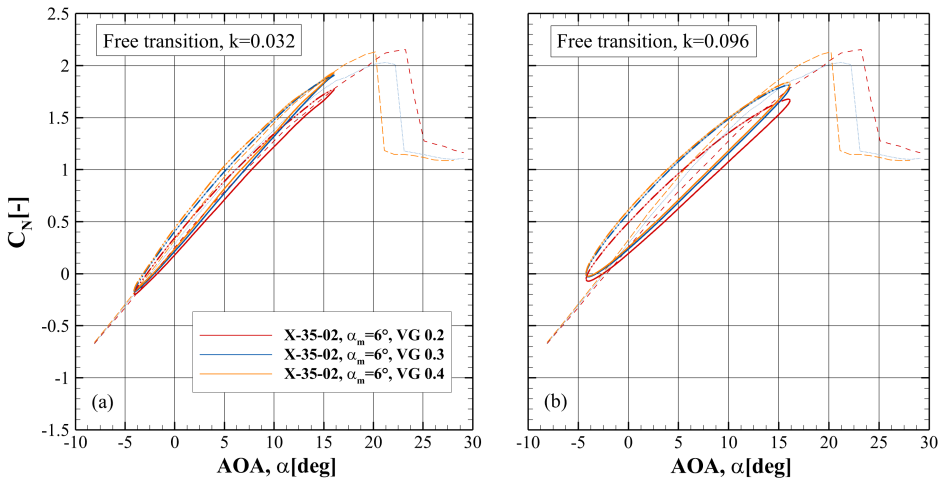


Figure 5.9: Effect of the VGs chord position on the normal coefficient in free transition flow regimes, $[Re = 1 \times 10^6, \text{X-35-02}, \alpha_m = 6^\circ]$.

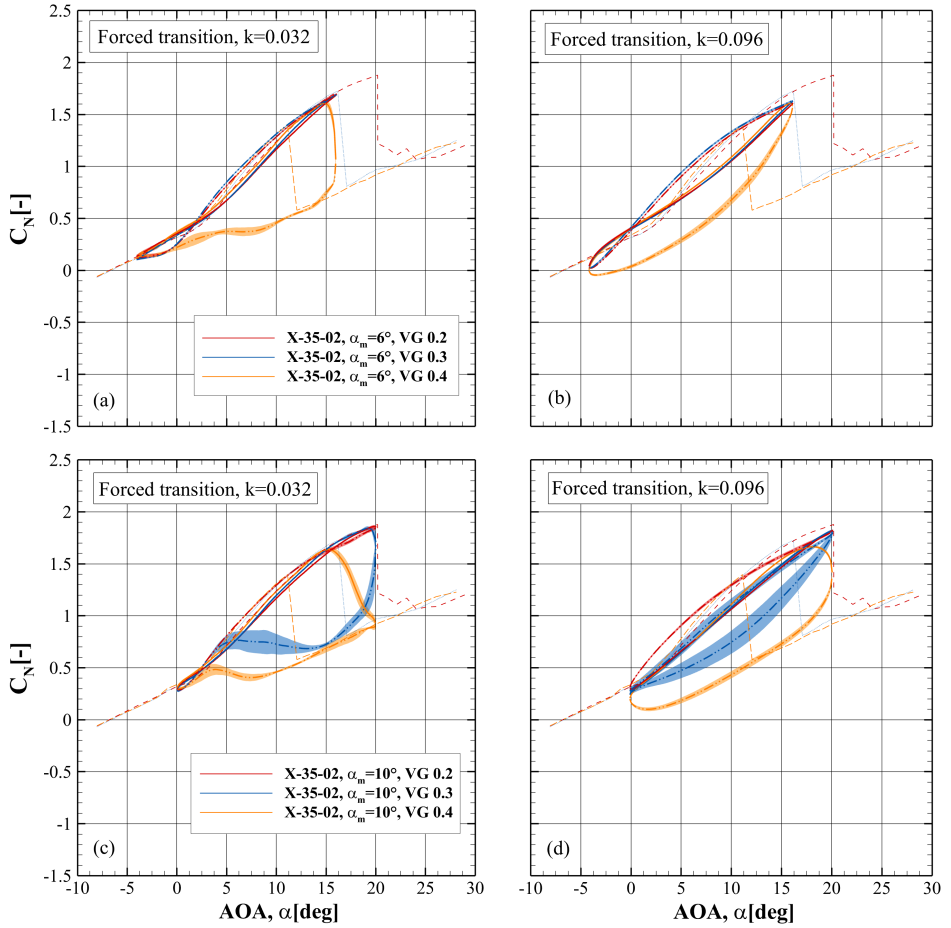


Figure 5.10: Effect of the VGs chord position on the normal coefficient in forced transition flow regimes, [$Re = 1 \times 10^6$, X-35-02, $\alpha_m = 6^\circ$ and $\alpha_m = 10^\circ$].

in the dynamic stall angle and promotes earlier flow reattachment during the DS-phase, compared to configurations with VGs located further aft. Notably, the recovery phase exhibits increased force fluctuations for the 30% chord VGs position relative to the 40% case. This behavior is due to the dynamics of the separation bubble, which undergoes repeated contraction and expansion during reattachment. At a reduced frequency of $k = 0.096$, dynamic stall is effectively eliminated for the 30% VGs configuration; however, elevated flow instability persists, indicating residual separation above the airfoil surface. A similar trend is observed for the flatback airfoil, as depicted in figure 5.12. Although the underlying flow-field differs due to the blunt trailing edge, certain VGs configurations yield reduced force fluctuations compared to the X-35-02 airfoil, highlighting the influence of airfoil geometry on the effectiveness of passive flow control strategies. Note that for both airfoils

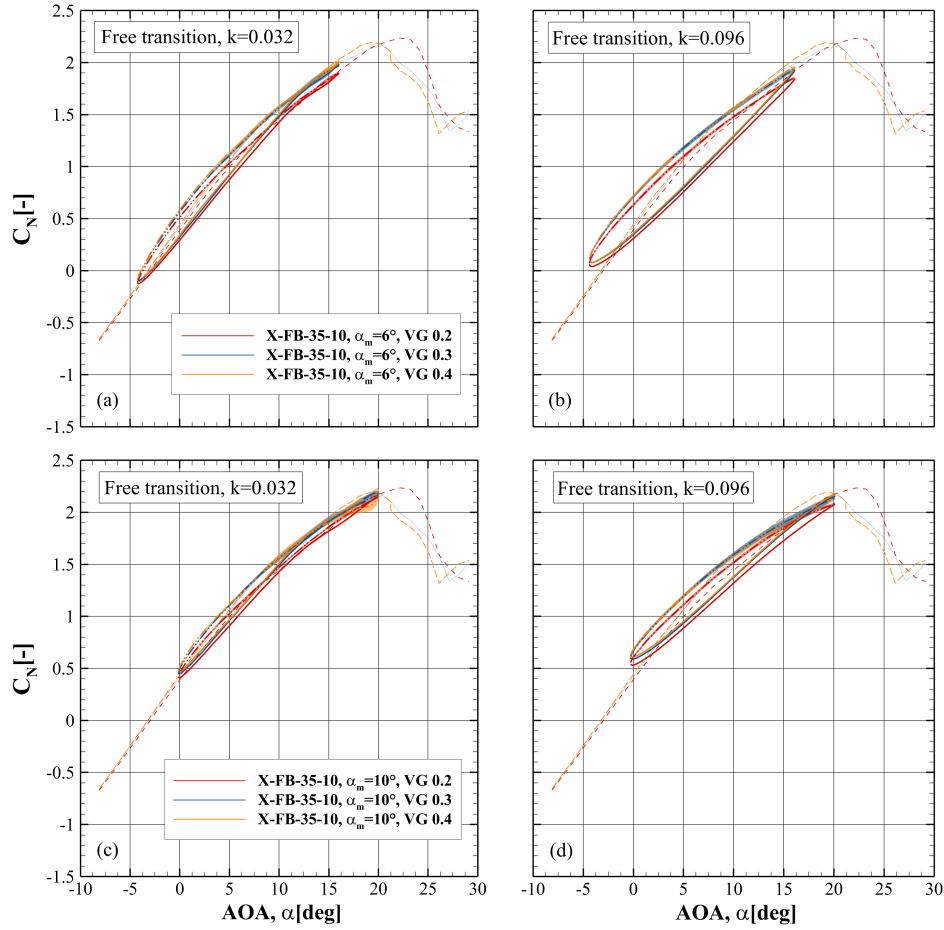


Figure 5.11: Effect of the VGs chord position on the normal coefficient in free transition flow regimes, [$Re = 1 \times 10^6$, X-FB-35-10, $\alpha_m = 6^\circ$ and $\alpha_m = 10^\circ$].

at a mean angle of attack of $\alpha_m = 10^\circ$ under forced transition conditions, bifurcated flow appears only in the 30% VGs configuration at $k = 0.096$.

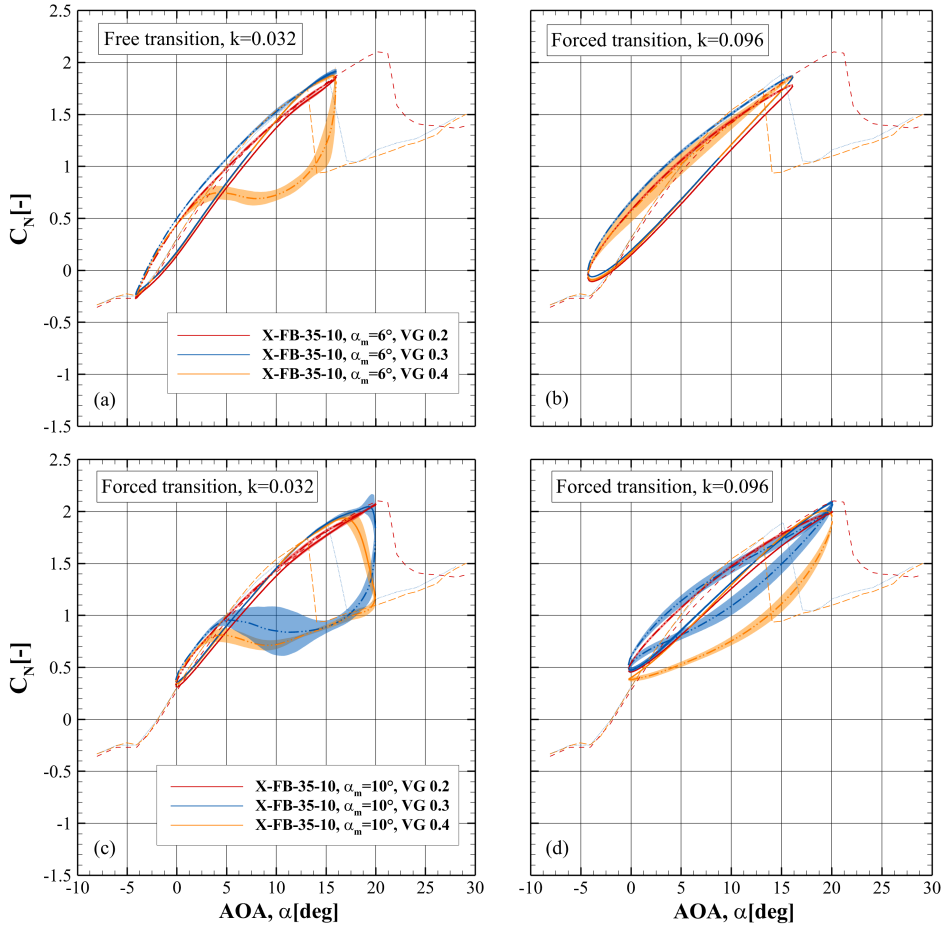


Figure 5.12: Effect of the VGs chord position on the normal coefficient in forced transition flow regimes, [$Re = 1 \times 10^6$, X-FB-35-10, $\alpha_m = 6^\circ$ and $\alpha_m = 10^\circ$].

5.3 CONCLUSIONS

The primary aim of this chapter was to examine the influence of VGs on the dynamic stall behavior of thick wind turbine airfoils, including both flatback and non-flatback configurations. Experimental investigations were carried out in the LTT wind tunnel of the TU Delft. Two representative airfoil geometries, X-35-02 and X-FB-35-10, were subjected to a series of tests under varying conditions. These included different mean angles of attack, a range of reduced frequencies, multiple chordwise positions of VGs, and both free transition and forced transition flow regimes. The aerodynamic performance was quantified through detailed surface pressure measurements, facilitating the determination of aerodynamic

coefficients and providing insight into the unsteady aerodynamic response associated with dynamic stall.

The dynamic stall measurements indicate that the X-FB-35-10 flatback airfoil exhibits considerably more stable dynamic hysteresis loops within the attached flow region of the lift curve. This enhanced stability can be observed via a lower standard deviation of the lift coefficient, a more pronounced lift overshoot during the US-phase, and a steeper mean dynamic lift slope. These aerodynamic characteristics are mainly attributed to the superior suction-side pressure gradient associated with the flatback geometry, which promotes delayed flow separation and improved flow attachment under unsteady conditions. In contrast, the X-35-02 airfoil demonstrates less stable dynamic loops, characterized by greater variability and reduced overshoot. Furthermore, the installation of VGs significantly improves flow stability during the US-phase, increases the overshoot of the normal force coefficient, and reduces the discrepancy between the dynamic loops of the two airfoils.

Under dynamic stall conditions, a trailing edge separation bubble develops over the airfoil during the US-phase and ultimately triggers stall. For the flatback airfoil, the growth of this separation region occurs more gradually compared to the sharp trailing edge configuration, thereby delaying the onset of dynamic stall and producing a larger overshoot in the normal force coefficient. Moreover, an increase in reduced frequency introduces a noticeable lag in pressure development on the suction side of the airfoil, which slows the expansion of the separation bubble and amplifies the dynamic lift overshoot. The installation of VGs on the airfoil surface further improves flow stability during the US-phase and reduces the differences between the dynamic cycles of the two airfoils. This modification results in a substantial increase in the maximum overshoot, particularly for the X-35-02 airfoil, when compared to the configuration without VGs. However, this increase in C_N overshoot also leads to a more pronounced drop in C_N following the occurrence of dynamic stall. For the flatback airfoil, the wider range of normal force and higher variability can be linked to its suction side pressure gradient, which promotes stronger pressure recovery and a more gradual stall onset. This geometric feature, combined with the stabilizing effect of VGs, delays complete flow detachment during the US-phase, thereby amplifying the dynamic normal force coefficient and increasing the asymmetry of the hysteresis cycle.

In post-stall conditions, extensive flow separation over the airfoil surface causes the dynamic lift to remain closely aligned with the static lift curve for both airfoils when operating under a forced transition flow regime. This behavior indicates that, once full separation occurs, the influence of unsteady effects on lift generation becomes minimal. However, under free transition flow conditions, pronounced dynamic lift fluctuations are observed. These fluctuations are particularly evident in cases characterized by smooth, steady stall and when the BSA approaches the steady stall angle of attack, indicating the sensitivity of the flow to transition mechanisms and stall onset under unsteady aerodynamic conditions.

The influence of reduced frequency on dynamic stall behavior is a critical factor in

understanding and optimizing unsteady aerodynamic performance for wind turbine applications. At low reduced frequencies, the abrupt onset of stall combined with large force excursions can induce severe unsteady loading, which poses significant risks to structural integrity and accelerates fatigue damage in wind turbine blades. Increasing the reduced frequency not only delays the onset of the dynamic stall and elevates the dynamic stall angle of attack, but, when combined with effective VGs placement, also promotes improved flow stability and suppresses undesirable force fluctuations, particularly for flatback airfoils. The observed differences between sharp trailing edge and flatback airfoils highlighting the importance of airfoil geometry in governing vortex dynamics and pressure recovery. These findings show that tuning reduced frequency in conjunction with VGs configuration represents a powerful strategy for controlling dynamic stall, enhancing load predictability, and mitigating fatigue in large-scale rotor blades. For blade designers, this emphasizes the necessity of integrating passive flow-control devices with dynamic load management approaches to minimize fatigue and improve overall rotor performance under unsteady inflow conditions.

5

The results further emphasize the critical role of VGs placement in managing dynamic stall, particularly under forced transition conditions. Forward positioning of VGs significantly enhances boundary layer stability and delays flow separation, which is especially advantageous for maintaining aerodynamic performance at elevated angles of attack. However, the persistence of flow instabilities at lower reduced frequencies and during the recovery phase for mid-chord and aft VGs positions indicates that VGs effectiveness is not uniform across all dynamic regimes. The interaction between VGs-induced flow control and airfoil geometry, especially in flatback configurations, adds complexity to the optimization process. The presence of a blunt trailing edge amplifies the intricacy of the vortex system, making the airfoil more susceptible to flow instabilities at low oscillation frequencies when the TSA approaches the steady stall angle of attack. Forward VGs placement mitigates these instabilities by promoting earlier boundary layer re-energization, which stabilizes the flow and reduces unsteady load fluctuations. This stabilization effect is particularly more pronounced at higher reduced frequencies for the flatback airfoil compared to the conventional sharp trailing edge design. Overall, the flatback airfoil tends to sustain attached flow, exhibits a lower tendency for dynamic stall, and recovers flow attachment more rapidly than the conventional airfoil. Nevertheless, when dynamic stall does occur, the force fluctuations within the separated flow phase are greater than those observed for the sharp trailing edge airfoil. In general, forward VGs positioning proves advantageous across a wider range of reduced frequencies; within the attached portion of the lift curve, it reduces the maximum overshoot in C_N , and at higher angles of attack, it effectively prevents dynamic stall and mitigates large force fluctuations. The suppression of dynamic stall and reduction in load variability contribute to improved fatigue resistance and operational reliability, which are essential for large-scale wind turbine applications.

This chapter demonstrates that the application of VGs can significantly enhance flow stability during the US-phase for both airfoils, with a more potential observed for the non-

flatback configuration. Increasing the trailing edge thickness, installing VGs on the airfoil surface, and operating at higher reduced frequencies collectively contribute to delaying the onset of dynamic stall and increasing the overshoot of the normal force coefficient. These modifications can exert a substantial influence on wind turbine operation. Furthermore, the combination of VGs and elevated reduced frequencies reduces the magnitude of the C_N drop following dynamic stall, thereby mitigating adverse aerodynamic effects. Preventing dynamic stall is essential for avoiding vortex-induced vibrations of rotor blades under highly turbulent inflow conditions, ultimately reducing vibration loads, improving overall efficiency, extending operational lifetime, and enhancing turbine reliability while minimizing sensitivity to wind fluctuations. However, this approach may also result in very high C_N overshoots, which could overstress turbine components, particularly when combined with flatback airfoil geometries. Consequently, further research is required to fully assess the potential of VGs and flatback airfoils in reducing dynamic stall-induced vibration loads and improving the structural performance of rotor blades.

6

EVALUATING DYNAMIC STALL MODELS FOR THICK WIND TURBINE AIRFOILS

6

This chapter evaluates the Beddoes–Leishman and Risø dynamic stall models by comparing their predictions with experimental data obtained on two thick wind turbine airfoils, as discussed in the previous chapters, both with and without vortex generators. Simulated dynamic lift, drag, and pitching moment coefficients are compared with measurements obtained in the TU Delft Low-Turbulence Tunnel at a Reynolds number of $Re = 1 \times 10^6$ and reduced frequencies ranging from 0.032 to 0.096. The assessment covers variations in mean angle of attack, reduced frequency, vortex generator configurations, and chordwise positions for both thick conventional and flatback airfoils. These findings highlight key critical limitations in the current dynamic stall modeling approaches and raise concerns regarding the accuracy and reliability of dynamic load prediction and aeroelastic analysis for modern large wind turbines.

6.1 OBJECTIVE

As reviewed in the dynamic stall section 1.2.2 of the state of the art chapter, the dynamic stall behavior of thin airfoils has been extensively investigated in numerous studies, primarily based on limited unsteady aerodynamic measurements. These measurements have served as the foundation for the development of several dynamic stall models designed to simulate this highly complex phenomenon. It is important to emphasize that all dynamic stall models currently adapted or modified for wind energy applications were originally formulated and validated using datasets obtained from thin airfoils, which were generally developed for aerospace applications rather than for wind turbines. Consequently, their applicability to thick airfoil sections, which exhibit fundamentally different flow characteristics, remains uncertain.

Despite the importance of the dynamic stall models, the current understanding of how well existing dynamic stall models perform for thick conventional and flatback airfoils, especially when combined with VGs and evaluated from the perspective of maximum load prediction is limited. Inaccuracies in modeling these behaviors can lead to substantial errors in dynamic load estimation, potentially compromising rotor blade design, fatigue life predictions, and overall turbine reliability. This issue becomes even more critical as turbine sizes continue to increase, amplifying the impact of modeling inaccuracies on structural safety and operational efficiency. To address this knowledge gap, the present chapter systematically evaluates the accuracy of the B-L and Risø dynamic stall models for airfoils with high relative thickness. The assessment focuses on the default time constants commonly employed by both industry and the scientific community, as these parameters strongly influence model performance and are widely used in aeroelastic simulation tools. To achieve this objective, the measurement data presented in chapter 4 and chapter 5 for the X-35-02 and X-FB-35-10 airfoils are used in the evaluation.

The experimental dataset serves as the foundation for examining model accuracy across a wide range of conditions, including variations in mean angle of attack within different regions of the static lift curve, trailing edge gap configurations, and VGs arrangements under free transition flow conditions. The measurement matrix encompasses a broad spectrum of dynamic parameters and geometric configurations, thereby providing a comprehensive basis for assessing the predictive capability of current dynamic stall models when applied to thick airfoils. This systematic evaluation not only identifies the limitations of existing models but also provides guidance for future improvements in dynamic stall modeling for large wind turbines.

This chapter is organized into the following sections: First, the calibration of model constants and parameters is discussed, informed by detailed flow-field observations and analysis. Next, the performance of each dynamic stall model is analyzed individually under varying parameter sets, airfoil configurations, and dynamic conditions. Subsequently, the limitations in model accuracy are examined from both physical and mathematical perspectives, and recommendations for future model development are proposed. Finally,

the key findings are summarized in the concluding section, highlighting their implications for aeroelastic design and dynamic load prediction in modern wind turbines.

6.2 CALIBRATION OF DYNAMIC STALL MODEL CONSTANTS

Like any mathematical model, the predictive capability of the B-L dynamic stall model is strongly dependent on the accuracy of the empirical constants incorporated into its formulation [119]. In general, as the number of model constants increases, the sensitivity of the overall accuracy to these parameters also rises. This is particularly relevant for the B-L model, which employs a relatively large set of constants to capture the complex aerodynamic phenomena associated with dynamic stall. Specifically, the model includes 16 parameters governing the attached flow behavior, 9 parameters associated with the flow separation module, and an additional 7 parameters dedicated to the vortex shedding mechanism. Consequently, the calibration and validation of these constants play a critical role in ensuring reliable predictions, as even small deviations in their values can lead to significant discrepancies in the computed aerodynamic loads and stall onset characteristics.

Several airfoil-dependent parameters, such as the lift curve slope $dC_N/d\alpha$, the zero-lift angle of attack α_0 , and the baseline drag and moment coefficients (C_{D0} and C_{M0}), can be readily extracted from the linear portion of the steady aerodynamic polar for each airfoil under the specified configuration. The lift curve slope is determined at the zero-lift angle of attack using a linear regression approach applied consistently across all cases to ensure accuracy and comparability. In addition, the chordwise recovery factor η , requires calibration for each airfoil and operating configuration, as it significantly influences the prediction of separated flow characteristics. For estimating the separated flow drag, the potential-flow drag is initially employed. However, because the actual chordwise force in real flow conditions cannot reach the theoretical maximum predicted by potential flow, a correction factor is introduced to account for this nonlinearity [8]. For rotorcraft airfoils, this correction factor typically ranges between 0.95 and 0.97 [120]; however, thick wind turbine airfoils generally exhibit lower values due to their distinct flow behavior. The calibration of η in this study follows the methodology proposed by [105], which minimizes the discrepancy between $\eta C_{T(s)}^P$ and the static tangential pressure force within the linear region of the aerodynamic curve. Figure 6.1 (a) illustrates the calibrated η for the X-35-02 airfoil in a clean configuration.

The indicial lift and moment response function constants (A_i, b_i) for the attached flow component of the B-L model are primarily functions of Mach number [90] and exhibit relatively low sensitivity to airfoil geometry, at least for thin airfoils [79]. These constants are typically determined by fitting the harmonic load response, both amplitude and phase, obtained from oscillatory tests conducted over a range of reduced frequencies [121].

The indicial lift response characterizes the time-dependent evolution of aerodynamic loads following a step change in angle of attack or pitch rate and forms the foundation of unsteady aerodynamic modeling in subsonic flow regimes. For computational efficiency,

the circulatory component of this response is commonly approximated by a two-term exponential function:

$$\phi_c(S, M) = 1 - A_1 \exp\left(-\frac{b_1}{\beta^2} S\right) - A_2 \exp\left(-\frac{b_2}{\beta^2} S\right) \quad (6.1)$$

where S denotes the non-dimensional time and β is the Prandtl–Glauert compressibility factor. The coefficients A_1, A_2 represent the relative contribution of each exponential term to the total circulatory response, while b_1, b_2 govern the decay rates of the respective transients. Beddoes' original values, as reported by Leishman [121], are $A_1 = 0.3$, $A_2 = 0.7$, $b_1 = 0.14$, $b_2 = 0.53$, which remain the default parameters in most aeroelastic codes. These values were derived from a series of CFD simulations conducted at NASA Ames [98] and have since been widely adopted in dynamic stall modeling.

From a mathematical perspective, the interpretation of these constants follows directly from the properties of exponential decay. Each term decreases at a rate proportional to its decay constant b_i/β^2 . A larger b corresponds to a steeper decay and thus a faster transient, whereas a smaller b produces a slower decay, representing the long-term component of the response. Consequently, with $b_2 = 0.53$ significantly larger than $b_1 = 0.14$, the term $-A_2 e^{-(b_2/\beta^2)S}$ dominates the "fast component" of the circulatory response, while $-A_1 e^{-(b_1/\beta^2)S}$ captures the "slow component". This distinction is essential for accurately modeling both the rapid initial adjustment and the gradual buildup of circulation in subsonic flow conditions.

The extraction of these coefficients from experimental data requires several conditions to be satisfied [121]. First, the aerodynamic system must exhibit linear behavior, which is generally valid for airfoils operating in nominally attached flow, even at high subsonic Mach numbers. Second, the aerodynamic response must be measured over a sufficiently broad range of reduced frequencies to capture both early-time and intermediate dynamics, as high-frequency data strongly influence the initial transient. Third, compressibility effects must be accounted for, since Mach number significantly alters both the initial non-circulatory loading and the asymptotic approach to static lift, necessitating data across multiple Mach numbers. Additionally, the pitch-axis location must be clearly defined and maintained during testing, as it directly affects the form of the indicial response. Finally, practical limitations, such as difficulties in achieving high reduced frequencies at elevated Mach numbers, often lead to sparse experimental datasets, complicating the optimization and generalization of these coefficients [121].

The airfoil-dependent parameters and the indicial response function constants are detailed in tables 6.1 and 6.2, respectively. For this study, the default indicial response function values as reported by Beddoes [98] are utilized for both dynamic stall models.

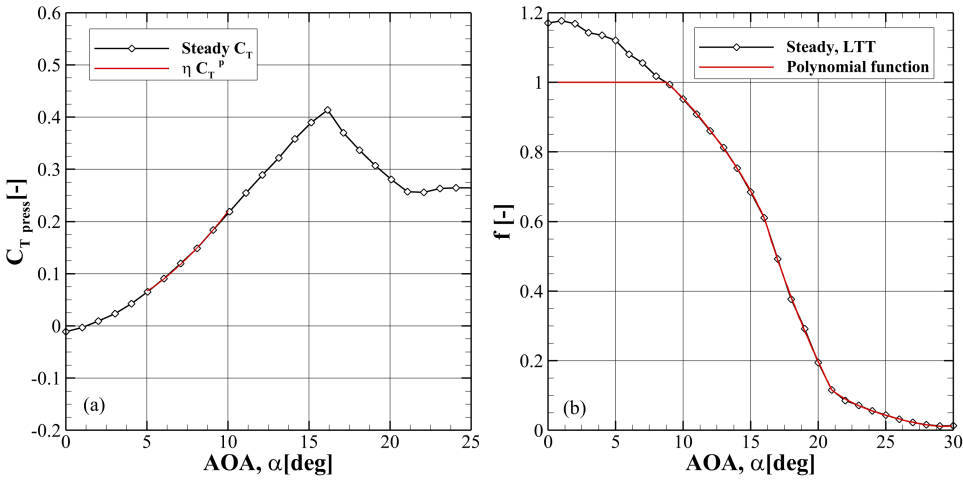


Figure 6.1: Chordwise recovery factor (η) calibration and steady separation position (f) curve fit in free transition flow regime, [$Re = 1 \times 10^6$, X-35-02].

Table 6.1: Airfoil-dependent constants for both dynamic stall models.

Airfoil	VG_s [x/C]	$dC_N/d\alpha$	$dC_L/d\alpha$	α_0	C_{D0}	C_{M0}	η
X-35-02	-	6.405	6.412	-2.97	0.011	-0.086	0.86
X-35-02	0.3	6.755	6.759	-2.42	0.013	-0.085	0.83
X-FB-35-10	-	7.608	7.660	-3.53	0.040	-0.111	0.75
X-FB-35-10	0.3	7.267	7.294	-3.22	0.039	-0.109	0.73

Table 6.2: Indicial lift and moment response function constants for both dynamic stall models.

Airfoil	VG_s [x/C]	A_1	b_1	A_2	b_2	A_3	b_3	A_4	b_4	A_5	b_5
X-35-02	-	0.3	0.14	0.7	0.53	0.15	0.25	-0.5	0.1	1	0.5
X-35-02	0.3	0.3	0.14	0.7	0.53	0.15	0.25	-0.5	0.1	1	0.5
X-FB-35-10	-	0.3	0.14	0.7	0.53	0.15	0.25	-0.5	0.1	1	0.5
X-FB-35-10	0.3	0.3	0.14	0.7	0.53	0.15	0.25	-0.5	0.1	1	0.5

The non-dimensional steady separation point, f , is a key parameter repeatedly used in the B-L dynamic stall model. This quantity is computed by solving the Kirchhoff separation formula (equation:1.23) for f using static aerodynamic polar data. In the original B-L formulation, the separation function is represented by a simple piecewise exponential curve fitted to experimental data, requiring three parameters: a breakpoint angle distinguishing attached and separated flow regimes, and two empirical coefficients. While this approach

is computationally efficient, it is strongly airfoil-dependent and does not always provide adequate accuracy for airfoils with non-standard polar shapes, such as thick wind turbine airfoils. To improve the fidelity of the separation function, a piecewise fourth-order polynomial fitting method was employed in this study to reconstruct the separation curve from steady aerodynamic data. This higher-order approach offers greater flexibility in capturing the nonlinear characteristics of the separation progression, particularly near the stall, where the slope of the curve changes rapidly. By using polynomial fitting, the method reduces local approximation errors and ensures a smoother transition between attached and separated flow regions, which is essential for accurate dynamic stall predictions. Figure 6.1 (b) depicts the computed separation parameter compared against values extracted from wind tunnel measurements for the X-35-02 airfoil in a clean configuration.

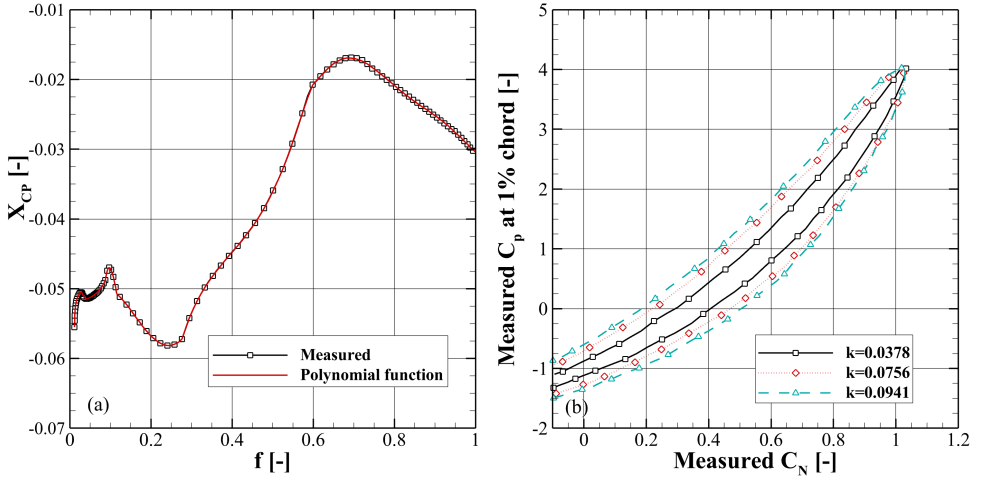


Figure 6.2: X_{CP} vs f curve fitting and the unsteady leading edge pressure comparison in free transition flow regime, $[Re = 1 \times 10^6]$, (a): X-35-02, (b): NACA 0012].

Because Kirchhoff's theory does not provide a means to compute the unsteady pitching moment, Beddoes and Leishman [8] introduced an empirical formulation that relates the steady separation position f to the unsteady pitching moment coefficient C_M . This approach employs the steady normal force coefficient C_N , the aerodynamic center, and three additional constants to approximate the pitching moment during dynamic stall. However, the original formulation was primarily developed for thin airfoils and may not accurately capture the behavior of thick wind turbine airfoils, which exhibit different center of pressure migration characteristics. As suggested by Leishman [8], the center of pressure at any angle of attack can be estimated from the ratio C_M/C_N derived from static aerodynamic data. By plotting this variation against the corresponding values of the steady separation parameter f , a functional relationship can be established through curve fitting. This approach is used here, to calculate the unsteady pitching moment for separated

flow within the separated flow module. The same as the f , a piecewise polynomial fitting technique is applied to capture the nonlinear dependence of X_{CP} on f . Figure 6.2 (a) presents an example of the fitted curve for the X-35-02 airfoil in a clean configuration.

Since the Risø dynamic stall model is fundamentally derived from the B-L model, the same overall calibration strategy is applied when adapting it to the present airfoils. Accordingly, a piecewise fourth-order polynomial fitting method is used to define the static trailing edge separation function f^{st} , analogous to the procedure employed for the B-L model. In addition, the required static aerodynamic coefficients, i.e. C_L^{st} , C_D^{st} , and C_M^{st} , are obtained directly from the steady aerodynamic polar, and the corresponding center of pressure functions $X_{CP,L}$ are constructed using curve fits to the measured static moment and lift force data.

Each dynamic stall model was evaluated using two configurations for the time constants T_p and T_f : one using the default values and another with adjusted values. In this study, the default values reported by Leishman [119] and Hansen et al. [122] are used for the B-L and Risø models, respectively, representing the state of the art implementations in aeroelastic simulation codes. These constants are presented in table 6.3.

Under unsteady forcing conditions, flow separation is delayed to higher effective angles of attack because of the favorable effects on the buildup of leading edge pressure due to the shed wake and pitch rate (induced camber). Despite these benefits, the leading edge pressure response exhibits a lag and attenuation relative to changes in angle of attack and pitch rate, particularly at higher reduced frequencies. This lag means that the critical pressure for stall onset is reached at a larger normal force coefficient and, consequently, at a higher angle of attack compared to quasi-steady conditions. On the US-phase, the leading edge pressure is reduced for a given lift level, while on the downstroke it is increased, delaying both stall onset and flow reattachment [103]. This mechanism strongly affects the dynamic stall hysteresis and is modeled in the B-L model by the leading edge pressure peak time constant, T_p via applying a first-order filter to the normal force coefficient. T_p defines the extent of the linear portion of the lift polar, corresponding to the attached flow phase of the dynamic stall cycle. Consequently, this parameter has a major effect on the MLOS. According to Leishman [8], T_p is a function of the Mach number and must be determined empirically from unsteady airfoil data. Although T_p is generally considered to be largely independent of airfoil geometry [123, 124], this conclusion primarily applies to thin airfoils. For thick airfoils, separate investigations are necessary to assess the validity of this assumption [119], especially for the reduced frequency range, that is observed in the wind industry. Figure 6.2 (b) shows the behavior of pressure coefficient with respect to the normal force for different reduced frequencies for NACA 0012 airfoil [8].

The boundary layer lag time constant, T_f , in the B-L model represents the lag in the boundary layer's response to changes in aerodynamic conditions, specifically the development and progression of trailing edge separation [103]. Physically, this lag arises because the viscous boundary layer cannot instantaneously adapt to variations in angle of attack or pressure distribution; instead, momentum diffusion and displacement thickness

Table 6.3: Airfoils, dynamic stall models, and time constants configurations.

Airfoil	VGs [x/C]	Model	Set	T_p	T_f	τ_p	τ_f
X-35-02	-	B-L	Default	1.7	3.0	-	-
X-35-02	-	B-L	Adjusted	0.46	1.93	-	-
X-35-02	-	Risø	Default	-	-	1.5	6.0
X-35-02	-	Risø	Adjusted	-	-	0.4	1.4
X-35-02	0.3	B-L	Default	1.7	3.0	-	-
X-35-02	0.3	B-L	Adjusted	4.89	0.49	-	-
X-35-02	0.3	Risø	Default	-	-	1.5	6.0
X-FB-35-10	-	B-L	Default	1.7	3.0	-	-
X-FB-35-10	-	B-L	Adjusted	1.33	0.01	-	-
X-FB-35-10	-	Risø	Default	-	-	1.5	6.0
X-FB-35-10	0.3	B-L	Default	1.7	3.0	-	-
X-FB-35-10	0.3	B-L	Adjusted	2.74	0.01	-	-
X-FB-35-10	0.3	Risø	Default	-	-	1.5	6.0

growth require time. In the B-L model, this effect is captured by applying a first-order lag to the quasi-steady separation point predicted from static data. This constant governs the rate at which the trailing edge separation point moves toward its equilibrium position under unsteady conditions. It defines the angle of attack range over which trailing edge flow separation occurs and primarily influences the Dynamic Stall Angle Of Attack (DSAOA). While T_f exhibits a weak dependence on Mach number compared to the leading edge pressure lag constant T_p [8], its variation with airfoil geometry is less well understood and more difficult to quantify. Note that, in the simulation codes, the value from NACA 0012 airfoil is used [123]. In this chapter, both time constants were adjusted with the aim of more accurately reproducing the experimentally observed MLOS and DSAOA.

Note that numerous Particle Image Velocimetry (PIV) measurements shown below conducted across a broad range of reduced frequencies, spanning both low and high values, consistently show that no leading edge flow separation occurs for these airfoils in any of the tested configurations during the onset of dynamic stall. Consequently, the Leading Edge Vortex (LEV) module within the B-L dynamic stall model is deactivated. As a result, the associated parameters, i.e., T_v , T_{vl} , K_{CT} , D_{CT} , and C_{N1} , are not considered in the present discussion. The following section discusses the flow physics of thick airfoils.

6.3 UNSTEADY FLOW PHYSICS OF DYNAMIC STALL ON THICK AIRFOILS

To characterize the flow structure during dynamic stall for the current class of thick airfoils, an extreme operating condition is selected to analyze the unsteady flow physics of airfoil dynamic stall in detail. This scenario represents conditions where the probability of leading edge vortex formation is maximized, involving a high mean angle of attack and elevated reduced frequency. Additionally, a pronounced adverse pressure gradient on the suction side typical of conventional airfoil geometries, further amplifies the severity of the stall process. Such an approach provides deeper insight into the governing unsteady aerodynamic mechanisms, including vortex evolution, boundary layer instability, and the interaction between separated shear layers and the main flow.

Figure 6.3 presents the phase-averaged values and standard deviations of the dynamic normal force coefficient and the quarter-chord pitching moment coefficient of this case for the X-35-02 airfoil operating in a free transition regime. The measurements correspond to a Reynolds number of $Re = 1 \times 10^6$, a mean angle of attack of $\alpha_m = 10^\circ$, and a reduced frequency of $k = 0.064$. For reference, the steady aerodynamic performance of the same airfoil is also included in the figure to ease comparison between static and dynamic conditions. The results clearly indicate that, under dynamic conditions, the lift curve exhibits a nearly linear increase to significantly higher angles of attack compared to the steady polar. Nonlinearities in the lift response become evident only at elevated angles of attack, where flow separation effects intensify. This behavior leads to a pronounced lift overshoot, with the maximum normal force coefficient increasing from $C_{Nmax} = 2.11$ in the steady case to $C_{Nmax} = 2.32$

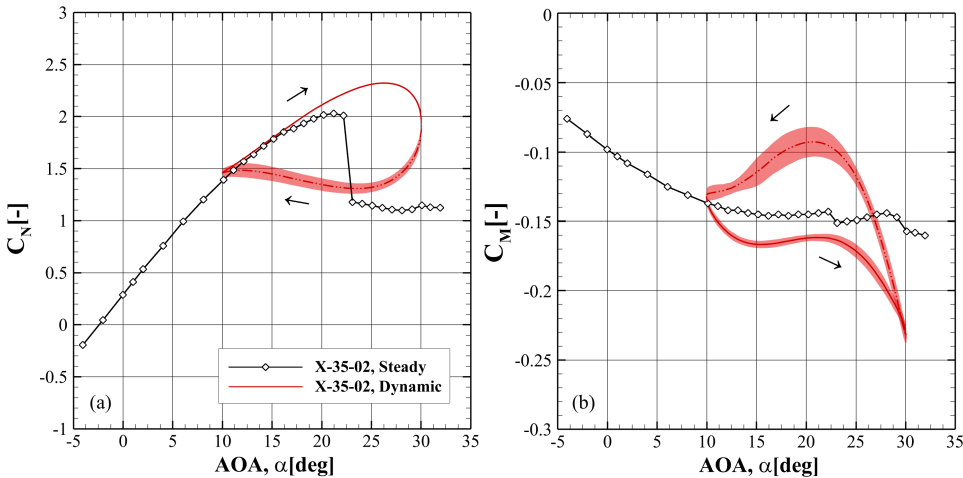


Figure 6.3: Measured steady and dynamic lift and pitching moment coefficients in free transition flow regime, [$Re = 1 \times 10^6$, X-35-02, $VGs_{(x/C)} = 0.3$, $\alpha_m = STL - 2^\circ$, $k = 0.096$].

during the dynamic cycle. Similarly, the stall angle of attack shifts from $\alpha_{stall} = 21.19^\circ$ to $\alpha_{stall} = 26.22^\circ$ within the US-phase of the oscillatory motion. It is important to note that dynamic stall initiates near the upper end of the US-phase, prior to reaching the TSA at $\alpha = 30^\circ$. As the angle of attack decreases from the TSA during the downstroke, the lift begins to drop further, accompanied by a noticeable increase in its standard deviation. This trend reflects the growing unsteadiness of the flow, mainly caused by fluctuations of the separation bubble over the suction surface. Due to the relatively high reduced frequency and the inherent aerodynamic lag of the system, flow reattachment occurs only at the BSA. A similar pattern is observed in the pitching moment response. The dynamic cycle exhibits a significant increase in the peak negative pitching moment prior to stall, with maximum pitching moment shifting from $C_{Mmax} = -0.14$ under steady conditions to $C_{Mmax} = -0.23$ dynamically. Interestingly, the maximum pitching moment occurs at the TSA ($\alpha = 30^\circ$), rather than at the angle corresponding to maximum lift, highlighting the strong phase lag between aerodynamic forces and moments under unsteady conditions.

Figure 6.4 (a) illustrates the instantaneous three-dimensional surface pressure coefficient distribution on the suction side of the airfoil during the US-phase of a dynamic cycle. For improved visualization, a corresponding two-dimensional projection of this surface contour is also provided. To further clarify the evolution of pressure characteristics, figure 6.5 presents the complete pressure coefficient distribution around the airfoil throughout the US-phase of the same cycle, with an incremental angle of attack step of $\Delta\alpha = 2.5^\circ$. Because the standard deviation of the lift during the US-phase is relatively small, only minor deviations in pressure coefficient are expected across different dynamic cycles, pointing to the fact that the observed behavior is representative.

The results reveal that, during the initial portion of the US-phase, the suction peak on the airfoil's suction side intensifies and gradually shifts toward the leading edge as the angle of attack increases. This trend is clearly visible in figure 6.5 for $\alpha = 10^\circ$ to $\alpha = 15^\circ$. Near the mean angle of attack ($\alpha = 20^\circ$), a distinct suction plateau develops close to the leading edge, covering approximately 20% of the chord length on the suction side. This feature can be identified in figure 6.5 between $\alpha = 17.5^\circ$ and $\alpha = 22.5^\circ$ as well as in the projected pressure coefficient contour shown in figure 6.4 (a).

As the angle of attack continues to rise, the suction plateau contracts gradually, while the suction peak near the leading edge becomes more pronounced. This phenomenon, clearly depicted in the three-dimensional contour of figure 6.4 (a), results in the formation of a strong adverse pressure gradient near over the airfoil. The global maximum suction occurs during this stage, prior to reaching the TSA. Similar behavior has been reported for thinner airfoils during the US-phase, preceding the onset of LEV formation, as documented by Gupta et al. [125]. Concurrently, the development of maximum suction near the leading edge is accompanied by reduced pressure recovery toward the trailing edge. This effect is clearly visible as elevated suction between approximately 60% of the chord and the trailing edge, as seen in the three-dimensional contour. Gupta et al. [125] also observed this characteristic in their study. The increase in suction near the trailing edge significantly

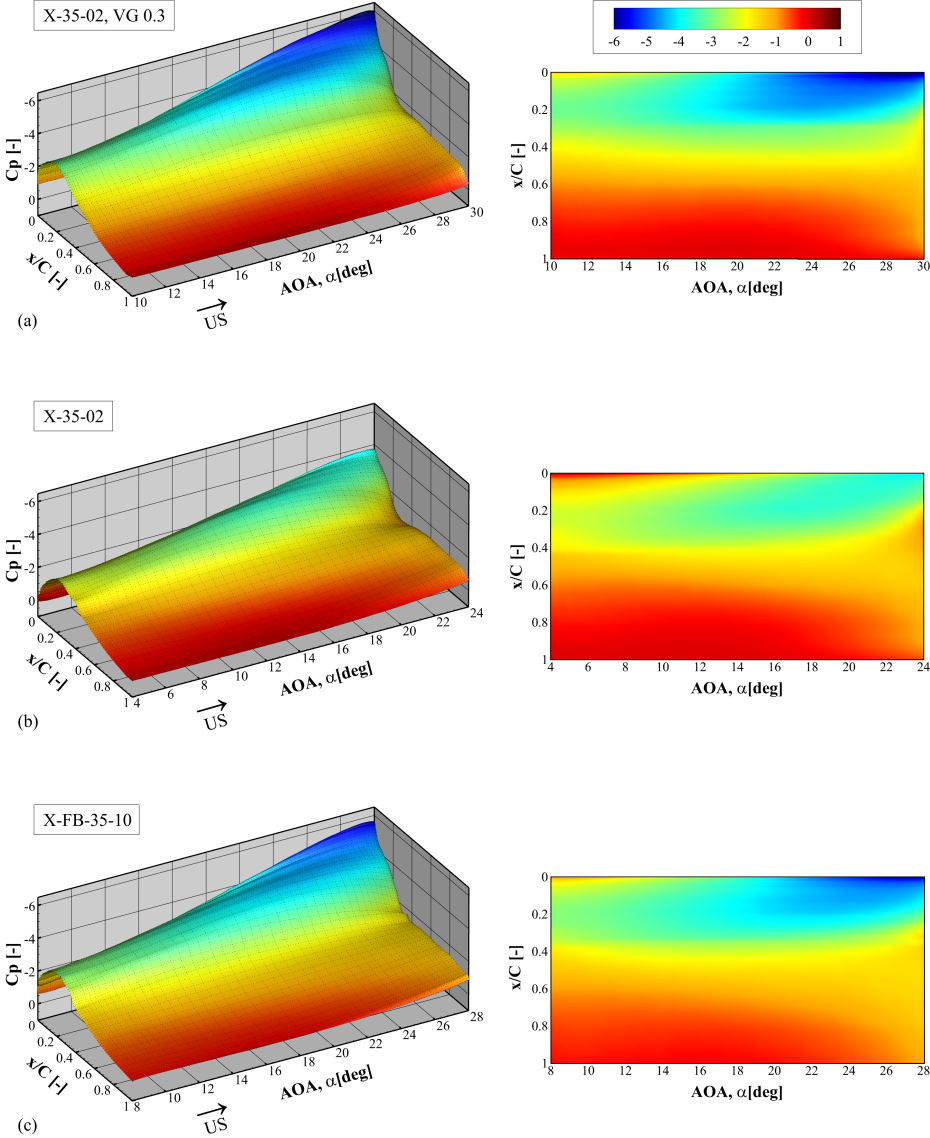


Figure 6.4: Instantaneous dynamic pressure coefficient development on the suction side of the airfoils in free transition flow regime in US-phase, [$Re = 1 \times 10^6$, $\alpha_m = STL-2^\circ$, $k = 0.096$].

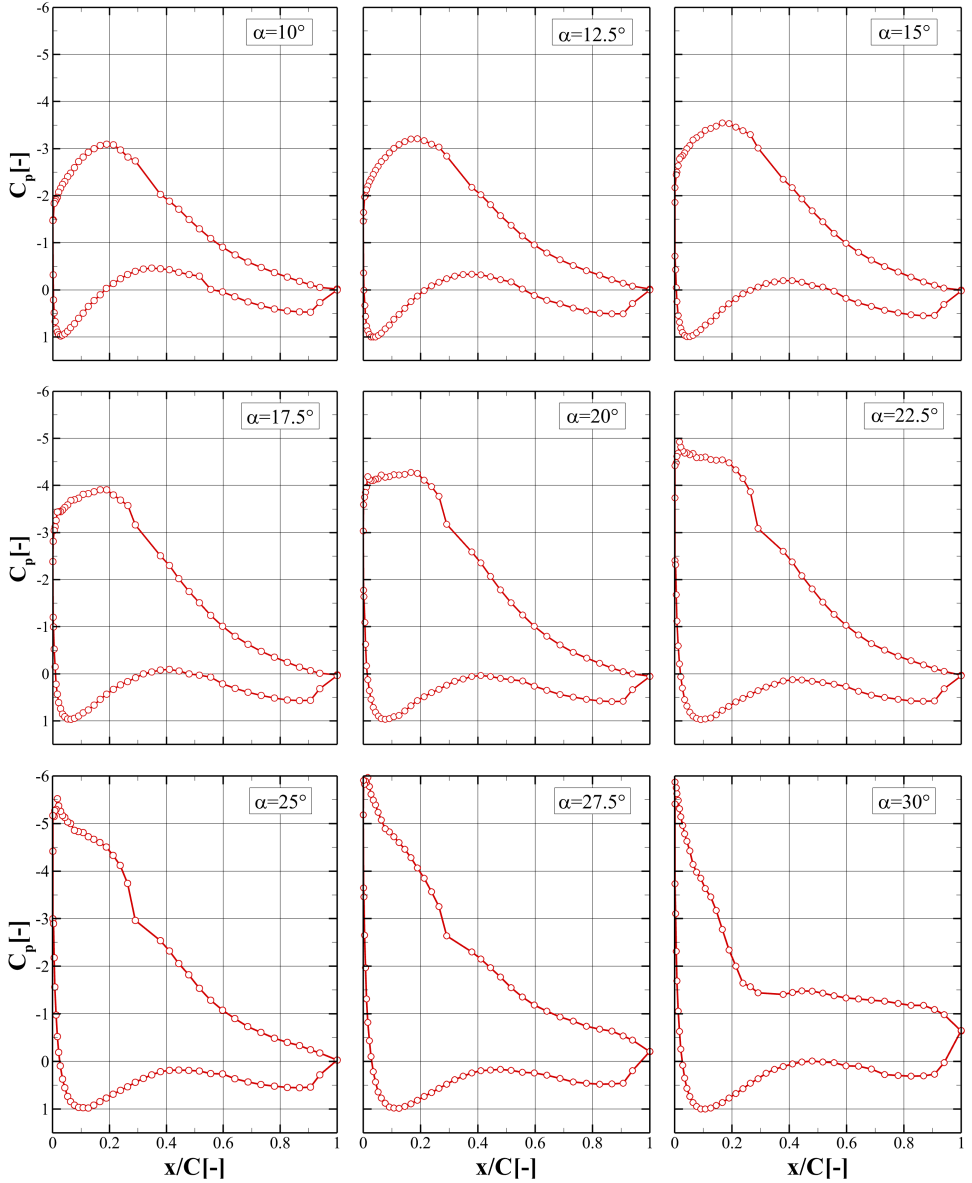


Figure 6.5: Instantaneous chordwise dynamic pressure coefficient distribution in free transition flow regime in US-phase, $[Re = 1 \times 10^6, X-35-02, VGs(x/C) = 0.3, \alpha_m = STL-2^\circ, k = 0.096]$.

contributes to the rise in dynamic pitching moment during the upper portion of the US-phase, primarily due to its long moment arm. The observed lift overshoot and delayed stall angle are conjectured to result from the combined influence of suction intensification near the leading edge and redistribution of pressure along the chord.

Gupta et al. [125] demonstrated that the formation of the LEV occurs subsequent to the development of maximum global suction on the suction surface of the airfoil. This stage is characterized by a rapid collapse of the suction peak near the leading edge, indicating the onset of significant flow instability. They initiated an experiment on NACA 0012 airfoil and showed that following the breakdown of the leading edge suction peak, perturbations within the separated shear layer amplify due to inherent flow instability. These disturbances promote the roll-up of the shear layer, ultimately leading to the formation of a coherent vortical structure. Once initiated, the LEV undergoes rapid growth, both in size and strength, generating an extensive suction region over the airfoil's upper surface. This large-scale suction effect is primarily responsible for the pronounced increase in the instantaneous lift slope observed during dynamic stall. As the LEV evolves spatially, it convects downstream along the airfoil surface. This movement produces a characteristic footprint on the pressure coefficient distribution, typically observed as a localized region of intensified suction, as it is presented in figure 6.6. In addition to its streamwise movement, the vortex core progressively convects away from the airfoil surface. This displacement reduces the influence of the vortex-induced suction, causing a gradual decline in suction magnitude as the angle of attack continues to increase. The combined effects of vortex growth, convection, and detachment play a critical role in shaping the unsteady lift and pitching moment in the US-phase. The aforementioned key indicators for the LEV i.e., the suction peak collapse and movement of the collapsed suction peak downstream is not observed here.

The fundamental difference in flow physics between the current thick airfoil (X-35-02) and the thin NACA 0012 airfoil can be explained through the Leading Edge Suction Parameter (LESP) criterion proposed by Ramesh et al. [126]. This criterion provides a quantitative measure for predicting the onset of discrete LEV formation during unsteady aerodynamic conditions. According to this study, a discrete LEV is shed only when the instantaneous suction at the leading edge, which is represented by the A_0 term in the Fourier series of bound vorticity, reaches a threshold value known as $LESP_{crit}$. This critical value is an inherent property of the airfoil geometry and Reynolds number, and it remains independent of the specific motion kinematics. For thin airfoils, such as NACA 0012, the sharp leading edge radius promotes a rapid intensification of the suction peak during dynamic pitch-up maneuvers. This accelerated growth of suction readily surpasses $LESP_{crit}$, initiating the characteristic collapse of the suction peak and subsequent roll-up of a coherent LEV. This process is a hallmark of dynamic stall on thin airfoils and is strongly associated with large transient loads and pronounced hysteresis in aerodynamic coefficients. In contrast, the thick X-35-02 airfoil features a larger leading edge radius which dampens the pressure gradient and mitigates the severity of the suction peak. This

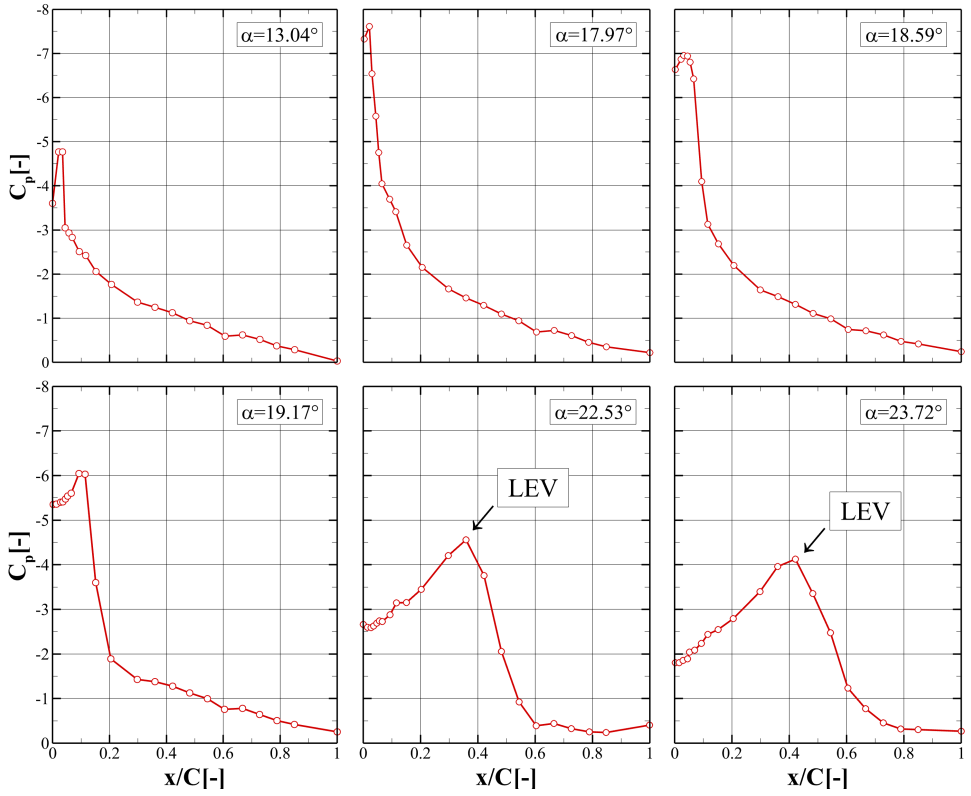


Figure 6.6: Phase-averaged dynamic chordwise pressure coefficient distribution measurements on the suction side of the airfoil in free transition flow regime in linear ramp maneuver [125], [$Re = 0.5 \times 10^6$, NACA 0012, $\omega^+ = 0.05$].

geometric damping ensures that even at extreme operating conditions with elevated mean angles of attack and reduced frequencies, the local suction fails to surpass the maximum allowable leading edge suction defined by the LESP criterion. As a result, the flow does not undergo the discrete-vortex shedding process typically associated with dynamic stall on thin airfoils. Instead, the stall is governed by a progression of trailing edge separation moving toward the leading edge, maintaining a relatively stable, yet fluctuating, separation bubble near the leading edge without the formation of a spatially convecting LEV.

Figures 6.4 (b) and (c) indicate the instantaneous three-dimensional surface pressure coefficient contour of two additional cases without VGs on X-35-02 and X-FB-35-10 airfoils in free transition flow regime. The results confirm similar flow behavior for both airfoils in dynamic stall, with no evidence of LEV formation under the tested conditions.

In addition to the dynamic surface pressure measurements, which provided insight into the near-wall flow characteristics, a series of stereoscopic PIV measurements were

conducted to capture the off-body flow-field during different phases of dynamic stall. These measurements enable a more comprehensive understanding of the three-dimensional flow structures and their evolution throughout the oscillatory cycle.

Figure 6.7 presents phase-averaged contours of normalized total vorticity at various angles of attack during the US-phase for the above study case. The initial stage of the DS-phase are also included in this series to illustrate the transition from the US into the DS-phase. To complement these observations, figure 6.8 displays the corresponding phase-averaged normalized velocity field contours, providing additional detail on the flow topology around the airfoil. Each dynamic measurement was based on 250 image pairs acquired at specific angles of attack using a phase-locked imaging technique, which synchronized the laser pulses with the oscillatory motion during both US and DS-phases. To enhance data quality and reduce measurement noise, a kriging regression method was applied to the PIV dataset, resulting in improved spatial resolution and contour clarity.

The flow evolution during the US-phase exhibits distinct characteristics. From the BSA up to approximately $\alpha = 20^\circ$, no evidence of large-scale flow separation is observed; the vorticity detected in this range originates primarily from the VGs. As the angle of attack increases to $\alpha = 25^\circ$, regions of elevated vorticity begin to appear downstream of the VGs. This increase in vortical activity is related to the intensifying adverse pressure gradient over the suction side, which produces a steep velocity gradient, a precursor to boundary layer separation. This transitional region can be identified in the velocity contours shown in figure 6.8 for the same angle of attack.

With further increase in angle of attack approaching the TSA, the trailing edge separation zone expands progressively toward the leading edge. A pronounced high-vorticity region develops in front of the VGs, corresponding to the formation of a strong shear layer, as illustrated in figure 6.7. The associated velocity contours in figure 6.8 confirm the presence of this shear layer at TSA. During the subsequent DS-phase, the flow separation chordwise position shifts downstream, accompanied by rapid spatial growth of the vortical structures and accelerated dissipation of vorticity, indicating strong unsteady dynamics during stall recovery. Importantly, the present results reveal no evidence of LEV formation, a phenomenon typically characterized by a coherent vortex that grows in size and convects downstream. This observation contrasts sharply with the PIV results reported by Gupta et al. [125] for thin airfoils, where LEV formation plays a dominant role in dynamic stall development.

Figure 6.9 provides an instantaneous velocity contour at TSA, indicating the presence of a fluctuating separation bubble. However, the chordwise location of the separation point remains essentially unchanged over time, showing a relatively stable flow-field near the leading edge during the peak angle of attack. This stability further supports the absence of LEV related flow instability in the current configuration.

The PIV measurements of the X-35-02 airfoil provide a detailed visualization of the shear layer dynamics that replace the classical LEV shedding mechanism, typically observed in thin airfoils. As the angle of attack approaches the TSA, the high-vorticity region

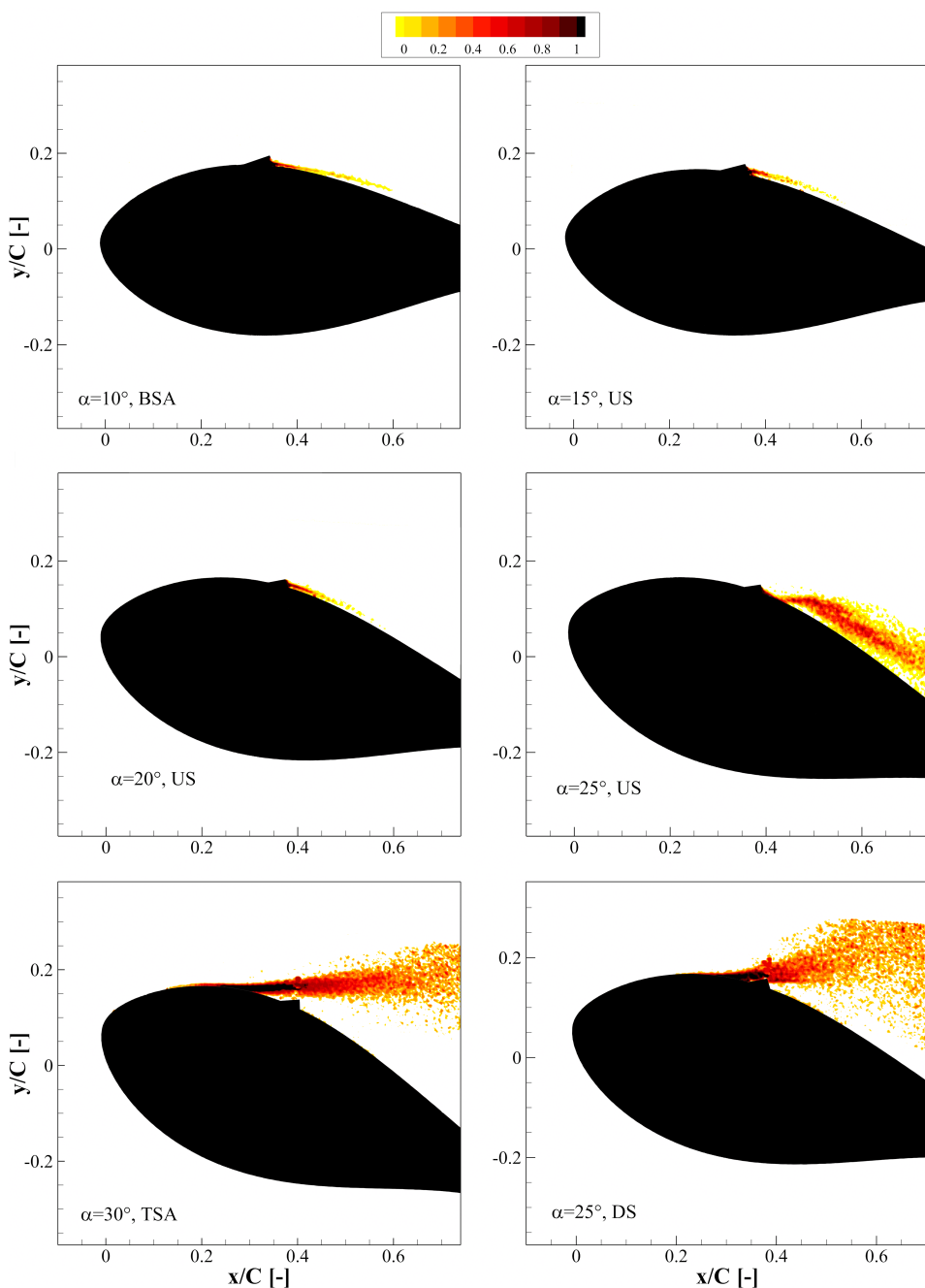


Figure 6.7: Phase-averaged measured normalized total vorticity contour in free transition flow regime in US-phase, [$Re = 1 \times 10^6$, X-35-02, $VG_{s(x/C)} = 0.3$, $\alpha_m = STL-2^\circ$, $k = 0.096$].

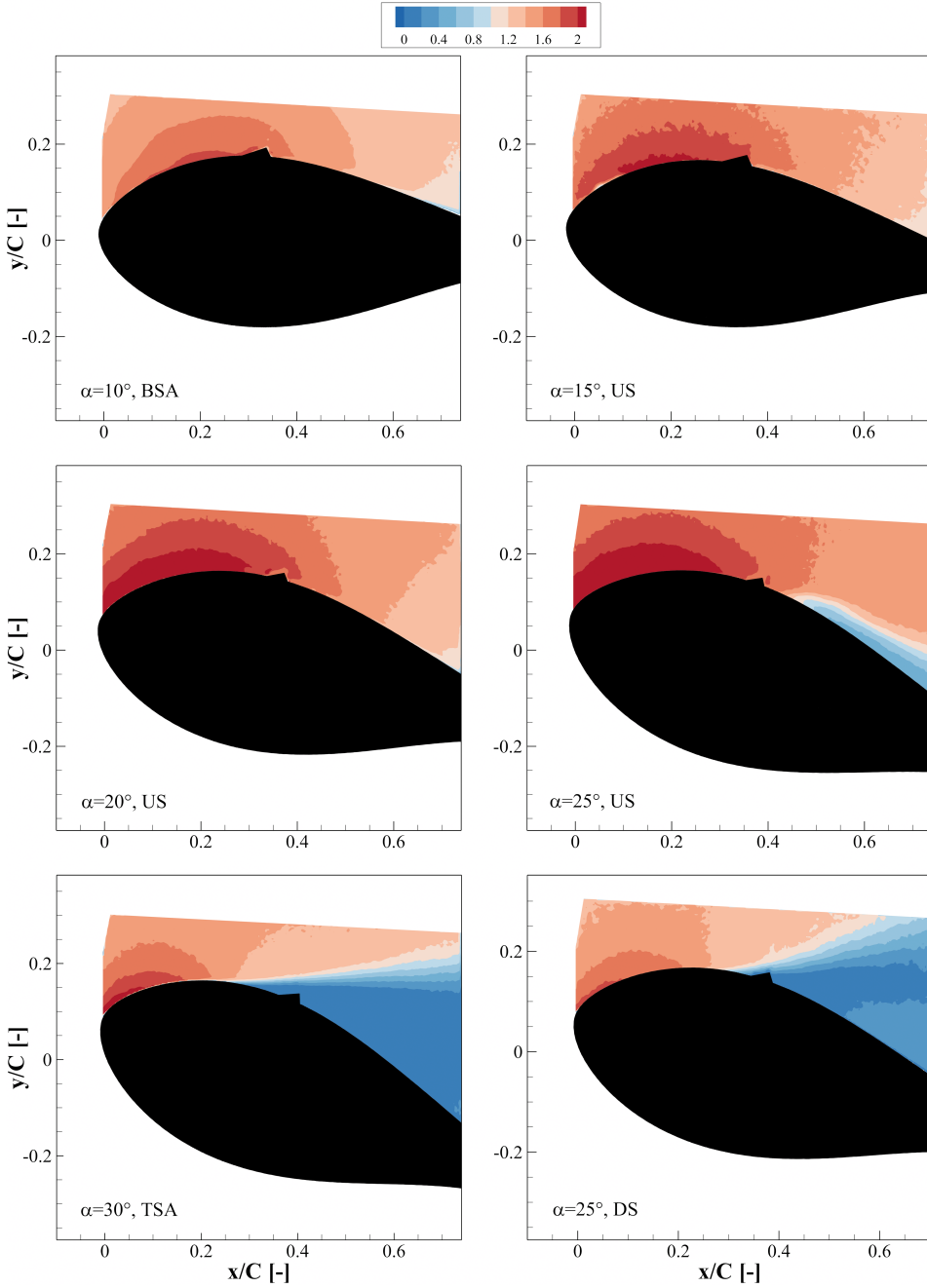


Figure 6.8: Phase-averaged measured normalized velocity contour in free transition flow regime in US-phase, $[Re = 1 \times 10^6, X-35-02, V G s_{(x/C)} = 0.3, \alpha_m = STL-2^\circ, k = 0.096]$.

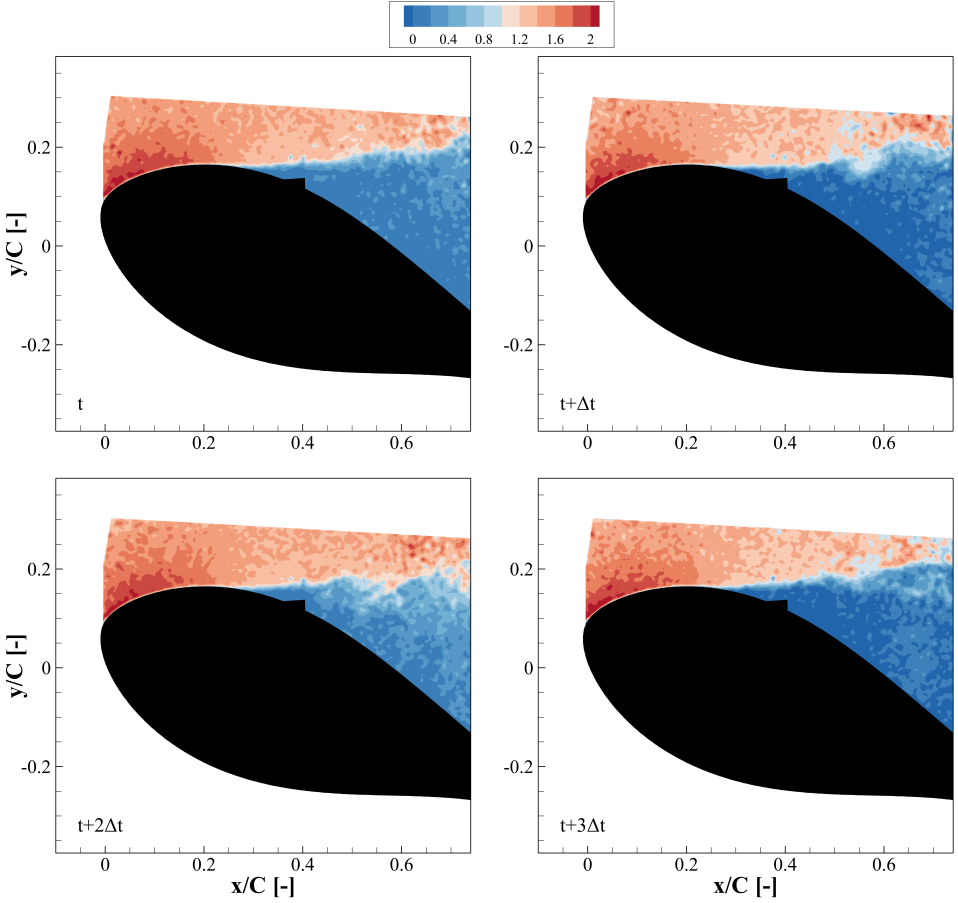


Figure 6.9: Instantaneous measured normalized velocity contour in free transition flow regime at TSA, [$Re = 1 \times 10^6$, X-35-02, $VG_{S(x/C)} = 0.3$, $\alpha_m = STL-2^\circ$, $k = 0.096$].

observed in figure 6.7 does not roll up into a discrete, circular core, but instead remains as an elongated, diffuse shear layer extending from the leading edge region. While thin airfoils concentrate vorticity into a compact region that rapidly destabilizes into a vortex, the thick airfoil distributes this vorticity over a broader spatial range, preventing a singular point of collapse. Referring to the principles of Ho and Huerre [127], this structure suggests that while Kelvin-Helmholtz instabilities are present, evidenced by the fluctuating nature of the separation bubble, the adverse pressure gradient on this thick airfoil is not sufficiently localized to force these instabilities to coalesce into a single global vortex. Unlike the results for the NACA 0012 [125], where the velocity contours would show a distinct closed recirculation zone translating downstream, the velocity fields in figures 6.8 and 6.9 indicate a relatively stable separation point and a shear layer that dissipates into smaller-scale

turbulence rather than organized vortical motion. This lack of spatial coherence in the vorticity field is a primary reason for the absence of the sharp, localized suction footprint on the airfoil surface, leading to the more gradual stall characteristics observed in the C_N responses.

6.4 RESULTS AND DISCUSSION OF THE VALIDATION OF THE MODELS

The aim of this section is to provide insights into the performance of two dynamic stall models when applied to thick wind turbine airfoils. To investigate this aspect, a series of simulations is carried out using the B-L and Risø models, and the results are compared with the aforementioned experimental data. Three mean angles of attack, namely $\alpha_m = 6^\circ$, $\alpha_m = STL-2^\circ$, and $\alpha_m = STL+5^\circ$, representing the linear region, stall, and post-stall phases at a reduced frequency of $k = 0.064$, are chosen to assess the accuracy of the models across different sections of the lift curve. Moreover, two distinct configurations are chosen for each airfoil, one with VGs and one without, under free transition flow regime.

Figure 6.10 illustrates the comparison of the measured steady and dynamic prediction with the B-L and Risø models at various mean angles of attack for the X-35-02 airfoil at a reduced frequency of $k = 0.064$ in the free transition flow regime. At a mean angle of attack of $\alpha_m = 6^\circ$, the measured C_L exhibits a slight deviation in the US and DS-phases compared to the steady polar curve. A minor dynamic stall occurs at the TSA, which is promptly resolved at the onset of the DS-phase. Both the B-L and Risø models accurately predict the cyclic behavior; however, their accuracy diminishes during the DS-phase. Additionally, both models overestimate the MLOS. Note that the tuned B-L model's MLOS is lower than that of default, yet it remains higher than the measured results. At $\alpha_m = STL-2^\circ$, the measured data reveal the occurrence of a pronounced dynamic stall. It is observed that the dynamic cycle exhibits a higher maximum C_L and a higher stall angle of attack compared to the steady data. Both models demonstrate an elevated MLOS and a faster stall recovery in the DS-phase in comparison to the measurements. The default Risø model displays the highest MLOS and fails to accurately predict the lift reduction, unlike the B-L model. The tuned B-L model accurately represents the MLOS and lift drop; however, its accuracy in stall recovery diminishes. Both models exhibit acceptable accuracy at the conclusion of the DS-phase and the beginning of the US-phase.

The post-stall mean angle of attack (i.e., $\alpha_m = STL+5^\circ$) leads to the formation of oval-shaped dynamic loops that cycle clockwise, in contrast to the linear segment. The B-L model captures the lift drop and the maximum lift angle of attack more accurately, compared to the Risø model. Moreover, the tuned B-L model better replicates the initiation of the US-phase than the default outcomes.

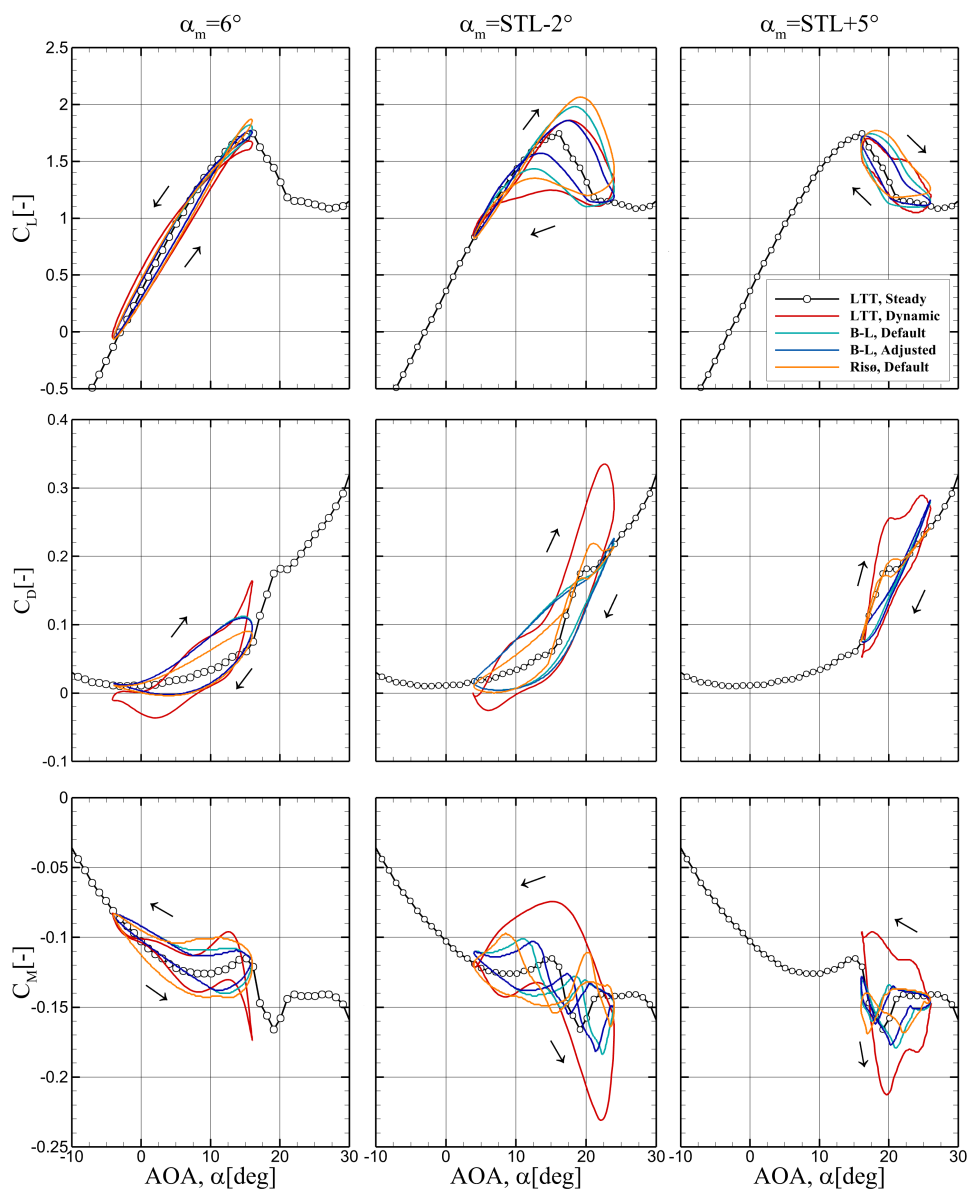


Figure 6.10: Measured and simulated dynamic polars in free transition flow regime, [$Re = 1 \times 10^6$, X-35-02, $k = 0.064$].

For drag prediction, the models are unable to accurately replicate the measured data, primarily due to the models' limitations in accurately representing flow separation and reattachment. At $\alpha_m = 6^\circ$ and $\alpha_m = STL-2^\circ$, the model results match the experimental data in the attached region of the polar, specifically at the beginning of the US-phase. However, significant deviations arise at the onset of dynamic stall and flow separation. Both models fail to predict the maximum drag at the TSA. At $\alpha_m = STL+5^\circ$, where flow separation occurs on the airfoil, both models underestimate the drag in the US-phase. Note that tuning the B-L model slightly alters the drag cycles, with no significant visible changes.

As a result of the common issue of flow separation and reattachment modeling, the prediction of the moment coefficient is inaccurate, and the deviation from the measurements amplifies in the flow separation and reattachment phases, namely in the dynamic stall, post-stall, and reattachment segments of the cycles. At $\alpha_m = 6^\circ$, the models can replicate the measured C_M in the lower angles of attack of the cycle but are unable to predict the C_M break angle of attack and magnitude. At $\alpha_m = STL-2^\circ$ and $\alpha_m = STL+5^\circ$, both models fail in predicting the C_M accurately.

The comparison of the dynamic stall models with the measurements at various mean angles of attack for the X-35-02 airfoil with VGs positioned at 30% of the chord length, at a reduced frequency of $k = 0.064$ in the free transition flow regime, is depicted in figure 6.11. At $\alpha_m = 6^\circ$, the models accurately capture the trend of the measurements. However, the deviation of the model results from the measurements increases compared to the configuration without VGs, especially at the TSA and DS-phases. Conversely, the models can predict the maximum and minimum C_L accurately, attributed to the significant distance of the TSA from the stall angle of attack. Additionally, the tuned B-L model exhibits lower accuracy in the US and DS-phases compared to the results from the default.

The results of the models exhibit discrepancies compared to the configuration without VGs at $\alpha_m = STL-2^\circ$. In this case, the model with default constants underestimates the DSAOA in comparison to the measurements. The B-L model underestimates MLOS and overestimates the magnitude of lift loss. The Risø model accurately predicts the MLOS and the magnitude of lift loss, but the shape of the DS-phase deviates from the measurements. Conversely, the tuned B-L model can predict the MLOS and DSAOA; however, the lift loss deviates from the measurements. All models overpredict the lift in the US-phase and poorly simulate the reattachment phase, particularly the B-L model due to inaccuracies in modeling flow reattachment with VGs. At $\alpha_m = STL+5^\circ$, all models exhibit poor prediction of the dynamic cycles in the post-stall region and overestimate the C_L at the BSA.

At $\alpha_m = 6^\circ$, all models predict the drag cycles in accordance with the measurements; however, they underestimate the maximum and minimum drag coefficient values. The B-L model configurations perform better in predicting the US-phase compared to the Risø model, but both models underpredict DS-phase values. In the dynamic stall cases at $\alpha_m = STL-2^\circ$, the B-L model configurations demonstrate superior performance to the Risø model and provide a more accurate estimation of the maximum and minimum C_D compared to the measurements. The Risø model exhibits the largest deviation from the

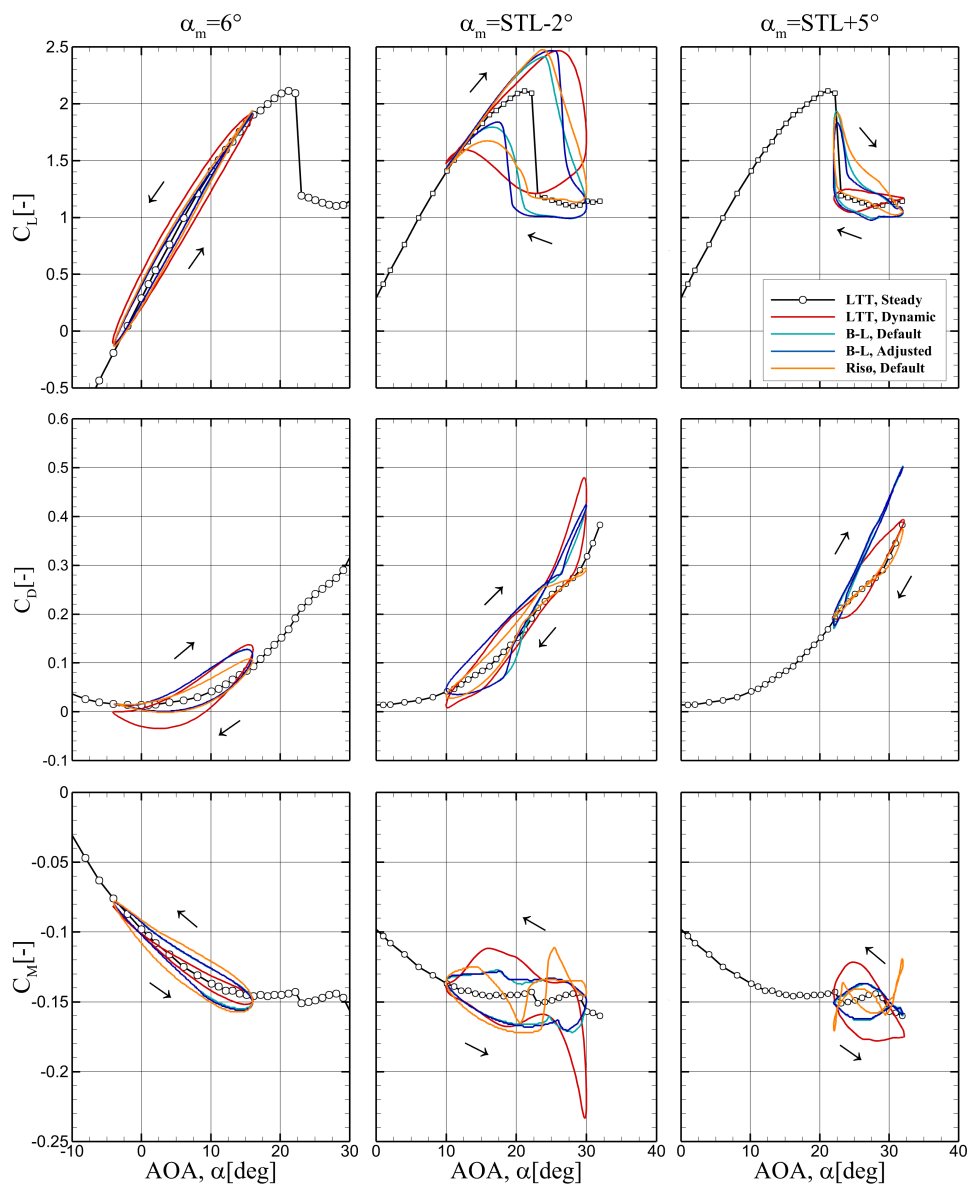


Figure 6.11: Measured and simulated dynamic polars in free transition flow regime, [$Re = 1 \times 10^6$, X-35-02, $VGs_{(x/C)} = 0.3$, $k = 0.064$].

measurements at the TSA among the models. At $\alpha_m = STL+5^\circ$, the B-L model configurations fail to accurately capture the measured drag cycles and overpredict the maximum C_D at the TSA. In contrast, the Risø model accurately predicts the maximum and minimum drag coefficients but struggles to replicate the measured post-stall cycle correctly. Note the tuned B-L model minimally alters the drag cycles. Owing to the previously discussed issue, the moment cycle predictions and C_M break points cannot be accurately predicted, leading to the models' failure in predicting the maximum and minimum C_M values and angles of attack accurately at $\alpha_m = STL-2^\circ$ and $\alpha_m = STL+5^\circ$. Only at $\alpha_m = 6^\circ$, i.e., linear part of the lift curve, the models correctly capture the cycle form.

Figure 6.12 depicts the comparison between the measured steady and dynamic data and the dynamic stall models at different mean angles of attack for the X-FB-35-10 flatback airfoil at a reduced frequency of $k = 0.064$ in the free transition flow regime. At $\alpha_m = 6^\circ$, the dynamic stall models reproduce the trend of the measurements, but overpredict the maximum C_L . However, the inaccuracy increases to half at the top and bottom of the US and DS-phases respectively. Furthermore, the tuned B-L model demonstrates a more accurate prediction of the maximum C_L at the TSA compared to the measurements. At $\alpha_m = STL-2^\circ$, both models overpredict the MLOS and slightly underpredict the DSAOA. Additionally, both configurations of the B-L model overpredict the lift decrease after the stall, unlike the Risø model. The tuned B-L model accurately predicts the MLOS, but the predicted DSAOA is slightly lower than the measurements. Unfortunately, it is not feasible to adjust both values simultaneously. All dynamic stall models fail to accurately model flow reattachment in the DS-phase, with increased inaccuracy observed in the tuned B-L model. At $\alpha_m = STL+5^\circ$, the Risø model overpredicts the C_L at the BSA, whereas the B-L model configurations exhibit improved C_L at the BSA.

For drag prediction, at $\alpha_m = 6^\circ$, the B-L model configurations overpredict the drag magnitude in both the US and DS-phases, particularly at the TSA. The Risø model more accurately predicts the maximum C_D compared to the B-L model, but it underestimates the C_D at the top and bottom parts of the US and DS-phases, respectively. Once again, due to flow separation modeling issues, both models fail to accurately capture the drag cycles at $\alpha_m = STL-2^\circ$ and $\alpha_m = STL+5^\circ$. Note that the B-L model overpredicts the magnitude of C_D , which is attributed not only to the inaccuracy of flow separation/reattachment modeling but also to the C_T calculation in separated flow, which is based on C_N .

Similar to previous observations, the models accurately capture the cycle form only during attached flow, but they fail to predict the shape, C_M break point, and the maximum and minimum C_M angles of attack.

The comparison between the dynamic stall models and the measurements at different mean angles of attack for the X-FB-35-10 airfoil with VGs positioned at 30% of the chord length, at a reduced frequency of $k = 0.064$ in the free transition flow regime, is illustrated in figure 6.13.

At $\alpha_m = 6^\circ$, the TSA remains well below the steady stall angle of attack, and a similar trend to the configuration without VGs can be observed in the dynamic cycles of the

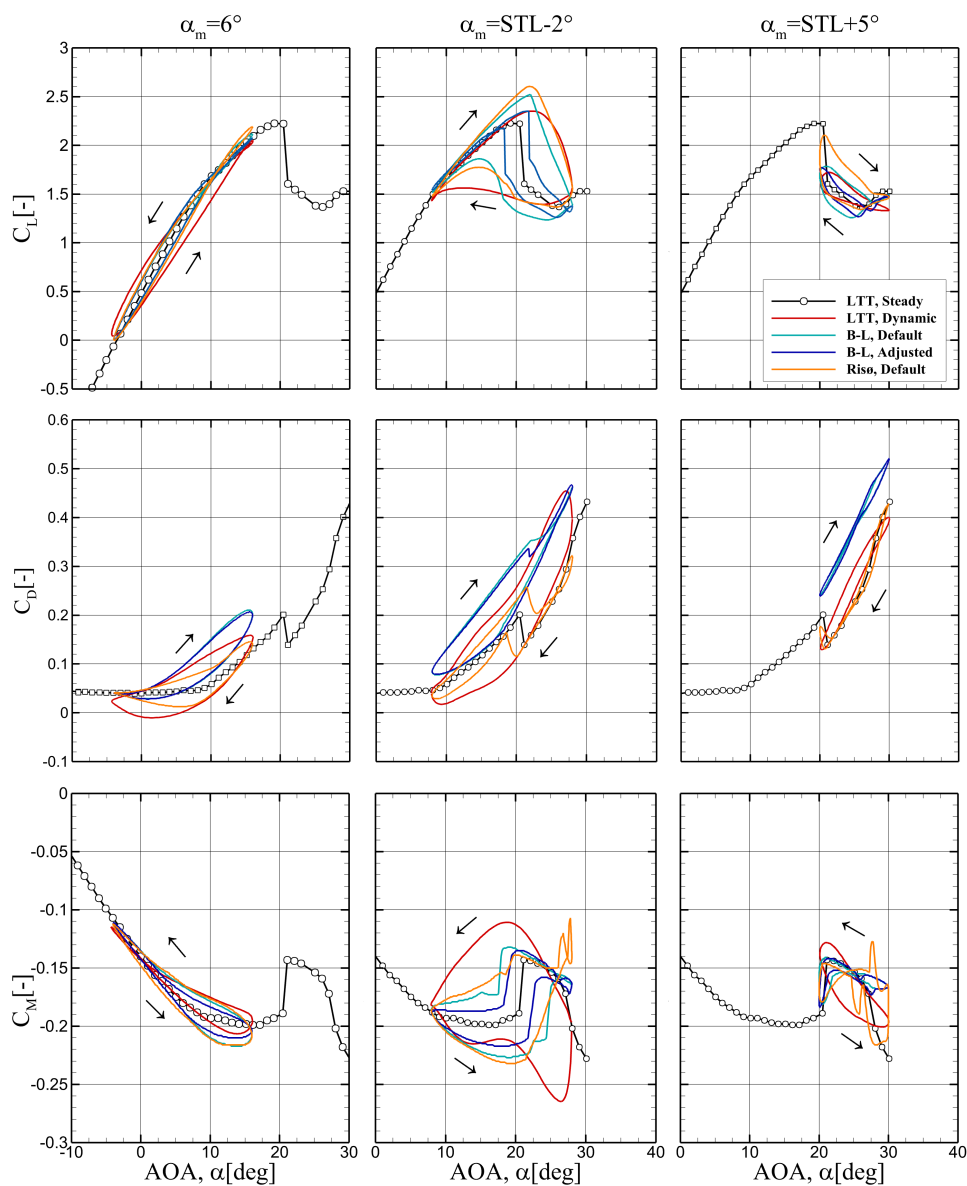


Figure 6.12: Measured and simulated dynamic polars in free transition flow regime, [$Re = 1 \times 10^6$, X-FB-35-10, $k = 0.064$].

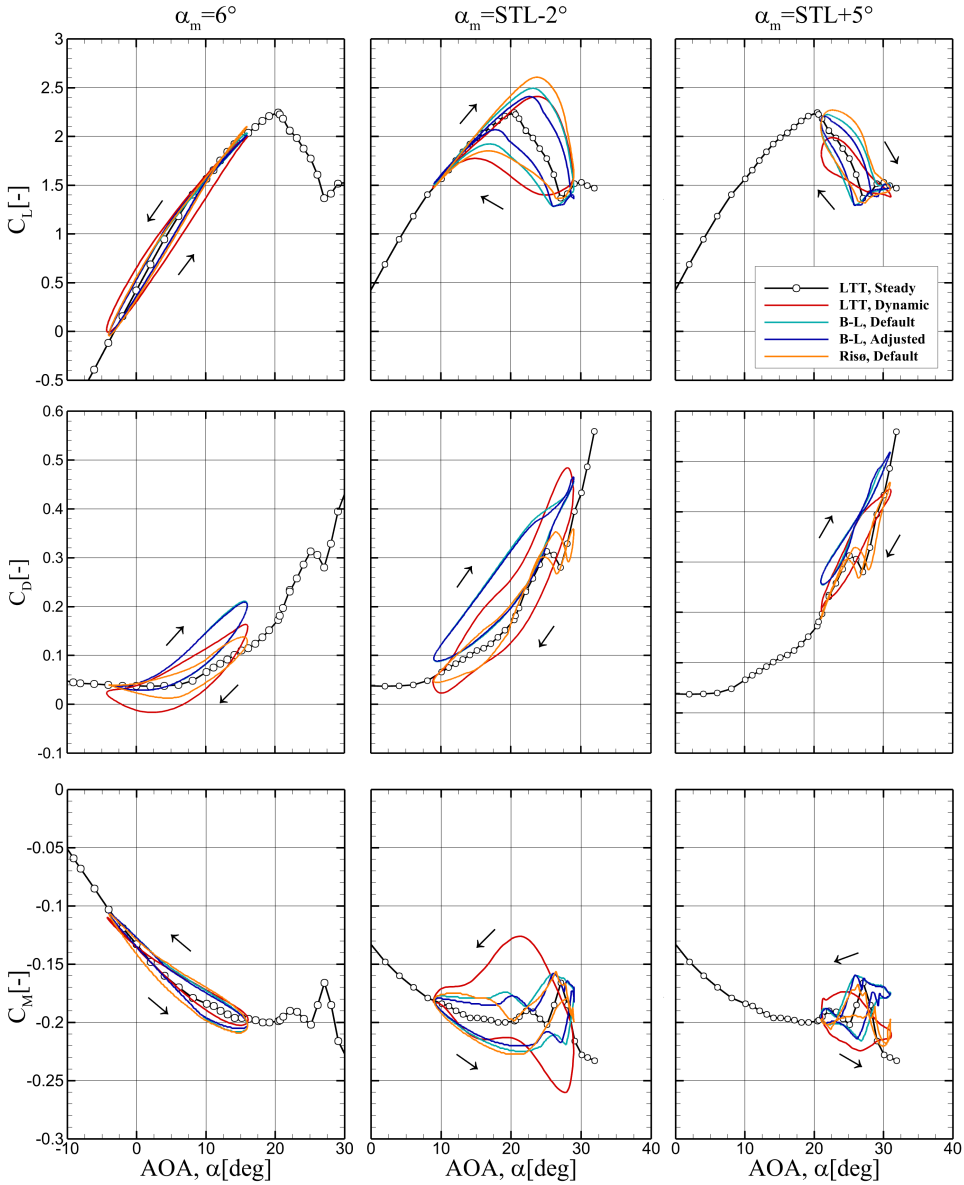


Figure 6.13: Measured and simulated dynamic polars in free transition flow regime, [$Re = 1 \times 10^6$, X-FB-35-10, $VG_{s(x/C)} = 0.3$, $k = 0.064$].

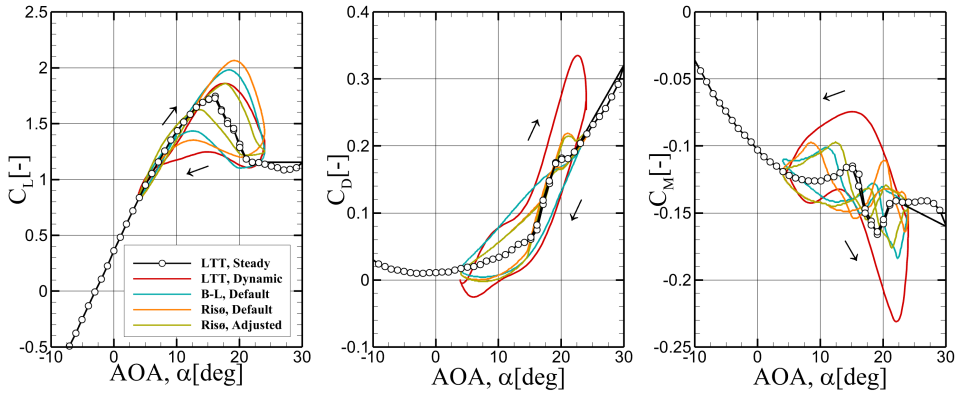


Figure 6.14: Measured and simulated dynamic polars in free transition flow regime, [$Re = 1 \times 10^6$, X-35-02, $k = 0.064$].

C_L . Both dynamic stall models overestimate the magnitude of the MLOS at $\alpha_m = STL-2^\circ$, with the Risø model showing a particularly pronounced overestimation. Furthermore, the DSAOA predicted by the B-L model is slightly lower than the experimental measurements. The tuned B-L model was only adjusted to match the MLOS value and not the DSAOA simultaneously, resulting in a larger discrepancy compared to the tuned model without VGs. Both models overpredict the lift fall during stall, and the accuracy of the tuned B-L model is reduced during the DS-phase, compared to the measurements. At $\alpha_m = STL+5^\circ$, all models exhibit an overestimation of the lift coefficient at the BSA and demonstrate a higher LFR than observed in the measurements.

For the prediction of drag and pitching moment of the airfoil equipped with VGs, a similar behavior to the configuration without VGs is observed. Specifically, the models fail to capture the trends observed in the measurements, as well as the maximum and minimum values of the dynamic cycles at $\alpha_m = STL-2^\circ$ and $\alpha_m = STL+5^\circ$, primarily due to inaccuracies in modeling the flow separation and reattachment phases.

Figure 6.14 depicts the comparison between the dynamic stall models with default constants and the tuned Risø model at $\alpha_m = STL-2^\circ$ for the X-35-02 airfoil. Note that the adjusted Risø model accurately predicts the MLOS and DSAOA in the C_L cycles. However, this adjustment leads to premature flow reattachment in the DS-phase, similar to the behavior observed with the B-L model. The tuned Risø model exhibits slight discrepancies in dynamic drag prediction compared to the default constants and fails to predict the cycle form and the DFR, which is also evident in the C_M cycle prediction, where the tuned Risø model shows no enhancements over the default constants.

Figures 6.15 and 6.16 show the comparative performance of both dynamic stall models for the X-35-02 airfoil under free transition flow conditions at a mean angle of attack of $\alpha_m = STL-2$. The results correspond to reduced frequencies of $k = 0.032$ and $k = 0.096$, re-

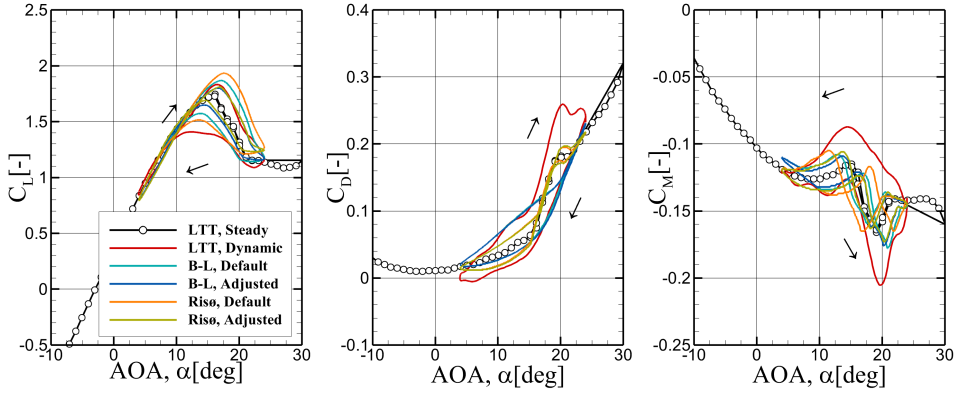


Figure 6.15: Measured and simulated dynamic polars in free transition flow regime, [$Re = 1 \times 10^6$, X-35-02, $k = 0.032$].

spectively, while employing the time constants previously adjusted for $k = 0.064$. Although these constants were originally calibrated for a mid-range reduced frequency, the adjusted models exhibit satisfactory predictive capability for the MLOS across both lower and higher reduced frequencies. This indicates a degree of robustness in the parameter tuning beyond the calibration point. In contrast, models using default constants demonstrate substantial inaccuracies, particularly in estimating the MLOS and the DSAOA. Furthermore, consistent with earlier observations, neither model successfully captures the dynamic evolution of dynamic C_D and C_M throughout the stall cycle.

A comparison with previous attempts to calibrate the B-L model reveals a clear trend.

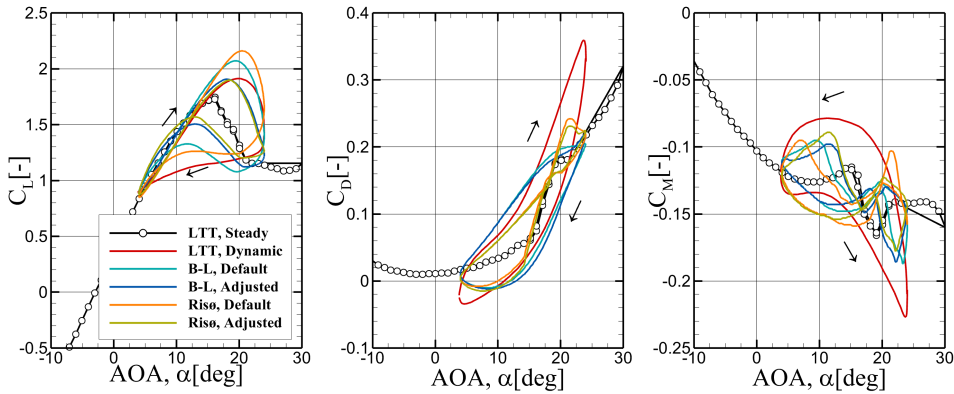


Figure 6.16: Measured and simulated dynamic polars in free transition flow regime, [$Re = 1 \times 10^6$, X-35-02, $k = 0.096$].

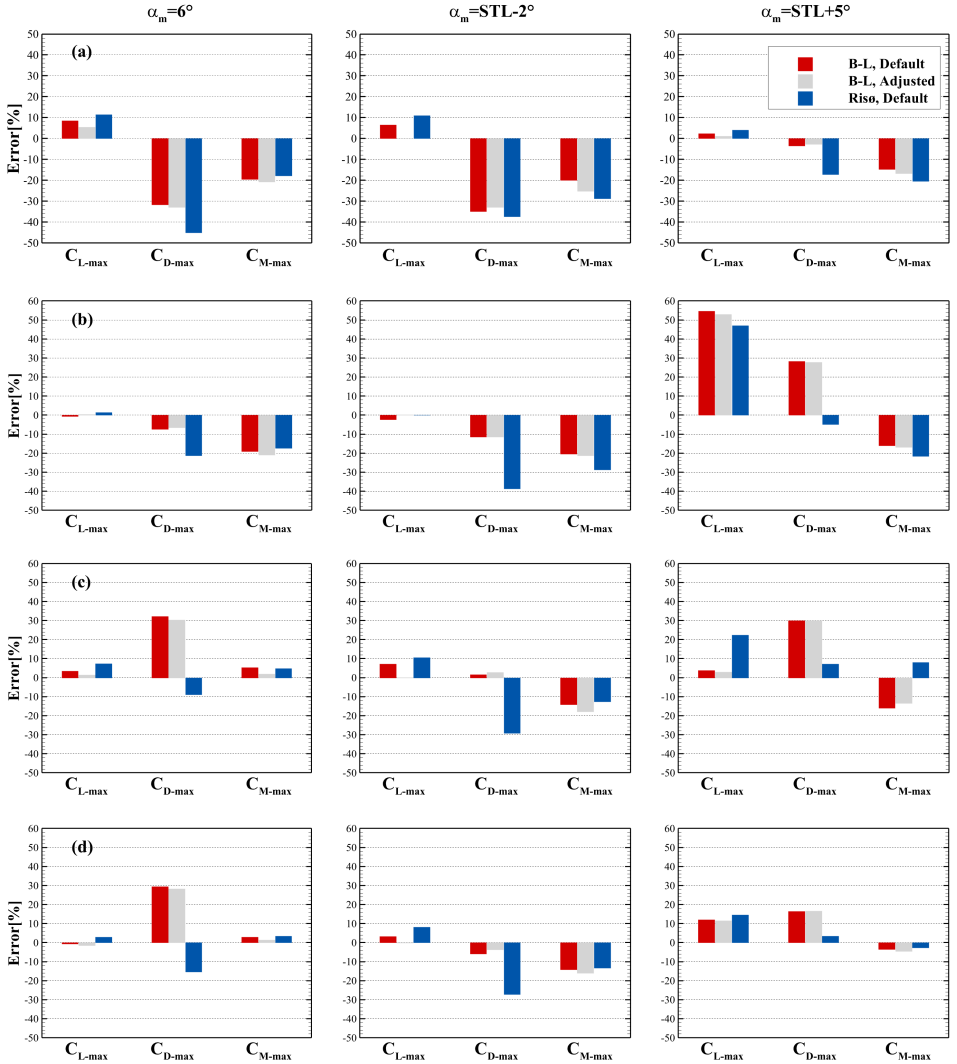


Figure 6.17: Variation in error of maximum aerodynamic coefficients across different airfoils at various mean angles of attack, [$Re = 1 \times 10^6$], (a): X-35-02, (b): X-35-02, $VGs_{(x/c)} = 0.3$, (c): X-FB-35-10, (d): X-FB-35-10, $VGs_{(x/c)} = 0.3$, $k = 0.064$.

The adjustments performed by Melani et al. [105] for the NACA 0012 airfoil demonstrate that the B-L model achieves good agreement with experimental data in both attached and separated flow regimes, i.e., during dynamic stall and post-stall conditions. The DSAOA, along with the characteristic increase in drag and pitching moment, is well captured by the model, confirming the validity of the Beddoes stall criterion for this class of airfoils.

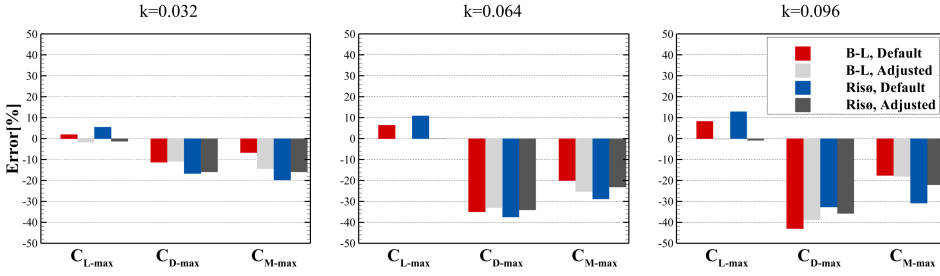


Figure 6.18: Variation in error of maximum aerodynamic coefficients for different reduced frequencies in free transition flow, [$Re = 1 \times 10^6$, X-35-02, $\alpha_m = STL-2^\circ$].

However, when the airfoil thickness increases to 21% (S809 airfoil), the model exhibits reduced calibration capability, particularly for unsteady drag and pitching moment coefficients. Despite this limitation, the inaccuracies in drag and pitching moment predictions remain smaller than those observed in the present study, and the peak angle of attack for these quantities is still predicted correctly. It should be noted that for both airfoils examined in the previous studies, the LEV module was active, unlike in the current investigation. Furthermore, differences in reduced frequency, mean angle of attack, and Reynolds number between the previous studies and the present work preclude a direct quantitative comparison. Therefore, only qualitative trends in the results can be meaningfully assessed.

Figures 6.17 and 6.18 present the percentage error between the calculated and measured maximum values for key aerodynamic coefficients at different mean angles of attack and reduced frequencies respectively. These coefficients play a vital role in aeroelasticity and aerodynamic load calculations, which are essential for determining the structural integrity and dynamic behavior of the blades. Any discrepancy between the calculated and measured values directly influences the reliability of load estimations. A significant error observed in these coefficients, such as 11% for C_{L-max} and 28% for C_{M-max} at $\alpha_m = STL-2^\circ$, provide a quantitative representation of the uncertainty in the load calculation process, using default time constants in the models. Note no improvements is seen in the C_{M-max} and C_{D-max} for the adjusted B-L model. Furthermore, the results indicate that an increase in reduced frequency leads to a greater error in predicting the maximum aerodynamic coefficients for both models. This underscores the importance of implementing refined modeling approaches or improved measurement techniques to enhance predictive accuracy. Note that the Normalized Mean Absolute Error (NMAE) of all predictions are shown in figures 6.19 and 6.20

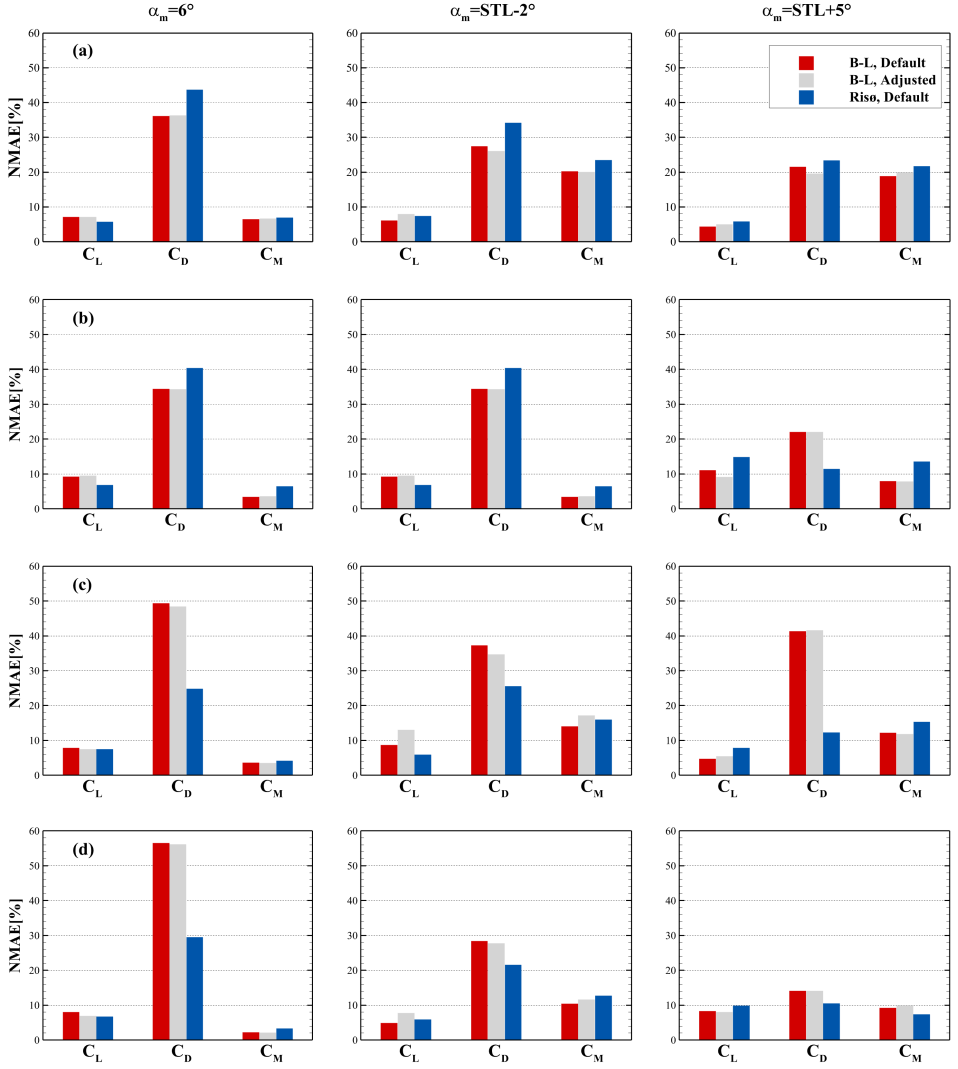


Figure 6.19: Variation of the NMAE error for different airfoils at different mean angle of attack, [$Re = 1 \times 10^6$, (a): X-35-02, (b): X-35-02, $VGs_{(x/C)} = 0.3$, (c): X-FB-35-10, (d): X-FB-35-10, $VGs_{(x/C)} = 0.3$, $k = 0.064$].

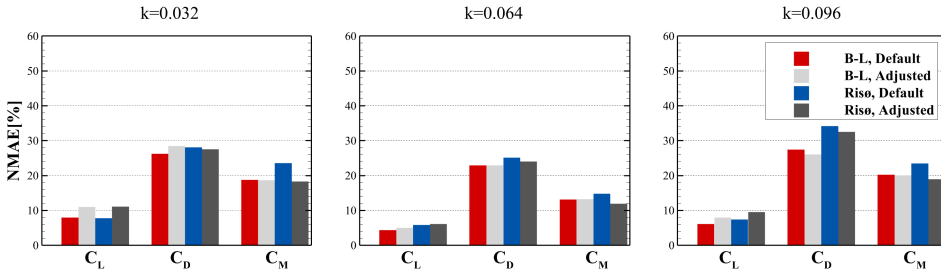


Figure 6.20: Variation of the NMAE for different reduced frequencies in free transition flow, $[Re = 1 \times 10^6, X-35-02, \alpha_m = STL-2^\circ]$.

DISCUSSION

The inaccuracy in modeling dynamic stall for thick airfoils is primarily attributed to the treatment of the trailing edge module in both models. Trailing edge flow separation leads to a loss of circulation and introduces nonlinear behavior in the lift, drag, and pitching moment characteristics. Both dynamic stall models employ the Kirchhoff/Helmholtz formulation [90], originally developed to model lift on a flat plate, to simulate the nonlinear effects of trailing edge separation on lift. This approach yields reasonable results for thin airfoils, where the stall mechanism typically involves a combination of progressive trailing edge separation and abrupt leading edge stall as the angle of attack increases [97, 105]. For such airfoils, the angle of attack range associated with trailing edge separation is relatively narrow, and the leading edge separation occurs at comparatively low angles of attack [128, 129]. The B-L and Risø models are based on this flow separation mechanism. In these models, the LEV —responsible for the peak values in C_L and C_M —is triggered shortly after trailing edge separation. This results in good agreement with experimental measurements for thin airfoils.

In contrast to thin airfoils, thick airfoils exhibit a fundamentally different flow separation mechanism, which alters the nonlinear lift behavior. These airfoils experience a stronger adverse pressure gradient on the suction side compared to thinner airfoils. Moreover, the LEV forms at significantly higher angles of attack (higher from the range of this study), while the trailing edge separation plays a dominant role in shaping the lift nonlinearities prior to stall onset [37, 130]. For thick airfoils, trailing edge separation is a gradual process that initiates at relatively lower angles of attack than in thin airfoils. As the angle of attack increases, the separated flow region slowly progresses toward the leading edge but remains confined behind the location of maximum airfoil thickness over a range of angles. This behavior has been observed by the authors through CFD and PIV measurements in this study, with detailed results to be presented in a forthcoming publication. The flow separation dynamics of flatback airfoils are even more complex than those of conventional thick airfoils. These airfoils exhibit multiple vortical structures that originate at the trailing

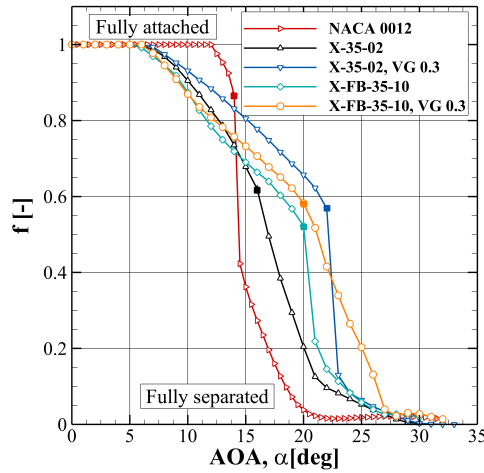


Figure 6.21: Comparison of the calculated effective separation point distribution for different airfoils.

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edge and along the suction surface. At post-stall angles of attack, these vortices merge, significantly altering the pressure distribution around the airfoil. Additionally, the VGs introduce a secondary vortical system oriented perpendicular to the primary one, further enhancing the flow nonlinearities. Consequently, the application of Kirchhoff's theory alone is insufficient to accurately model the complex flow structures associated with thick and flatback airfoils.

The comparison of the calculated effective separation point distribution, based on the Kirchhoff formulation for a NACA 0012 thin airfoil [131] and the thick airfoils studied in this research, is presented in figure 6.21. The steady stall angle of attack is indicated on each curve as a square. Note that the B-L model demonstrates good agreement with the dynamic measurements in various phases of dynamic stall [8], contrasting the presented results. The disparity between the calculated separation points for thin and thick airfoils is clearly visible. As previously mentioned, the NACA 0012 airfoil exhibits a short trailing edge separation phase (2°) until reaching the steady stall angle of attack, followed by a short phase (0.2°) at $f=0.7$, which is used to determine the critical angle of attack for triggering the LEV module [8]. Conversely, trailing edge separation for thicker airfoils initiates at much lower angles of attack, and a significant portion of the separation point curve is utilized to predict the dynamic stall and reattachment phases of the cycle.

Future studies may focus on refining the leading edge pressure peak concept, originally proposed by Beddoes [64], which serves as a criterion to link flow separation to the maximum suction peak near the leading edge (see equations 1.24–1.30). In this approach, Beddoes introduced a lagged normal force coefficient, C'_N , which is used to compute an equivalent lagged angle of attack. This lagged angle is then employed to determine the

dynamic separation point f_{bl} . Each of these steps is currently modeled using a first-order lag formulation. However, first-order lag models are limited in their ability to capture multiple time scales and the nonlinear behavior of vortex dynamics during separation and reattachment, particularly for thick airfoils. Replacing them with higher-order formulations could potentially improve model fidelity. Additionally, the normal force coefficient C_N^P in equation 1.24 could be corrected at higher angles of attack to better account for strong nonlinearities via additional term in the equation. Enhancing the formulation of f_{bl} may also lead to improved predictions of the pitching moment coefficient C_M during the trailing edge separation phase. Additionally, a novel approach is necessary for the B-L model to predict C_T that is not solely reliant on the C_N . Further investigations could explore the development of a new trailing edge separation model specifically tailored to the flow separation characteristics of thick airfoils, as a replacement for the Kirchhoff/Helmholtz-based approach (equation 1.23). Emerging machine learning techniques may also be leveraged to enhance this new formulation, provided that sufficient wind tunnel measurement data is available for training and validation. These proposed improvements are equally applicable to the Risø model, which employs a similar trailing edge separation framework.

6.5 CONCLUSIONS

The objective of this chapter is to investigate the accuracy of dynamic stall models on thick wind turbine airfoils equipped with VGs. To achieve this goal, a set of experiments was carried out in the LTT wind tunnel at TU Delft. Two distinct airfoils, X-35-02 and X-FB-35-10, were examined across various mean angles of attack, reduced frequencies, and VGs chord position. Aerodynamic coefficients were determined using surface pressure measurements and the results were compared to the B-L and Risø dynamic stall models. Since the PIV measurements showed no LEV in different airfoil configurations and test conditions, this module was disabled for the B-L model.

The investigation into the unsteady flow physics of the thick X-35-02 and X-FB-35-10 airfoils reveals a fundamental divergence from the classical LEV paradigms, established for thin airfoils. While dynamic stall on thin airfoils is typically a ‘leading edge initiated’ process triggered by the abrupt shedding of a coherent LEV, the stall mechanism observed in this study follows a gradual trailing edge separation progression. A main finding is the absence of discrete LEV formation under extreme operating conditions, which can be theoretically justified by the LESP criterion. The large leading edge radius of these thick airfoils dampens the local pressure gradient, maintaining a stable suction peak, that prevents the flow from reaching the critical LESP threshold required for vortex roll-up. Consequently, the flow remains attached at the leading edge longer than in thin airfoil cases. Furthermore, stereoscopic PIV measurements demonstrate that the off-body flow is dominated by an extended shear layer rather than a concentrated vortex core. Moreover,

the broad spatial distribution of vorticity weakens the development and growth of Kelvin-Helmholtz instabilities within the separated shear layer. Consequently, the flow does not organize into a coherent large-scale vortex, but instead dissipates into smaller turbulent structures. This results in a fluctuating separation bubble that dissipates into small-scale turbulence rather than achieving spatial coherence. Ultimately, these mechanisms lead to a moderated aerodynamic response. The absence of a convecting LEV precludes the impulsive lift transients common in thin airfoils, with load transients instead driven by the gradual forward expansion of trailing edge separation. These insights indicate that traditional dynamic stall models require topological adjustments to accurately predict the aeroelastic loads on the thick inboard sections of modern wind turbine blades.

The simulated lift cycles indicate that the models correctly predict the cycle direction. However, the discrepancy between the models and the measurements becomes more pronounced as the size of flow separation above the airfoil increases. This discrepancy is attributed to the lack of accuracy in the trailing edge separation/reattachment modeling within the models. Particularly, this discrepancy is more evident for the $\alpha_m = STL-2^\circ$ cases, where flow separation and reattachment occur during the US and DS-phases. This modeling inaccuracy leads to incorrect values for MLOS, DSAOA, post-stall lift drop, and lift in the DS-phase of a dynamic stall event. Since the Risø model is based on the B-L model, similar discrepancies are observed in the predictions of both models. Notably, no significant difference in lift cycle prediction accuracy is observed between flatback and non-flatback airfoils.

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The results indicate that models using default time constants tend to overpredict the MLOS magnitude, resulting in an overestimation of the load simulation on the rotor blade. Conversely, this inaccuracy is mitigated in the cases involving VGs. This behavior may be linked to the specific combination of VGs position and airfoil, requiring further investigation. Additionally, the time constants T_p and T_f can be utilized to adjust MLOS in dynamic stall models; however, this adjustment compromises the accuracy of reattachment in the DS-phase for both airfoils. Additionally, adjustments can be made to either the MLOS or the DSAOA, but not both simultaneously. This characteristic of model adjustment is also evident in the Risø model. Note that modifications to the indicial lift and moment function constants (A_i , b_i) primarily influence the attached flow portion of the dynamic stall cycle. Adjusting these parameters does not improve the accuracy of the model in predicting separated flow behavior. However, due to limited knowledge of their dependence on airfoil geometry, a study on the influence of airfoil thickness on these coefficients is recommended.

In general, both models fail to accurately predict the C_D and C_M . Adjusting the MLOS has minimal impact on these cycles. The Risø model utilizes steady drag to compute the dynamic C_D , resulting in more precise DFR in the post-stall region compared to the B-L model. However, the Risø model still struggles to accurately predict the magnitude of the cycles. The B-L model's dynamic C_D prediction for the flatback airfoil exhibits greater inaccuracy than for the X-35-02 airfoil, as the flatback airfoils have higher steady drag than

conventional airfoils. The B-L model's drag prediction method is based on the coefficient of normal force, hence this inaccuracy is expected. Similar inaccuracies in dynamic C_D predictions are observed for the X-35-02 airfoil with VGs. The predicted dynamic C_M cycles align with the measured trends and cycle directions when no trailing edge separation occurs for both airfoils and various configurations. However, in cases of flow separation, the models fail to capture the measured cycle trends and magnitudes.

This research reveals the notably poor predictive performance of the B-L and Risø dynamic stall models in capturing the unsteady aerodynamic behavior of thick conventional and flatback airfoils, both with and without VGs. These findings highlight significant limitations in the accuracy and reliability of dynamic load prediction and aeroelastic analysis for modern large wind turbines when using current dynamic stall models, particularly those employing default time constants. Given the widespread use of these models in scientific and industry-standard simulation tools such as HAWC2, OpenFAST, and Bladed, this study encourages further development of separation and reattachment modules specifically tailored to thick airfoils within existing dynamic stall methodologies.

Part III: Conclusions

7

CONCLUSIONS AND RECOMMENDATIONS

This chapter presents the principal conclusions derived from the research conducted in this thesis. It summarizes the key findings obtained throughout the study, highlighting the most significant scientific contributions and insights that advance the understanding of the investigated dynamic stall phenomena. The chapter ends with targeted recommendations for future work further refine the methods and broaden their scope.

7.1 OVERVIEW OF THE WORK

As the dimensions of modern wind turbine blades continue to grow to meet the demands of global energy production, the aerodynamic complexity of thick inboard-mid-span airfoil sections has emerged as a primary challenge for structural integrity and performance efficiency. While thin airfoil dynamic stall has been widely investigated, the complex behavior of thick conventional and flatback airfoils, especially when combined with passive flow control devices such as VGs remains insufficiently understood. This thesis addresses these gaps through a comprehensive experimental and numerical investigation of steady and unsteady aerodynamics, vortex dominated flow physics, and modeling limitations of dynamic stall on thick wind turbine relevant airfoils. These findings advance our understanding of dynamic stall physics, improve design insights for large rotor blades, and highlight key deficiencies in current industry-standard dynamic stall models. Below, the main outcomes of the thesis are summarized using the structure and titles of the individual chapters.

LARGE-SCALE WIND TURBINES

The design of modern large-scale wind turbine blades ultimately represents a carefully managed compromise between aerodynamic efficiency, structural integrity, and manufacturability. As turbine sizes continue to grow, the unfavorable scaling imposed by the square-cube law leads to disproportionately higher gravitational and aerodynamic loads, resulting in significantly larger bending moments that demand thick inboard airfoils capable of providing sufficient internal volume and stiffness. These increased loads also amplify aeroelastic sensitivities, making phenomena such as flutter, torsional divergence, and dynamic stall more critical design considerations than in earlier turbine generations. Thick and flatback airfoils in the root and mid-span regions have therefore become essential design solutions, providing improved structural efficiency and, in the case of flatback airfoils, enhanced aerodynamic performance. Passive flow control devices such as vortex generators further support these sections by helping to maintain attached flow under the high angles of attack typical of their operation. At the same time, the extreme size of contemporary blades intensifies both manufacturing and transportation challenges. Production facilities struggle not only to maintain geometric tolerances, but also to fabricate molds of sufficient accuracy and stiffness for such large structures. Transporting blades of this scale introduces further complications, as road infrastructure, particularly bridges, tunnels, and tight turning roads limits feasible routes. The assembly of these large components at the installation site adds another layer of complexity, demanding heavy lift equipment, precise alignment procedures, and favorable environmental conditions. These constraints demonstrate that modern blade design is shaped not only by aerodynamic and structural

considerations, but also by aeroelastic stability requirements and the practical limitations imposed by large-scale manufacturing, logistics, and deployment.

DYNAMIC STALL IN LARGE WIND TURBINES

The analysis of the large modern wind turbine shows that the inboard blade regions operate in highly unsteady aerodynamic environments that frequently exceed their static stall limits. Within the operational envelope defined by the IEC 61400-1 design load cases, thick inboard airfoil sections experience combinations of mean angle of attack and oscillation amplitude that place them deeply inside the dynamic stall regime, with amplitudes surpassing 30° . Under these conditions, dynamic stall vortex formation and shedding induce impulsive lift and moment overshoots, leading to pronounced local unsteady aerodynamic loading in the inboard blade sections. The presence of reduced frequency values above 0.3 further indicates strongly non-stationary flow behavior, enlarging aerodynamic hysteresis loops and promoting negative aerodynamic damping, which can excite or amplify modal instabilities in the blade's flapwise and edgewise vibration modes. Dynamic stall events arise regularly under both NTM and ETM inflow conditions, making them important contributors to ultimate and fatigue loading throughout normal operation. The results also show a systematic reduction in unsteady aerodynamic severity with decreasing airfoil thickness, clearly identifying the thick inboard blade sections as the regions most susceptible to dynamic stall in large-scale turbines.

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THICK CONVENTIONAL AND FLATBACK AIRFOILS IN STEADY FLOW

The wind tunnel measurements shows that the flatback airfoil (X-FB-35-10) delivers markedly superior aerodynamic performance compared to the conventional sharp trailing edge X-35-02 airfoil. The flatback geometry mitigates adverse pressure gradients on the suction side, stabilizes the boundary layer, and delays flow separation, which collectively result in a higher stall angle of attack, greater maximum lift coefficient, and a noticeably steeper lift curve slope. An additional advantage is the reduced sensitivity of the flatback airfoil to leading edge roughness, providing more robust performance under realistic operating conditions where surface contamination is inevitable. VGs further enhance aerodynamic performance by increasing lift and extending stall margins. This is particularly beneficial for sharp trailing edge airfoils, which are prone to flow separation and the associated increase in drag at high angles of attack when operated without VGs. Although flatback airfoils inherently introduce higher base drag due to their blunt trailing edge, this penalty is modest relative to the substantial gains in lift, stall resilience, and roughness insensitivity. Overall, the combination of flatback geometry and VGs provides a highly effective aero-

dynamic solution for thick inboard, mid-span blade sections, providing superior stability, performance robustness, and adaptability compared to conventional airfoil designs.

DYNAMIC STALL CHARACTERISTICS OF THICK WIND TURBINE AIRFOILS

The experimental investigations indicate that the flatback airfoil X-FB-35-10 shows markedly enhanced dynamic stability compared to the conventional sharp trailing edge X-35-02 airfoil, as reflected in smoother hysteresis loops, fewer lift fluctuations, and consistently higher mean dynamic lift slopes throughout the oscillatory cycle. These improvements arise from the more favorable suction side pressure gradient inherent to flatback geometries, which promotes larger lift overshoots and stabilizes the attached flow region. The development of the trailing edge separation bubble was observed to progress more gradually over the flatback configuration, resulting in a delayed onset of dynamic stall. However, this benefit weakens at higher reduced frequencies when light trailing edge separation becomes significant. Increasing reduced frequency generally amplifies the suction side pressure lag for both airfoils, slowing the separation process and enhancing dynamic lift overshoots, with this stabilizing effect being especially pronounced for the flatback airfoil, which experiences a notably diminished lift drop at stall onset. In the post-stall regime, the sharp trailing edge X-35-02 shows strong sensitivity to leading edge tripping and extensive flow separation, causing its dynamic lift to remain close to the static curve under forced transition, whereas the flatback airfoil maintains substantial dynamic lift fluctuations under the same conditions. From a design perspective, these results indicate that flatback airfoils can effectively delay dynamic stall and reduce the severity of stall-induced aerodynamic load fluctuations, potentially improving the dynamic response of blade sections subjected to unsteady inflow conditions. Combined with their known structural benefits, flatback airfoils present a promising pathway toward improving rotor stability, energy capture, and overall reliability in modern large-scale wind turbines.

Beyond these specific aerodynamic differences between the X-35-02 and X-FB-35-10 airfoils, the broader findings of this study indicate that dynamic stall behavior in thick airfoils is governed not only by geometric choices but also by subtle interactions between pressure gradient development, transition regime, and unsteady flow physics. Furthermore, the study has indicated several broader insights, i.e., the critical influence of reduced frequency on the severity of dynamic stall, the central role of the trailing edge separation bubble expansion in shaping unsteady lift response, and the strong dependency of post-stall behavior on both free and forced transition conditions. These findings reinforce that flatback airfoils offer genuine advantages in delaying dynamic stall onset and stabilizing the unsteady lift development, yet they also revealed that these benefits diminish under certain combinations of reduced frequency and trailing edge separation, a nuance with important implications for aeroelastic modeling and fatigue load prediction. More fundamentally, the

study shows that understanding dynamic stall for thick airfoils requires a combined view of steady stall characteristics, boundary layer behavior, and unsteady pressure development lag, all of which interact in nonlinear and sometimes counterintuitive ways.

VORTEX GENERATOR EFFECTS ON DYNAMIC STALL OF THICK AIRFOILS

The experimental results demonstrate that the combined application of VGs and flatback airfoil geometries substantially enhances the dynamic stall behavior of thick wind turbine blade sections. VGs significantly stabilized the US-phase by improving boundary layer attachment, increasing the overshoot of the normal force coefficient, and reducing aerodynamic discrepancies between sharp and blunt trailing edge configurations. Forward VGs placement proved consistently superior across a wide range of reduced frequencies, promoting earlier boundary layer re-energization and effectively suppressing undesirable force oscillations. These flow control benefits were particularly evident for the conventional sharp trailing edge airfoil, where VGs generated substantial improvements in stability, though they also led to larger overshoots that intensified the post-stall drop in lift. While the flatback airfoil's blunt trailing edge introduced a more complex vortex system, it nonetheless maintained attached flow more effectively and recovered more rapidly than the sharp trailing edge counterpart. Still, the combined effects of flatback geometry and VGs may generate very large peak loads, indicating a trade-off between dynamic stall suppression and structural loading that requires careful consideration. Overall, the results show that increased trailing edge thickness, strategic VGs placement, and operation at higher reduced frequencies contribute collectively to delaying dynamic stall, stabilizing the unsteady aerodynamic response, and mitigating vortex-induced vibrations, thereby improving fatigue resistance and operational reliability under turbulent inflow conditions.

At a broader level, the study reveals important aerodynamic mechanisms through which VGs influence dynamic stall behavior in thick airfoils. The results show that VGs do far more than simply energize the boundary layer, i.e., they fundamentally reshape the unsteady aerodynamic response by modifying the timing, structure, and progression of separation throughout the oscillation cycle. A key lesson is that VGs effectiveness is governed by a complex interaction between boundary layer re-energization and the inherent time-lag of the unsteady flow. This interaction becomes particularly evident at higher reduced frequencies, where the natural delay in suction side pressure development amplifies the stabilizing influence of VGs, prolonging attached flow and suppressing large amplitude flow oscillations that typically trigger dynamic stall. The transition regime also plays a decisive role, as VGs reduce the sensitivity of the flow to free transition fluctuations, narrowing the gap between free and forced transition behavior and yielding more predictable aerodynamic response. Furthermore, the experiments indicate that VGs performance is highly dependent on chordwise placement: forward positions strengthen

early boundary layer momentum, delay the onset of separation, and provide the most robust mitigation of dynamic stall across a broad range of frequencies, whereas mid-chord and aft positions fail to counteract the unsteady growth of the separation bubble at critical phases of the pitch cycle. This research therefore shows that effective VGs design for wind turbine applications cannot rely solely on steady-state considerations but must account for the time-dependent nature of separation, the frequency-dependent pressure lag, and the geometric vortex interactions unique to thick airfoils.

EVALUATING DYNAMIC STALL MODELS FOR THICK WIND TURBINE AIRFOILS

The experimental and modeling investigations conducted in this thesis shows that dynamic stall on thick wind turbine airfoils fundamentally differs from the classical LEV-dominated behavior known from thin airfoil aerodynamics. Stereoscopic PIV measurements reveal that the unsteady flow-field is characterized by a broad, diffuse shear layer rather than a coherent vortex structure, as the distributed vorticity prevents the subharmonic resonance necessary for global vortex formation. This produces a fluctuating separation bubble that quickly breaks down into small-scale turbulence, leading to load transients governed by the gradual forward migration of trailing edge separation rather than impulsive lift overshoots. The absence of discrete LEV formation under extreme unsteady conditions is confirmed both experimentally, consistent with the LESP criterion, as the large leading edge radius of thick airfoils suppresses the local pressure gradients required for vortex roll-up. Although the B-L and Risø dynamic stall models correctly reproduce the direction of the lift hysteresis loop, their accuracy deteriorates as trailing edge separation grows, leading to substantial errors in maximum lift overshoot, dynamic stall angle of attack, and post-stall behavior. Both models overpredict the magnitude of the lift overshoot when using default time constants, and although tuning these parameters can reduce the overshoot, it compromises separation and reattachment predictions in the DS-phase. Finally, neither model accurately captures the dynamic drag or pitching moment, with particularly poor drag prediction in flatback configurations. These findings collectively show that standard B-L-type models exhibit structural limitations when applied to thick airfoils with or without vortex generators, and that their dynamic load predictions should therefore be interpreted cautiously when used in tools such as HAWC2 or OpenFAST.

In addition to the detailed assessment of dynamic stall on thick airfoils, this chapter provides a broader insight that deepens the central narrative of the thesis: namely, that the aerodynamic response of modern, large-scale wind turbine blades cannot be reliably captured using dynamic stall models originally formulated for thin airfoil behavior. The evidence presented here shows that the governing flow topology, separation physics, and unsteady load mechanisms in thick airfoils diverge sharply from conventional assumptions, implying that the limitations observed in aeroelastic load predictions stem from

fundamental model–physics mismatches rather than parameter tuning issues alone. This understanding reinforces the broader theme of the thesis, that accurate aero-structural prediction for next generation turbine blades requires physics-based refinements in dynamic stall modeling, particularly in the treatment of trailing edge separation and reattachment, as well as improved integration of high-fidelity experimental data such as PIV. The chapter thus delineates the future direction of modeling efforts, emphasizing the need for new or improved dynamic stall methods specifically designed for thick airfoils and the boundary layer control devices applied in large modern rotors.

7.2 OUTLOOK AND RECOMMENDATIONS

The major recommendations for future studies based on this thesis are discussed below:

Expand the experimental envelope to operating conditions representative of utility-scale turbines. The results show that flatback geometries produce more stable hysteresis loops in the attached flow portion, delay stall onset, and yield larger overshoots, compared to the conventional airfoils. These trends depend not only on the geometry of the airfoil, but also on reduced frequency and flow transition regime, and VGs geometry (VGs chord position, VGs height, etc). These mechanisms should be examined over thicker airfoils, different airfoil families, higher Reynolds number ranges, higher reduced frequencies, negative angles of attack, and especially higher angles of attack in the post-stall region, to reflect inboard blade sections of multi-MW wind turbines.

Incorporate rotation and three-dimensionality to assess the persistence of observed 2D mechanisms. Since the present conclusions are derived from 2D wind tunnel experiments, the next step is to test rotating blade segments (or use a high aspect ratio rotating rig) to quantify how spanwise flow, Coriolis effects, and centrifugal effects modify the dynamic stall characteristics. The absence of a discrete LEV in the stereoscopic PIV indicates a fundamentally different dynamic stall topology for thick profiles; it is therefore important to determine whether rotation suppresses or enhances shear layer roll up, modifies the timing of reattachment, and changes the hysteresis loop area and cycle asymmetry on both flatback and conventional airfoil geometries with and without VGs.

Systematic optimization of VGs geometry and placement in dynamic environments. The results demonstrate that forward VGs placement stabilizes the upstroke, enhances the normal force overshoot, mitigates flow instabilities at higher reduced frequencies, and decreases the disparity between the dynamic cycles of

flatback and sharp trailing edge airfoils. At the same time, this configuration can intensify the subsequent force drop and amplify separated phase fluctuations at low reduced frequencies. These competing effects indicate a substantial potential for optimization in VGs application. Future studies should systematically vary VGs height, length, angle, spacing, and chordwise location to identify robust configurations that achieve flow stabilization while maintaining bounded overshoot and minimizing post-stall penalties. Moreover, “optimal” VGs placements are likely to differ across the attached, pre-stall, and near stall operating regimes, making multi objective optimization essential. Such optimization approaches should explicitly incorporate fatigue related metrics in addition to conventional aerodynamic performance indicators, ensuring that both aerodynamic and structural requirements are satisfied.

High-Fidelity CFD for dynamic stall analysis and optimization. High-fidelity CFD approaches, such as Detached Eddy Simulation (DES) and Large Eddy Simulation (LES), can be employed to investigate the detailed flow-field and extract key aerodynamic features, including separation bubble length, reattachment mechanism, shear layer spectral characteristics, and surface pressure dynamics for both flatback and conventional airfoils, with and without VGs, at higher Reynolds numbers. These simulations are particularly valuable when the aerodynamic behavior shows strong Reynolds number dependence, or when the addition of VGs alters the Reynolds scaling characteristics of the airfoil. Furthermore, CFD can support the optimization of VGs configurations. By systematically varying VGs geometry and evaluating the resulting flow physics, high-fidelity simulations can help identify designs that achieve an optimal balance between lift overshoot enhancement and acceptable post-stall behavior across different operating scenarios. This approach allows a more efficient and physics informed exploration of the trade-off space than would be feasible through experiments alone. In addition, DES and LES offer a cost-effective and flexible alternative to the inherently complex and expensive wind tunnel measurements required to characterize dynamic stall on thick airfoils. CFD simulations allow controlled parametric studies across a wide range of airfoil geometries, VGs configurations, and dynamic operating conditions, thereby extending the range of scenarios that can be analyzed beyond what is feasible experimentally.

Physics-based enhancements to dynamic stall models for thick airfoils. The evaluation shows that the B-L and Risø models reproduce the general loop direction but fail to capture key cycle characteristics for thick airfoils. These inaccuracies stem mainly from the thin-airfoil assumptions embedded in their trailing edge separation module, which is based on the Kirchhoff/Helmholtz flat-plate formulation. Thick airfoils, however, exhibit early and gradually developing trailing edge separation, delayed or absent LEV formation, and much stronger adverse pressure gradient effects. Moreover, for flatback geometries and VGs configurations, multiple interacting

vortical structures further increase the complexity of the flow. These mechanisms cannot be modeled correctly by a single lagged separation parameter or by the first-order time-lag formulations used in current models. Future developments should therefore introduce a dedicated trailing edge separation state with its own evolution equation, incorporate higher-order lag dynamics to capture the multiple time scales of separation and reattachment, and decouple drag and moment predictions from C_N by adding shear-layer and surface pressure contributions linked to bubble size and reattachment mechanism. Geometric effects, such as airfoil relative thickness, leading edge radius, and trailing edge bluntness, should be embedded directly into model coefficients, and VGs influences should be included through source terms that modify effective momentum thickness and stall-delay behavior. Improved formulations of C_N^P , f_{bl} , and C_T would further enhance predictive accuracy in the nonlinear, high angle of attack regime. A new trailing edge separation module, potentially supported by data-driven techniques, should replace the Kirchhoff-based approach and be implemented within the existing B-L and Risø methods. Such a model would improve predictions in the regions where the largest discrepancies currently occur, including the dynamic stall lift overshoot, post-stall lift drop, and the C_D and C_M cycles.

7

7.3 FINAL WORDS

This thesis investigated the dynamic stall characteristics of a high relative thickness conventional sharp trailing edge and a flatback airfoil representative of the mid span region of modern large wind turbines, under various configurations and unsteady operating conditions. The work presented here constitutes a pioneering experimental study in this field, and the results provide a valuable foundation for developing new dynamic stall models tailored to thick airfoils or for adapting existing formulations to better capture their behavior. Nevertheless, this research represents only an initial step toward a deeper understanding of unsteady aerodynamics for thick wind turbine airfoils. Further studies, both experimental and computational, are required to refine the physical models, expand the operating envelope, and ultimately enable more accurate prediction and control of the complex aerodynamic phenomena governing next generation wind turbine blades.

Part IV: Appendices

A

DYNAMIC STALL MODELS METHODOLOGY AND VALIDATION

This appendix presents the validation of the Beddoes-Leishman and Risø dynamic stall models.

To validate the Beddoes-Leishman and the Risø dynamic stall models, the mathematical formulations of both dynamic stall models, as outlined in sections 1.2.3.1 and 1.2.3.2, were implemented in Python. The governing ODEs associated with each model were solved using a second-order time discretization scheme to ensure numerical stability and sufficient temporal accuracy. Prior to applying the models to the experimental datasets considered in this work, the correctness of the numerical implementations was verified through validation against published reference results. For the validation of the B–L dynamic stall model, recent results reported by Melani et al. [105] were adopted. In their study, Melani et al. employed experimental measurements conducted by McCroskey et al. [132] on a harmonically pitching NACA 0012 airfoil at a Reynolds number of $Re = 4.2 \times 10^6$ and a reduced frequency of $k = 0.1$. Their implementation results were compared against the numerical results reported by Leishman et al. [133]. The default model constants, as documented in [121], [99], and [119], were employed, consistent with the values summarized by Melani et al. [105].

Figure A.1 illustrates the comparison between the aerodynamic coefficients obtained from the present B–L model implementation and the corresponding reference results. Overall, a very good agreement is observed, indicating that the implemented model is capable of accurately reproducing the dynamic stall hysteresis loops and unsteady aerodynamic response. Minor discrepancies between the two datasets are attributed primarily to differences in static airfoil data curve fitting and to the numerical solution of the ODE system.

The validation of the Risø dynamic stall model was performed using reference results presented by Hansen et al. [13] for a NACA 6315 airfoil. It should be noted that the exact values of the indicial function constants used in that study are not explicitly reported. In the present work, the classical flat-plate constants introduced by Wagner [102] were

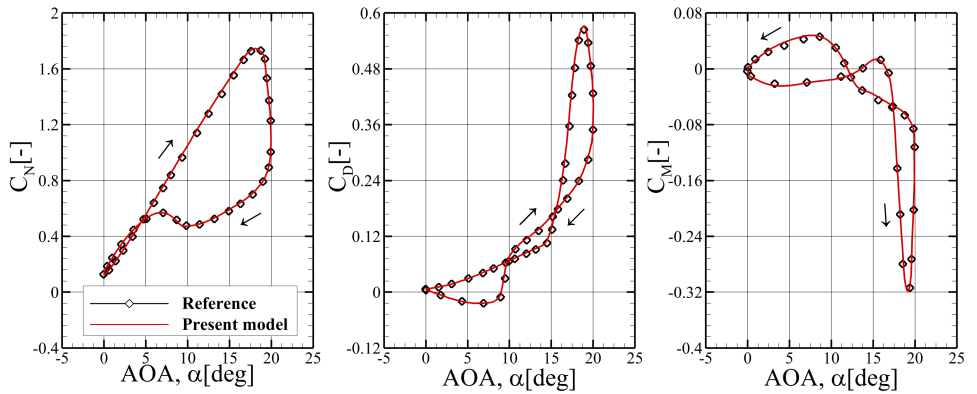


Figure A.1: Comparison between the the reference B–L model [105] and the current implemented model, [$Re = 4.2 \times 10^6$, NACA 0012, $A=10^\circ$, $\alpha_m=5^\circ$, $k = 0.1$].

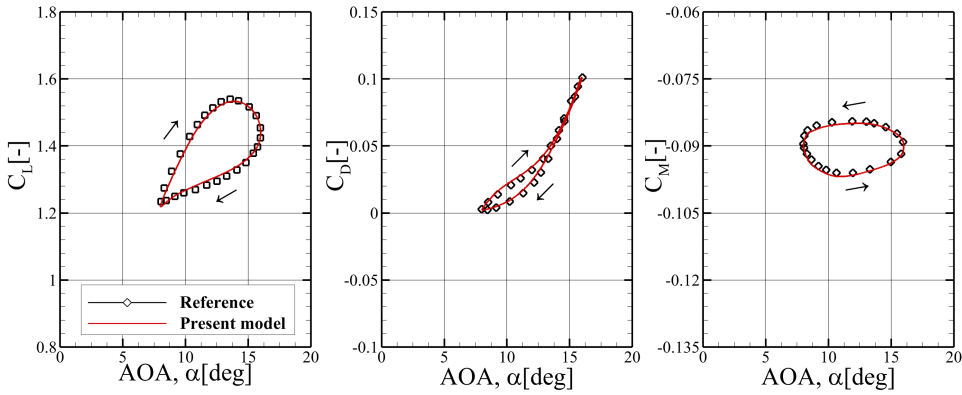


Figure A.2: Comparison between the the reference Risø model [105] and the current implemented model, [NACA 6315, $A=4^\circ$, $\alpha_m=12^\circ$, $k=0.1$].

adopted, namely $A_1 = 0.165$, $b_1 = 0.0445$, $A_2 = 0.335$, and $b_2 = 0.3$. For the time constants associated with pressure and separation dynamics, $T_p = \tau_p T_u$ and $T_f = \tau_f T_u$, the default HAWC2 values were used, with $\tau_p = 1.5$ and $\tau_f = 6$, following [122].

A comparison between the results obtained from the present Risø model implementation and the reference results is shown in figure A.2. The comparison demonstrates an overall good agreement between the two implementations, confirming the correctness of the numerical formulation and parameter selection. Remaining deviations are considered acceptable and are mainly attributed to uncertainties in the model constants and static data curve fitting. A detailed methodology for the calibration and further refinement of both dynamic stall models is presented in chapter 6.

B

MULTIDISCIPLINARY DESIGN AND AERODYNAMIC OPTIMIZATION OF FLATBACK AIRFOILS

This appendix summarizes the research conducted at Hochschule Bremen between 2008 and 2011, focusing on the multidisciplinary design optimization of flatback airfoils for multi-megawatt wind turbine applications. The work combines advanced MDO techniques with detailed aerodynamic analysis to evaluate and enhance flatback airfoil performance. The resulting airfoil designs provide improved aerodynamic efficiency and structural robustness, forming the basis for subsequent experimental and numerical investigations. This work was funded by the German Ministry of Education and Research (BMBF FKZ: 17N2107).

B.1 SUMMARY

B

Recent research on the design of large multi-megawatt wind turbine blades indicates that employing blunt, or flatback, airfoils in the inboard blade region provides significant structural and aerodynamic advantages. In this work, six flatback airfoils with relative thicknesses ranging from 35% to 50% and multiple trailing edge gap configurations were designed and optimized under both two-dimensional and quasi-3D flow conditions. The aerodynamic behavior of the airfoils was further investigated through wind tunnel experiments and CFD simulations using both RANS and DES methods. Given the large number of coupled aerodynamic and structural parameters involved in airfoil design, a multidisciplinary, non-gradient-based Simulated Annealing (SA) optimization algorithm, enhanced with fuzzy-logic decision support, was employed. This optimization method demonstrated strong capability in handling highly nonlinear objective functions. The CFD analyses within the optimization loop were conducted using 2D and quasi-3D RANS simulations implemented in the commercial solver Fluent at a design Reynolds number of $Re = 6 \times 10^6$. The results show that the optimized flatback airfoils achieve notable improvements compared to currently used flatback designs. These improvements include higher lift coefficients, increased sectional moment of inertia, and reduced sensitivity to leading edge surface roughness. Additionally, airfoils optimized using the quasi-3D solver exhibited superior aerodynamic and structural performance relative to those optimized using a purely 2D flow solver. Complementary experimental and high-fidelity numerical investigations further revealed the complex wake dynamics associated with flatback airfoils, including the formation of counter-rotating recirculation zones and coherent vortex shedding behind the blunt trailing edge. The results highlight the significant influence of rotational augmentation, captured via quasi-3D simulations, on both steady and unsteady aerodynamic behavior, particularly near stall conditions. It was shown that radial flow effects modify separation characteristics and enhance suction side pressure recovery, thereby improving performance at high angles of attack. These findings show the importance of incorporating three-dimensional rotational effects during the airfoil design process. Moreover, the study points to the need for future aeroacoustic optimization strategies to mitigate increased noise associated with blunt trailing edge configurations.

B.2 BACKGROUND, PROBLEM DEFINITION, AND OBJECTIVES

B.2.1 INTRODUCTION

B

The purpose of this part is to review wind turbine airfoils, the aerodynamic phenomena that influence their performance, and the key challenges that hinder further wind turbine development. The first section provides an overview of previous developments in wind turbine airfoil design. The second section discusses the essential aerodynamic and structural characteristics required for modern wind turbine airfoils. The third section examines flatback airfoils and their critical role in multi-megawatt turbine applications. Section four analyzes the effects of blade rotation on airfoil performance. In the fifth section, various optimization methodologies relevant to airfoil design are introduced. Finally, the part concludes with an outline of the motivation, objectives, and overall goals of the present research.

B.2.2 WIND TURBINE AIRFOILS

The design and development of wind turbine airfoils constitute a fundamental step in the evolution of modern wind energy technology. As rotor sizes have increased and turbine performance requirements have become more demanding, the need for airfoils specifically tailored to wind turbine operating conditions has grown correspondingly. In the early stages of wind turbine development, classical NACA aviation airfoils, particularly the NACA 63-2XX, 63-4XX, and 63-6XX families, were widely adopted for rotor blades [134, 135]. These airfoil families were applied extensively in wind energy applications due to the availability of comprehensive wind tunnel data, the accumulated operational experience of turbine manufacturers, and their favorable geometric compatibility across a broad range of relative thicknesses. This compatibility allowed designers to use different members of the same airfoil family at various radial positions along the blade, thereby simplifying manufacturing and ensuring acceptable aerodynamic continuity. However, despite these practical advantages, the NACA airfoil families were originally developed for aviation applications and therefore lacked several aerodynamic and structural characteristics required for efficient wind turbine operation [134]. For instance, the very thick airfoils required in the inboard section of rotor blades were often generated by scaling thinner NACA airfoils to higher thicknesses. Such geometric scaling resulted in significant degradation of aerodynamic performance, including reduced lift-to-drag ratios and earlier stall onset. In addition, the NACA airfoils exhibited strong sensitivity to leading edge roughness caused by dust, insects, or ice accretion; undesirable stall behavior; and pronounced post-stall vibrations, especially at high angles of attack [136–138]. These issues rendered the NACA

series suboptimal for use in wind turbines. Notably, the annual loss in energy production due to leading edge contamination alone has been reported to reach up to 30% [139]. Such limitations, combined with the need for thick structural airfoils in the root region, provided a strong motivation for the development of wind turbine specific airfoils.

The first low-speed airfoils designed explicitly for wind energy applications included the NASA LS(1) series and the Wortmann FX xx-W-xx series [140]. Although these airfoils were better suited for low Reynolds number conditions typical of wind turbine blades, their aerodynamic performance was still limited, particularly under off-design or roughness-affected conditions [139, 141].

A major and systematic effort toward the development of purpose-built wind turbine airfoils began in the mid-1980s. From 1984 to 1995, Tangler and Somers at the National Renewable Energy Laboratory (NREL), formerly known as the Solar Energy Research Institute (SERI), designed nine airfoil families comprising a total of 25 sections using the Eppler code [136, 139, 142]. These airfoils, designated S801 through S829, were created for stall-regulated, variable-pitch, and variable-RPM wind turbines. Their primary design objective was to achieve low sensitivity to leading edge roughness while maintaining acceptable aerodynamic performance. The NREL airfoil families were primarily intended for small wind turbines, especially stall-regulated ones, which were prevalent during that period.

In 1990, Björk [143] introduced the FFA-Wx-xxx airfoil families, developed using the XFOIL code [144]. The first character in the designation indicates the airfoil series, while the subsequent digits denote the relative thickness. These airfoils were designed to cover a broad thickness range from thin, high-speed airfoils to thick, structurally robust ones, while maintaining low roughness sensitivity and moderate to high maximum lift coefficients.

A significant contribution to modern wind turbine airfoil development was made by Timmer and van Rooij [19, 145] at Delft University of Technology. They developed a series of high-performance airfoils for both the root and tip regions of rotor blades. Their work relied on XFOIL and its modified version, RFOIL, which incorporates corrections for rotational effects that influence airfoil performance on rotating blades. The resulting airfoils, designated DU xx-W-xxx, have relative thicknesses ranging from 15% to 40% of the chord line. The first two digits denote the year of design, while the last three digits correspond to the relative thickness. Additional airfoil developments for wind-turbine applications are documented in [146–148].

Another major effort in airfoil development occurred at the Wind Energy Department of Risø National Laboratory in Denmark starting in the late 1990s [149]. Fuglsang and Dahl designed the Risø-A-XX family, which comprises six airfoil sections with relative thicknesses ranging from 12% to 30%. These airfoils were intended for stall-regulated kilowatt-scale wind turbines. The last two digits of the designation indicate thickness. Similar to other modern designs, these airfoils were developed using XFOIL in combination with a multidisciplinary optimization methods. The design objectives included low sensitivity to leading edge roughness, high aerodynamic efficiency, and geometric compatibility across

thickness levels. Wind tunnel and field test data for these airfoils are reported in [150, 151]. In 2001, Risø introduced the Risø-P family, intended to replace the earlier Risø-A1 airfoils for application in pitch-regulated and higher rated power turbines. This family consists of four airfoils with relative thicknesses between 15% and 24%. Subsequently, the Risø-B1 family was developed, also in 2001, with the goal of achieving high lift for megawatt-scale, variable-speed, pitch-controlled turbines. The Risø-B1 series contains seven airfoil sections with relative thicknesses ranging from 15% to 53% [14, 152]. Wind tunnel data for several Risø-B1 sections are available in [153, 154]. Both the Risø-P and Risø-B1 families were designed using XFOIL. The most recent family from Risø, the Risø-C2 series introduced in 2007, represents a further advancement in aerodynamic performance, offering improved characteristics compared to the earlier Risø-B1 family [155]. These modern airfoils are particularly suitable for high efficiency, large-scale turbine applications.

B.2.3 WIND TURBINE AIRFOIL CHARACTERISTICS

The desirable characteristics of wind-turbine airfoils have been examined extensively in the literature, as documented in [14, 19, 136, 141]. In general, these characteristics may be grouped into two broad categories: aerodynamic properties and structural properties. Both sets of requirements play crucial roles in determining the overall performance, reliability, and efficiency of modern wind turbine blades.

Tangler and Somers (1987) [136] provided one of the earliest systematic descriptions of airfoil requirements for wind turbines in the 10–30m rotor diameter class. They emphasized that wind turbine airfoils must exhibit a maximum lift coefficient with low sensitivity to leading edge roughness, which is commonly caused by environmental contaminants such as dust, insects, and ice. For outboard blade regions, where design lift coefficients are moderate, a high lift-to-drag ratio is essential to maximize aerodynamic efficiency. At the same time, fatigue loads are reduced when maximum lift coefficient gradually decreases from root to tip, helping to limit cyclic blade bending moments. In contrast, the inboard region of the blade requires higher maximum lift coefficients, as this portion of the blade produces relatively low aerodynamic torque due to its small tangential velocity. Consequently, the inboard airfoils do not require extremely low roughness sensitivity. Additionally, the pitching moment coefficient must remain sufficiently low for wind-turbine applications to minimize torsional loading on the blade. According to the investigation of Björk (1990) [141], airfoil thickness represents a critical parameter for structural purposes. Manufacturers generally favor airfoils with concave pressure side surfaces because such shapes are structurally advantageous. Moreover, positioning the maximum thickness location further forward (closer to the leading edge) shifts the shear center and center of gravity toward the aerodynamic center, yielding improvements in aeroelastic stability and reducing torsional deformations. Although increasing airfoil thickness typically reduces

the L/D ratio, this decrease is primarily relevant for outboard airfoils. In the inboard region, the primary challenge is not the reduced efficiency, but rather the tendency of thick airfoils to exhibit lower $C_{L,max}$ which limits torque production. Since $C_{L,max}$ directly influences the structural loads in stall-regulated turbines, the design angle of attack must not be selected too close to the stall angle. The design lift coefficient typically corresponds to the angle of attack at which the L/D ratio is maximized, and it depends on chord length, tip-speed ratio, and structural constraints. Maintaining low sensitivity of $C_{L,max}$ to leading edge roughness is essential for reliable turbine performance. Ideally, the difference between the clean and contaminated $C_{L,max}$ should be minimal. Similarly, low pitching moment coefficients are advantageous for pitch-regulated wind turbines, as they reduce the actuator power required to adjust blade pitch. Conversely, large pitching moment coefficients can twist the blade and shift the optimal twist distribution. Another important parameter is the thickness of the upper-surface of the airfoil: thinner upper surfaces reduce adverse pressure gradients and delay boundary layer separation, yielding higher $C_{L,max}$ lower drag, and lower sensitivity to roughness.

Investigations by Timmer and van Rooij [19] at Delft University further highlight the importance of upper-surface thickness distribution. For pitch-controlled turbines, thin airfoils with relatively high design lift coefficients are desirable because they allow for shorter chord lengths, thereby reducing extreme load levels such as storm loads. In contrast, stall-regulated turbines rely on moderate design lift coefficients in the outboard region to ensure predictable stall behavior. For thick inboard airfoils, a high $C_{L,max}$ is required to generate sufficient low wind speed torque. Increasing $C_{L,max}$ enables designers to reduce chord length and blade mass, which can significantly ease challenges associated with manufacturing, transportation, and maintenance of large blades. Additionally, 3D-rotational effects, which substantially alter boundary layer behavior near the hub, must be considered in the design of thick inboard airfoils.

Research conducted at the Risø National Laboratory [14, 149] reinforces many of these findings. Their studies conclude that wind turbine airfoils must exhibit low sensitivity to leading edge contamination, which can be achieved by forcing early laminar to turbulent transition near the leading edge. For stall-regulated turbines, a high L/D ratio is required near the stall angle, and the design angle of attack should be selected close to $C_{L,max}$ in order to achieve the desired stall characteristics. Moreover, ensuring leading edge separation close to the design lift coefficient promotes soft-stall behavior, which helps maintain stable turbine performance at high wind speeds. Noise reduction is another important design consideration. A thin trailing edge minimizes vortex-shedding noise, which is increasingly important for large turbines operating near populated areas. Thick airfoils used in the inboard region must also satisfy structural requirements by providing sufficient stiffness and strength, while maintaining high $C_{L,max}$ to reduce blade solidity. Because airfoil performance is highly dependent on the Reynolds number, selecting an appropriate design Reynolds number is a crucial part of the airfoil design process. Finally, designers require geometric compatibility between thick and thin airfoils to ensure smooth

aerodynamic and structural transitions along the blade span.

B

B.2.4 FLATBACK AIRFOILS

Today, the wind energy industry is expanding rapidly due to rising global energy demand, environmental considerations, and the increasing cost of fossil fuels. Over the past three decades, wind-turbine size has grown dramatically. Designing such large rotors presents significant challenges, as modern blades must remain lightweight, cost-effective, and structurally reliable while delivering high aerodynamic performance.

In 2002, the Wind Energy Technology Department of Sandia National Laboratories (SNL) launched an extensive research initiative known as the Blade System Design Study (BSDS) [156]. The primary aim of the BSDS program was to explore advanced design and manufacturing approaches for next generation large-scale turbine blades with significantly higher power production capabilities. The BSDS consisted of three major components:

- Parametric Study for Large Wind Turbine Blades [4]
- Cost Study for Large Wind Turbine Blades [16]
- Innovative Design Approaches for Large Wind Turbine Blades [26]

This multidimensional approach simultaneously considered manufacturing methods, structural design requirements, and aerodynamic performance. One of the most significant outcomes of the BSDS investigations was the identification of increased thickness in the inboard blade region as a key strategy for enhancing structural efficiency and reducing blade mass. These two factors, i.e., structural stiffness and reduced weight, are critical for the feasibility of the next generation of large wind turbine blades. As a direct consequence, the use of flatback or blunt trailing edge airfoils in the inboard up to 40% of the blade span was recognized as an effective solution with multiple structural and aerodynamic advantages.

Flatback airfoils are produced by adding thickness to the aft portion of a conventional sharp trailing edge airfoil while preserving the original camber line. The resulting airfoil therefore has the same mean camber as its base airfoil but incorporates a finite trailing edge gap. Figure B.1 (a) illustrates this difference through a comparison of the sharp trailing edge and flatback variants of the Delft DU97-W-300 airfoil. It is important to distinguish flatback airfoils from truncated airfoils, in which the trailing edge is simply cut off, resulting in a modified camber line and degraded aerodynamic performance relative to the base airfoil [38]. An example of a truncated airfoil is shown in figure B.1 (b).

As discussed earlier, compared to modern thick wind turbine airfoil families with sharp trailing edges, such as those developed at Risø [14] and Delft [19, 157], flatback airfoils offer several notable aerodynamic and structural performance benefits.

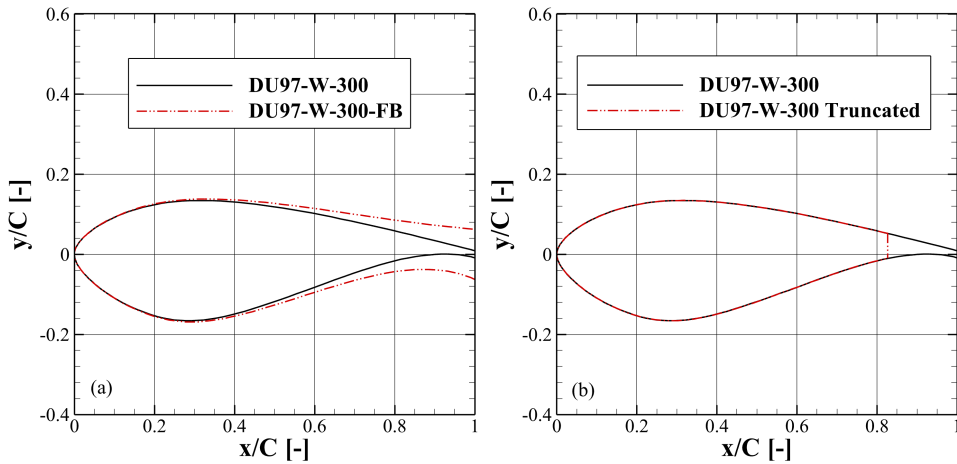


Figure B.1: Comparison between sharp trailing edge, flatback, and truncated airfoil sections.

From an aerodynamic perspective, the reduction in adverse pressure gradient on the suction side of a flatback airfoil, which caused by its thick trailing edge, leads to higher lift coefficients and steeper lift curve slopes than those of sharp trailing edge airfoils of equal maximum thickness. Figure B.2 shows wind tunnel lift coefficient measurements at a Reynolds number of $Re = 333 \times 10^3$ for the sharp trailing edge FB-3500-0050 airfoil and its flatback version, FB-3500-1750 [35]. Furthermore, the lift coefficient of flatback airfoils exhibits lower sensitivity to leading edge soiling, a highly desirable characteristic in wind

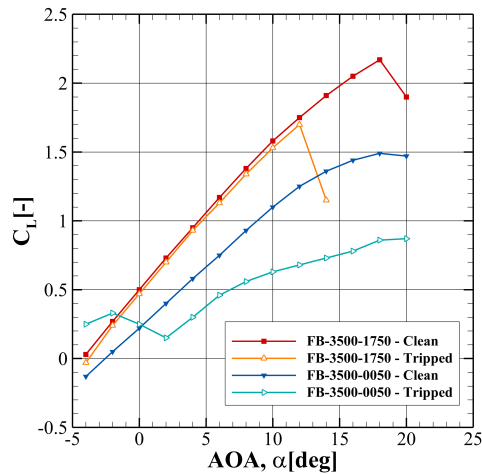


Figure B.2: Lift Coefficient comparison for flatback and sharp trailing edge airfoils.

energy applications where roughness contamination is unavoidable.

From a structural point of view, increasing the trailing edge thickness significantly enlarges the cross-sectional area and sectional moment of inertia of the airfoil for the same overall thickness. This improvement allows designers to use less glass or carbon fiber reinforcement in the blade structure while still meeting stiffness and strength requirements. Consequently, blades incorporating flatback airfoil sections become lighter, enabling smaller root chords and reducing material consumption. This not only decreases production cost but also simplifies transportation and installation. According to the BSDS program [4, 16], blade weight typically scales with the cube of blade length, making material efficiency critical because materials account for a substantial portion of total blade cost.

Despite these advantages, flatback airfoils also have certain drawbacks. Their blunt trailing edges generate higher base drag and increased aerodynamic noise due to vortex shedding [158, 159]. Thus, the use of flatback airfoils requires a careful balance between structural benefits and the associated aerodynamic penalties, especially for applications where noise regulations or wake performance considerations are critical.

B.2.5 REVIEW OF PREVIOUS RESEARCH ON FLATBACK AIRFOILS

Systematic investigations of airfoils with blunt trailing edges date back to the early 1950s and 1960s, when Hoerner conducted pioneering experimental work on the aerodynamic behavior of such airfoils [160, 161]. His results demonstrated that the maximum L/D ratio of thick airfoils could be increased by introducing a blunt trailing edge. However, many early studies employed truncated airfoils. Although this approach produces a blunt trailing edge geometry, it also compromises the original camber distribution, causing a noticeable degradation in overall aerodynamic performance [26].

Baker, van Dam, and Gilbert [35] later performed one of the most comprehensive experimental studies on flatback airfoils at the University of California, Davis Aeronautical Wind Tunnel. Their work focused on the FB-xxxx-yyyy airfoil series, where the first four digits denote the maximum thickness of the airfoil and the latter four digits specify the trailing edge thickness. These FB airfoils were originally developed during Phase I of the BSDS program [4]. The design concept involved combining the low pressure side of thick, high-lift inboard NREL airfoils with the high pressure side of the LS-1 series, which offers favorable structural characteristics. The flatback configuration was created by adding material symmetrically to the aft portion of the FB-3500 airfoil while preserving the original camber line.

Three FB airfoils, with trailing edge thicknesses of 0.5%, 8.75%, and 17.5% of chord, were tested in wind tunnel experiments at Reynolds numbers of $Re = 333 \times 10^3$ and $Re = 666 \times 10^3$. These airfoils are shown in figure B.3. To simulate the effects of leading edge contamination, each airfoil was tested under both free and fixed boundary layer transition conditions.

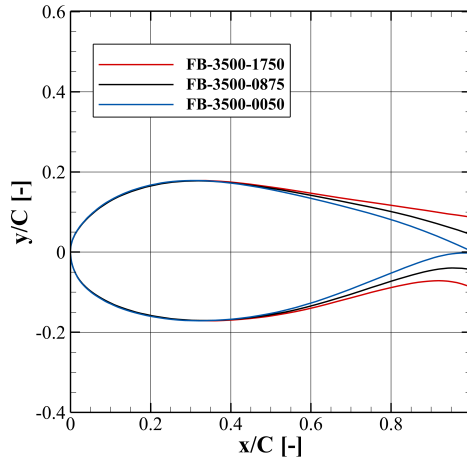


Figure B.3: Various sections of FB-3500-XXXX airfoil family.

The results (see figure B.2) demonstrated that the sharp trailing edge variant (0.5% trailing edge thickness) exhibited a significant reduction in lift under roughness conditions. As the trailing edge thickness increased, the sensitivity of the lift coefficient to leading edge roughness decreased markedly, causing the clean and contaminated lift curves to become more similar. However, the introduction of a blunt trailing edge also increased the base drag due to the formation of a low pressure unsteady wake behind the airfoil. To reduce the wake-induced drag, six different trailing edge modifications were tested at $Re = 333 \times 10^3$, aimed at diminishing bluff-body vortex shedding.

A CFD investigation of rotational effects on blades equipped with flatback airfoils was later conducted by Chao and van Dam [162]. Using the OVERFLOW CFD solver, they simulated the NREL Phase VI rotor, a configuration for which extensive wind tunnel measurement data are available, enabling thorough solver validation prior to applying flatback geometries. Their results indicated that improving rotor performance with flatback airfoils requires redesigning the twist distribution, airfoil selection along the blade span, and blending between sections. The study reaffirmed that thick, flatback airfoils provide a critical link between aerodynamic performance and structural requirements for next generation large wind turbine rotors.

To mitigate the base drag penalty of flatback airfoils, several geometric modification strategies, such as trailing edge splitter plates, serrations, base cavities, and wedges, have been proposed and summarized by van Dam and Kahn [163]. Their findings showed that significant drag reductions can be achieved using these devices, and that wind tunnel testing would allow a detailed assessment of such concepts for thick inboard airfoils. A more extensive experimental and numerical study by Baker and van Dam [164] evaluated splitter plates, base cavities, and offset cavities on the FB-3500-1750 airfoil. Their results

indicated that splitter plates could reduce drag by up to 50%, while base cavities achieved reductions of approximately 25%, though at the cost of decreasing lift. Offset cavities offered drag reductions comparable to splitter plates but with the added advantage of suppressing unsteady vortex shedding prior to stall.

Since earlier wind tunnel investigations were conducted mostly at low or moderate Reynolds numbers, Berg and Barone [158] extended this research to higher Reynolds numbers between $Re = 1.6 \times 10^6$ and $Re = 3.2 \times 10^6$. Their work, carried out at the Virginia Polytechnic Institute's wind tunnel on behalf of Sandia National Laboratories, examined both aerodynamic and aeroacoustic behavior. A flatback version of the DU97-W-300 airfoil with a 10% trailing edge thickness was tested, with additional experiments using a simple splitter plate. Their results showed increases in lift curve slope and maximum lift coefficient at high Reynolds numbers. Furthermore, the drag penalty associated with the blunt trailing edge could be reduced by up to 50% using a splitter plate. As expected, flatback airfoils generated more noise than sharp trailing edge variants, but the addition of a splitter plate significantly reduced acoustic emissions. The study also revealed that flatback airfoils exhibit larger nose-down pitching moments and that their lift is relatively insensitive to leading edge roughness; moreover, the splitter plate effectively weakened the vortex shedding tones.

A CFD study of aerodynamic and limited aeroacoustic behavior of flatback airfoils was later performed by Stone et al. [165]. Using a flatback version of the DU97-W-300 airfoil with a 10% trailing edge gap, chosen due to the availability of experimental data from the Virginia Tech wind tunnel campaign [166], they evaluated a wide range of turbulence models, including SA, $k-\omega$, and DES, across several CFD solvers. The results demonstrated that mean lift predictions were relatively insensitive to the choice of grid and turbulence model, while drag predictions depended significantly on both. RANS models produced the most accurate drag predictions, outperforming DES. Aeroacoustic simulations matched experimental data reasonably well, although the predicted Strouhal number of trailing edge vortex shedding was 10 – 15% lower than measured.

Finally, Paquette, van Dam, and Hughes [167] conducted an experimental structural study on blades equipped with flatback airfoils. This work, initiated by Sandia National Laboratories, was carried out at the National Wind Technology Center and involved full-scale structural testing under extreme flapwise loading conditions. Three 9 m blades designed for a 100 kW stall-regulated turbine were tested:

- **CX-100 (Carbon Experimental):** equipped with a carbon-fiber spar cap and a geometry combining the ERS-1006 (outboard) and NW-100 (inboard) blade designs;
- **TX-100 (Twist-Bend Experimental):** geometrically identical to CX-100 but incorporating a different laminate design, featuring fiberglass spar caps and carbon reinforced skins;
- **BSDS blade:** incorporating flatback airfoils in the inboard region and equipped with a carbon fiber spar cap.

The results, summarized in table B.1, showed that the BSDS blade was significantly lighter and capable of withstanding substantially higher loads, demonstrating the structural advantages of flatback airfoils for large wind turbine blades.

Table B.1: Summary of results of 9 m blade tests.

Property	CX-100	TX-100	BSDS
Weight (lb)	383	361	289
% of Design Load at Failure	115%	197%	310%
Root Failure Moment (kN-m)	128.6	121.4	203.9
Max. Carbon Tensile Strain at Failure (%)	0.31%	0.59%	0.81%
Max. Carbon Compressive Strain at Failure (%)	0.30%	0.73%	0.87%
Maximum Tip Displacement (m)	1.05	1.80	2.79

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B.2.6 ROTATIONAL EFFECTS

Horizontal-axis wind turbines operate in a highly complex and continuously evolving aerodynamic environment. Throughout their lifetime, turbine blades are exposed to a wide range of unsteady flow phenomena including inflow turbulence, tower–blade interaction, flow separation, dynamic stall, and, critically, rotational effects. Accurate prediction of these phenomena is essential for the design and optimization of major wind turbine components such as blades, the gearbox, the rotor, and the tower. Among the various aerodynamic mechanisms, rotational effects play one of the most significant roles in both the aerodynamic and structural behavior of wind turbine blades, particularly in the inboard regions where the influence of rotation is strongest. In modern turbine design practice, the BEM theory remains the most widespread tool for estimating aerodynamic loads. BEM relies on 2D steady airfoil data to predict lift, drag, and pitching moment distributions along the blade span. Although BEM performs well under attached flow conditions, its accuracy deteriorates significantly in deep stall or separated flow regimes. Under such conditions, BEM typically underpredicts blade loads, leading to discrepancies between predictions and full-scale measurements [168, 169]. This underprediction is closely tied to the complex three-dimensionality of the flow around a rotating blade, which cannot be captured by purely 2D airfoil data.

Experimental observations have shown that the boundary layer of a rotating blade contains a radial flow component in stalled conditions. This spanwise movement of fluid significantly alters the aerodynamic characteristics of blade sections, particularly in the inboard region where rotation dominates [170]. The presence of radial flow modifies the effective pressure distribution and boundary layer properties, leading to a delay in stall onset and an increase in lift relative to non-rotating conditions. Although the full physics of rotational effects are not yet completely understood, the phenomenon—commonly

referred to as stall delay, is widely attributed to the combined influence of centrifugal forces and Coriolis forces. The centrifugal force drives the boundary layer fluid outward along the span, thinning the boundary layer and reducing its susceptibility to separation. Simultaneously, the Coriolis force acts on this radial flow, pushing it toward the trailing edge and generating a favorable pressure gradient. Together, these forces stabilize the boundary layer and delay flow separation, resulting in elevated lift coefficients on rotating blades.

The earliest systematic investigation of rotational effects was conducted by Himmelkamp in the 1940s [171], who measured the lift distribution along propeller blades and observed significantly increased lift near the root region under rotation. Subsequent experiments reinforced these findings. McCroskey (1971) [172] identified radial flow in separated flow regions, while Millborrow and Ross [173] reported enhanced lift on model rotors tested in wind tunnels. Flow visualization studies on full-scale turbines by vanes Savino and Nyland [174] showed chordwise flow under attached conditions and strong radial flow during stall. Measurements by Ronsten [175] further demonstrated clear differences in pressure distribution and aerodynamic loads between rotating and non-rotating blades. These trends were later confirmed through extensive field tests by Bruining et al. [176], Butterfield et al. [177], and Madsen and Christensen [178]. The significance of 3D stall delay mechanisms was further validated through the IEA Wind R&D Program (1991–1997) [179–182], a major international collaborative effort involving ECN, NREL, Risø, CRES, and others. Among all full-scale experiments to date, the most comprehensive and widely referenced are those conducted by NREL at the NASA Ames 24.4×36.6 m wind tunnel [44, 183–186]. These tests, performed around the year 2000, included multiple turbine configurations and produced approximately 2,200 high quality data sets. The results have been extensively analyzed in the literature and remain a benchmark for validating CFD models of rotating wind turbine blades [187–192].

Analytical investigations of rotational aerodynamics began in the 1950s with Sears [193], who examined potential flow behavior around an infinite rotating cylinder. Follow up research by Fogarty and Sears expanded on these concepts using potential flow solutions for rotating blades. Fogarty [194] also studied laminar boundary layers over rotating surfaces and observed minimal differences between rotating and non-rotating cases under attached flow, while predicting more substantial differences under separation. Additional theoretical work by Banks and Gadd [195] suggested that rotation delays laminar boundary layer separation, especially near the inboard sections of blades. Further contributions came from McCroskey and Yaggy [196] in their analysis of helicopter rotor blades, concluding that rotational crossflow exerts beneficial aerodynamic effects. Later numerical studies by McCroskey and Dwyer [197] identified that Coriolis forces dominate crossflow behavior in inboard blade regions, whereas centrifugal forces become more influential under strong adverse pressure gradients. In another study, McCroskey [172] reported negligible influence of rotation on the transition or separation location under attached flow, but noted considerable radial flow in separated regimes.

With the rise of computational power, numerical simulations became an essential tool for studying rotational effects. In 1986, Sørensen [198] employed a 3D viscous–inviscid interaction method and found that rotation produces substantial radial flow within the trailing edge separation bubble, resulting in large discrepancies between 2D and 3D lift predictions. He also confirmed that the separation line location remained largely unchanged between 2D and 3D simulations. Wood [199] later used a 3D panel method to analyze stall delay, concluding that the effect is strongly linked to local rotor solidity. Narramore and Vermeland [200] advanced the field further in 1992 by using a full Navier–Stokes solver with an algebraic turbulence model to simulate helicopter blades. Their results confirmed that rotation delays separation, particularly near the root. In parallel, Snel et al. [201] developed a quasi-3D model for rotating flow. Early publications by Snel and Houwink [202] and comparisons with measurements [168] showed higher lift and increased lift curve slope due to rotation, with the local chord-to-radius ratio (C/r) identified as a key parameter. The quasi-3D model was later extended [203] and incorporated into the XFOIL code, resulting in the well known RFOIL solver. These predictions were later validated by Sørensen [204] and additional studies by Shen and Sørensen [205] using quasi-3D Navier–Stokes models. Further contributions came from Hansen, Michelsen, and Sørensen [206], who used a 3D Navier–Stokes approach to simulate stalled wind turbine blades, again observing increased lift at the inboard regions. Chaviaropoulos and Hansen [207] employed a quasi-3D Navier–Stokes solver in 2000, identifying chord-to-radius ratio as a dominant factor influencing lift. They also proposed a semi-empirical correction model for load coefficients. Using a 3D integral boundary layer solver, Du and Selig [208] concluded that stall delay is driven primarily by Coriolis effects, with the magnitude decreasing toward the blade tip. These findings were corroborated by Sørensen, Michelsen, and Schreck [209], who conducted detailed simulations of the NREL Phase IV rotor. Later work by Martínez, Sørensen, and Shen [210] applied similarity variable techniques to 3D boundary layer equations and again confirmed delayed separation and increased boundary layer stability under rotation. A number of researchers, including Snel et al. [168], Du and Selig [211], Chaviaropoulos and Hansen [207], Lindenburg [212], and Bak et al. [213], have developed 3D correction models that adapt 2D airfoil data for use in rotating blade simulations. These models continue to be used widely in BEM based design tools and remain essential for practical wind turbine analysis.

B.2.7 OPTIMIZATION

Designers continuously seek to improve their systems in order to enhance performance, reduce production costs, and increase competitiveness in the global market. Such improvements may stem from more efficient manufacturing processes or from refined product designs that yield superior functional characteristics. In many engineering applications,

even small modifications to a complex system can produce substantial improvements. For example, a minor change in the leading edge geometry of a wind turbine airfoil can significantly alter its sensitivity to leading edge contamination. Historically, improvements in engineering design were achieved primarily through trial and error approaches, which were often time consuming, labor intensive, and lacking in systematic rigor. With increasing system complexity, these traditional methods became insufficient. Today, optimization techniques play a central role in the development of high performance systems. In general terms, optimization involves maximizing or minimizing an objective function that quantifies system performance. The objective function may be linear or nonlinear, and the optimization problem may involve single or multiple objectives.

In aerodynamic applications, typical objective functions include the lift coefficient, drag coefficient, or the L/D ratio. Prior to the emergence of modern optimization methods, aerodynamic design was a demanding process that required the construction and testing of numerous wind tunnel models. This approach was costly and impractical, especially when many design variables were involved. With the advent of CFD, the aerodynamic design process became significantly more efficient and cost-effective. Designers could now evaluate numerous geometries rapidly, using wind tunnel experiments primarily for validation of the final optimized shape. However, even CFD based iterative design remains a form of trial-and-error and does not inherently guarantee an optimal solution. The development of mathematical optimization methods in recent decades has therefore opened new possibilities. Today, the combination of CFD and advanced optimization algorithms enables aerodynamic designers to identify high performance configurations far more efficiently than with traditional techniques. Beightler [214] outlined three fundamental steps for optimizing a system:

- Identification of the system and all relevant design variables
- Establishment of an objective function that quantifies system performance and determination of how each variable influences it
- Determination of the optimal set of design variables that yields the desired improvement

In the first step, the designer must thoroughly understand the physical mechanisms governing the system, including the relationships among design variables. In the second step, the designer must define the criteria that characterize an optimized system, and understand the interactions between performance metrics. For instance, in airfoil optimization, increasing the lift coefficient typically increases the pitching moment coefficient. In the third step, an optimization algorithm is used to adjust the design variables until the objective function is optimized. The choice of optimization algorithm depends on the nature of the system, such as linearity, the number of design variables, the complexity of the objective function, constraints, and whether the optimization is single- or multi-objective.

MATHEMATICAL FORMULATION OF AN OPTIMIZATION PROBLEM

Optimization is a computational design process that identifies the values of design variables that maximize or minimize an objective function while satisfying constraints. Mathematically, a general constrained optimization problem can be expressed as:

$$\begin{aligned} &\text{Find } X \\ &\text{to minimize/maximize } F(X) \\ &\text{subject to } \begin{cases} H(X) = 0, \\ G(X) \leq 0, \\ A \leq X \leq B. \end{cases} \end{aligned} \quad (\text{B.1})$$

where X is the vector of design variables, i.e., $X = [x_1, x_2, \dots, x_n]$, $F(X)$ is the objective function, $H(X)$ and $G(X)$ denote equality and inequality constraints, respectively, and A and B are the lower and upper bounds of the design variables. If no constraints are present, the problem becomes an unconstrained optimization problem. An optimization problem thus consists of three main components:

Objective Function: The objective (or cost) function quantifies the performance of the system. The goal of the optimization process is to maximize or minimize this function. For aerodynamic optimization, common objectives include lift, drag, the lift-to-drag ratio, or weighted combinations of these characteristics.

Design Variables: Design variables are the adjustable parameters within a system that influence the objective function. A different value of the n -dimensional design variable vector results in a different system configuration and therefore a different objective function value. In airfoil optimization, design variables may be control points defining the airfoil shape. Depending on the problem, design variables may be continuous or discrete.

Design Constraints: Most optimization problems include constraints that must be satisfied during the design process. Aerodynamic optimization typically includes both geometric and aerodynamic constraints. For example, airfoil thickness or the location of maximum thickness may be fixed to meet structural requirements. Aerodynamic constraints may include maintaining a fixed lift coefficient while minimizing drag.

AERODYNAMIC DESIGN METHODS

Aerodynamic design approaches can be broadly divided into two categories: indirect (inverse) methods and direct optimization methods. Historically, both classes have played

important roles in airfoil and aerodynamic shape design, although modern practice relies predominantly on direct optimization techniques.

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INDIRECT (INVERSE) METHODS

Inverse design methods determine the aerodynamic shape by prescribing a desired pressure distribution around the body. These approaches were widely used during periods when designers had limited ability to control aerodynamic performance directly. Notable developments in inverse methods include the work of Mangler [215], Lighthill [216], and Selig and Maughmer [217]. Although historically important, inverse methods have largely been replaced by more flexible and powerful direct optimization strategies and are no longer routinely applied in modern aerodynamic design workflows.

DIRECT OPTIMIZATION METHODS

Direct optimization approaches evaluate the aerodynamic performance of a candidate geometry and modify the design iteratively to improve the chosen performance metric. These methods can be classified into two groups: gradient-based and non-gradient-based methods. Both have been successfully used in aerodynamic shape optimization, depending on the characteristics of the problem.

Gradient-Based Methods: Gradient-based approaches rely on computing the derivatives of the objective function with respect to the design variables. Consequently, the objective function must be continuously differentiable. Common gradient-based methods include the adjoint method, successive quadratic programming, and finite-difference sensitivity analysis. After the sensitivity information is computed, first-order or second-order optimization algorithms are used to determine improved designs. These approaches have been widely applied in aerodynamic optimization by Hicks et al. [218], Eyi and Lee [219], Jameson [220], Nadarajah [221], and others. Gradient-based techniques typically require relatively few design iterations to converge; however, they generally identify only local extrema and depend strongly on the initial design.

Non-Gradient-Based Methods: Non-gradient-based, or zeroth-order, methods do not require derivative information. Instead, they search the design space by comparing the objective function values for different sets of design variables. Representative methods include simplex approaches, genetic algorithms, modified genetic algorithms, downhill simplex strategies, simulated annealing, Darwinian search techniques, the Nelder–Mead method, and grid search procedures. A major advantage of these methods is their ability to

locate global extrema in complex design spaces. They also allow the use of both continuous and discrete design variables, and they remain effective even when the objective function exhibits discontinuous derivatives. The primary drawback is that they often require a large number of function evaluations, which can be computationally expensive. Despite this, non-gradient methods have been used successfully in aerodynamic optimization by Fuglsang and Bak [149, 154], Cardamone [222], Burger [223], and others.

B.2.8 MOTIVATION AND RESEARCH OBJECTIVES

Flatback airfoils offer several aerodynamic and structural advantages for large wind turbine blades; however, despite their potential, a number of critical aspects remain insufficiently understood. Previous studies [35, 158, 162, 164, 224] indicate several limitations that have hindered the systematic adoption of flatback airfoils in commercial wind turbine design. These challenges include:

- The limited availability of systematically designed flatback airfoil families
- The scarcity of high quality wind tunnel measurements, particularly at high Reynolds numbers relevant to multi-megawatt turbines
- Aerodynamic drawbacks such as elevated drag, increased nose-down pitching moment, and enhanced noise emission caused by blunt trailing edge vortex shedding
- The presence of unsteady flow phenomena in the trailing edge region
- Insufficient understanding of the influence of rotational 3D-effects on the flow over blunt trailing edges

These open issues illustrate the need for a more comprehensive investigation of flatback airfoil characteristics and highlight the necessity of developing new, optimized airfoil sections that address both aerodynamic and structural requirements.

The primary motivation of the present research is therefore to design and analyze optimized flatback airfoils with relative thicknesses between 35% and 50% and incorporating various trailing edge gaps. Unlike previous studies, which largely relied on modifying sharp trailing edge airfoils, the present work develops flatback airfoils through a multi-disciplinary design method, that simultaneously incorporates aerodynamic and structural considerations.

A further objective is to investigate how 3D-rotational effects influence the performance and optimization of flatback airfoils, particularly in the inboard region of large wind turbine blades where rotational effects are dominant. This thesis therefore introduces several novel elements that distinguish it from previous work, including:

- The multidisciplinary optimization of flatback airfoils considering multiple aerodynamic and structural design parameters simultaneously
- The development and application of a quasi-3D Navier-Stokes solver capable of capturing rotational effects at the inboard blade region
- The use of RANS equations as the aerodynamic solver within the optimization process
- The design and evaluation of airfoils in both 2D and quasi-3D computational environments
- The implementation of fuzzy logic techniques combined with non-gradient-based optimization algorithms
- A combined experimental and numerical investigation into the aerodynamic behavior of the optimized flatback airfoils

In total, six flatback airfoils were developed, both with and without rotational effects. To further investigate their aerodynamic behavior, a carbon fiber wind tunnel model of one optimized airfoil (35% relative thickness and 15% trailing edge gap) was manufactured. Wind tunnel experiments, including force measurements and time-resolved PIV analysis in the trailing edge region, are being conducted at TU Braunschweig. These results will be compared with high-fidelity CFD simulations using both RANS and DES turbulence modeling approaches.

The overarching goals of this thesis are:

1. To develop a new generation of structurally and aerodynamically optimized flatback airfoils suitable for modern multi-megawatt wind turbines, and
2. To advance the understanding of the aerodynamic mechanisms, particularly rotational effects, that govern the performance of thick, flatback airfoil sections.

B.3 METHODOLOGY AND APPROACH

B.3.1 INTRODUCTION

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This part presents the methodology and overall approach adopted in this work. It begins with the development of the quasi-3D flow solver, which is employed to simulate 3D-rotational effects relevant to the aerodynamic optimization process. The subsequent part addresses the design methodology for the new flatback airfoils. This includes a discussion of the desired performance characteristics of flatback airfoils, the optimization strategy, and the aerodynamic and structural design parameter evaluation procedures. Finally, the experimental setup used for the wind tunnel investigations is described in detail.

B.3.2 QUASI-3D FLOW

As discussed in Section B.2.6, rotational effects have a significant influence on the aerodynamic performance of airfoil sections in the inboard region of wind turbine blades. Accurate prediction and analysis of these phenomena are essential for the structural and aerodynamic design as well as the optimization of modern wind turbine blades. Although advances in computational power and CFD methodologies over the past decade have enabled the use of full three-dimensional Navier-Stokes simulations to resolve rotational effects on rotor blades, such simulations remain unsuitable for optimization purposes. The primary challenges include the high computational cost, the long simulation times, the need for generating meshes for large and complex domains, and the strong influence of root and tip flow structures on the overall flow domain. Consequently, there is a strong need for efficient quasi-3D methods for the aerodynamic design and optimization of inboard airfoil sections.

Several approaches have previously been explored to formulate quasi-3D models. Snel [201] and Snel and Houwink [202] applied order-of-magnitude analysis to the three-dimensional boundary layer equations, simplifying them into a two-dimensional system with an additional correction equation. Shen and Sørensen [205] performed a similar order-of-magnitude analysis on the spanwise derivatives of the full Navier-Stokes equations, expressed in streamfunction–vorticity form, to obtain a quasi-3D formulation. Chaviaropoulos and Hansen [207] integrated the Navier-Stokes equations along the radial direction to extract mean radial quantities and derive a simplified quasi-3D system.

However, in this project, a different approach is used to develop the system of equations for the quasi-3D model. The concept involves the imposition of rotational forces, with the corresponding angle of attack, Reynolds number, C/r ratio, and rotational speed, at each radial airfoil section of the rotor blade on a quasi-3D domain. This allows for the

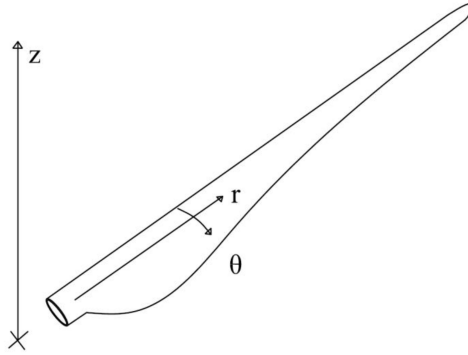


Figure B.4: The Coordinate system of the rotating blade.

balance and development of inertia, viscous, Coriolis, and centrifugal forces at that specific radius. Although this method is three-dimensional, the system of equations is solved for the boundary conditions of a particular radius, obviating the need for a high number of grids in the spanwise direction. Consequently, the simulation time is low and suitable for optimization purposes. Moreover, in contrast to previous quasi-3D models that have a 2D domain, the 3D-flow domain of the simulation enables better visualization and analysis of the flow pattern than previous 2D methods.

The incompressible Navier-Stokes equations in a rotating cylindrical reference frame attached to the blade (figure B.4) can be expressed as:

$$\rho \left(\frac{\partial v_\theta}{\partial t} + v_r \frac{\partial v_\theta}{\partial r} + v_\theta \frac{\partial v_\theta}{r \partial \theta} + v_z \frac{\partial v_\theta}{\partial z} + \frac{v_r v_\theta}{r} \right) = \rho F_\theta - \frac{\partial p}{r \partial \theta} + \mu \left(\frac{\partial^2 v_\theta}{\partial r^2} + \frac{\partial v_\theta}{r \partial r} + \frac{\partial^2 v_\theta}{r^2 \partial \theta^2} + \frac{\partial^2 v_\theta}{\partial z^2} \right) \quad (\text{B.2})$$

$$\rho \left(\frac{\partial v_r}{\partial t} + v_r \frac{\partial v_r}{\partial r} + v_\theta \frac{\partial v_r}{r \partial \theta} + v_z \frac{\partial v_r}{\partial z} - \frac{v_\theta^2}{r} \right) = \rho F_r - \frac{\partial p}{\partial r} + \mu \left(\frac{\partial^2 v_r}{\partial r^2} + \frac{\partial v_r}{r \partial r} + \frac{\partial^2 v_r}{r^2 \partial \theta^2} + \frac{\partial^2 v_r}{\partial z^2} \right) \quad (\text{B.3})$$

$$\rho \left(\frac{\partial v_z}{\partial t} + v_r \frac{\partial v_z}{\partial r} + v_\theta \frac{\partial v_z}{r \partial \theta} + v_z \frac{\partial v_z}{\partial z} \right) = \rho F_z - \frac{\partial p}{\partial z} + \mu \left(\frac{\partial^2 v_z}{\partial r^2} + \frac{\partial v_z}{r \partial r} + \frac{\partial^2 v_z}{r^2 \partial \theta^2} + \frac{\partial^2 v_z}{\partial z^2} \right) \quad (\text{B.4})$$

Where F_θ , F_r , and F_z are external force terms, and v_θ^2/r and $v_r v_\theta/r$ are coordinate curvature terms. The external forces acting in a rotating reference frame include gravitational, Coriolis, and centrifugal forces. The gravitational force is neglected, while the centrifugal force has a component only in the radial direction, given by $r\Omega^2$, where Ω is the rotational speed. The Coriolis force contains two components in the r and θ directions, namely $-2\Omega v_\theta$ and $2\Omega v_r$, respectively. Therefore, the external force terms can be written as follows:

$$F_\theta = 2\Omega v_r \quad (\text{B.5})$$

$$F_r = r\Omega^2 - 2\Omega v_\theta \quad (\text{B.6})$$

$$F_z = 0 \quad (\text{B.7})$$

Thus, the system of the equations can be written as:

$$\rho \left(\frac{\partial v_\theta}{\partial t} + v_r \frac{\partial v_\theta}{\partial r} + v_\theta \frac{\partial v_\theta}{r \partial \theta} + v_z \frac{\partial v_\theta}{\partial z} \right) = -\frac{v_r v_\theta}{r} + \rho 2\Omega v_r - \frac{\partial p}{r \partial \theta} + \mu \left(\frac{\partial^2 v_\theta}{\partial r^2} + \frac{\partial v_\theta}{r \partial r} + \frac{\partial^2 v_\theta}{r^2 \partial \theta^2} + \frac{\partial^2 v_\theta}{\partial z^2} \right) \quad (\text{B.8})$$

$$\rho \left(\frac{\partial v_r}{\partial t} + v_r \frac{\partial v_r}{\partial r} + v_\theta \frac{\partial v_r}{r \partial \theta} + v_z \frac{\partial v_r}{\partial z} \right) = \frac{v_\theta^2}{r} + \rho(r\Omega^2 - 2\Omega v_\theta) - \frac{\partial p}{\partial r} + \mu \left(\frac{\partial^2 v_r}{\partial r^2} + \frac{\partial v_r}{r \partial r} + \frac{\partial^2 v_r}{r^2 \partial \theta^2} + \frac{\partial^2 v_r}{\partial z^2} \right) \quad (\text{B.9})$$

$$\rho \left(\frac{\partial v_z}{\partial t} + v_r \frac{\partial v_z}{\partial r} + v_\theta \frac{\partial v_z}{r \partial \theta} + v_z \frac{\partial v_z}{\partial z} \right) = -\frac{\partial p}{\partial z} + \mu \left(\frac{\partial^2 v_z}{\partial r^2} + \frac{\partial v_z}{r \partial r} + \frac{\partial^2 v_z}{r^2 \partial \theta^2} + \frac{\partial^2 v_z}{\partial z^2} \right) \quad (\text{B.10})$$

The equations are solved at a constant radial position. It is assumed that the surface curvature in the chordwise direction can be neglected relative to the boundary layer thickness. Consequently, it is possible to substitute $ds = r d\theta$. As a result, the system of equations can be rephrased as follows:

$$\rho \left(\frac{\partial v_s}{\partial t} + v_r \frac{\partial v_s}{\partial r} + v_s \frac{\partial v_s}{\partial s} + v_z \frac{\partial v_s}{\partial z} \right) = \frac{v_r v_s}{r} + \rho 2\Omega v_r - \frac{\partial p}{\partial s} + \mu \left(\frac{\partial^2 v_s}{\partial r^2} + \frac{\partial^2 v_s}{\partial s^2} + \frac{\partial^2 v_s}{\partial z^2} + \frac{\partial v_s}{r \partial r} \right) \quad (\text{B.11})$$

$$\rho \left(\frac{\partial v_r}{\partial t} + v_r \frac{\partial v_r}{\partial r} + v_s \frac{\partial v_r}{\partial s} + v_z \frac{\partial v_r}{\partial z} \right) = \frac{v_s^2}{r} + \rho(r\Omega^2 - 2\Omega v_s) - \frac{\partial p}{\partial r} + \mu \left(\frac{\partial^2 v_r}{\partial r^2} + \frac{\partial^2 v_r}{\partial s^2} + \frac{\partial^2 v_r}{\partial z^2} + \frac{\partial v_r}{r \partial r} \right) \quad (\text{B.12})$$

$$\rho \left(\frac{\partial v_z}{\partial t} + v_r \frac{\partial v_z}{\partial r} + v_s \frac{\partial v_z}{\partial s} + v_z \frac{\partial v_z}{\partial z} \right) = -\frac{\partial p}{\partial z} + \mu \left(\frac{\partial^2 v_z}{\partial r^2} + \frac{\partial^2 v_z}{\partial s^2} + \frac{\partial^2 v_z}{\partial z^2} + \frac{\partial v_z}{r \partial r} \right) \quad (\text{B.13})$$

It is evident that these systems of equations contain additional terms related to coordinate curvature, centrifugal, and Coriolis forces. These supplementary terms were incorporated into the equation system using UDF programming in the Fluent code.

VALIDATION OF THE QUASI-3D FLOW SOLVER

To validate the quasi-3D flow solver, the Unsteady Aerodynamics Experiment (UAE) Phase VI was selected. This experiment, conducted by NREL in the NASA Ames Research Center wind tunnel at Moffett Field, California, is considered one of the most accurate and comprehensive aerodynamic measurement campaigns for horizontal-axis wind turbines [44]. The primary objective of UAE Phase VI was to provide high quality data describing the complex three-dimensional aerodynamic behaviour of wind turbines under controlled inflow conditions, in contrast to earlier field measurements where atmospheric turbulence and wind shear introduced significant uncertainty.

The tests were performed in the 24.4×36.6 m open-loop test section of the National Full-Scale Aerodynamics Complex at NASA Ames. This section, added in 1987 as an extension to the original closed-loop facility built in 1944, is equipped with six variable-pitch, 15-bladed fans driven by 16.8 MW motors, capable of generating wind speeds up to 50 m/s. UAE Phase VI was carried out on a Grumman Windstream 33 wind turbine, an upwind, two-bladed, 10 m diameter, stall-regulated machine with independent pitch control for each blade. The rotor operated at a nominal speed of 71.6 rpm. The turbine was mounted on a 12.2 m tubular tower positioned at the center of the test section and installed on a seven component balance system to measure forces and moments acting on the low-speed shaft. Wind tunnel inflow conditions were monitored using pitot-static probes and ultrasonic anemometers [44]. One blade was fully instrumented for aerodynamic data-acquisition.

The S809 airfoil, developed by Airfoils, Inc., was used along the entire blade span. This airfoil is well documented, and extensive wind tunnel measurements are available [225]. Each instrumented section contained 22 pressure taps, with higher density near the leading edge to resolve strong pressure gradients. Pressure was measured at five radial stations located at 30%, 47%, 63%, 80%, and 95% of the blade radius [44]. The blades were both twisted and tapered: the chord decreased linearly from 0.737 m at 25% span to 0.356 m at the tip, while the twist varied nonlinearly from 22.1° at 25% radius to 0° at the tip. The region between 25% and 100% span consisted of a uniform S809 cross-section, whereas the inner region (25%-12% span) transitioned toward the pitch shaft. Further details on blade construction and instrumentation are provided in the UAE documentation [44, 185]. Note that the incoming flow angles were measured near each pressure tap distribution using five-hole probes [226]. These probes were mounted at 34%, 51%, 67%, 84%, and 91% of the blade radius and extended to 80% of the chord upstream of the leading edge. The probe tips were bent downward by 20° to improve measurement accuracy. The Local Inflow Angle (LFA) and the Spanwise Inflow Angle (SFA) were obtained using these probes.

The comparison between the experimental measurements and the numerical quasi-3D simulations of the NREL Phase VI blade for the normal force coefficient at Reynolds numbers of $Re = 0.65 \times 10^6$, $Re = 0.75 \times 10^6$, and $Re = 0.81 \times 10^6$ at the 30%, 47%, and 63% radial positions is presented in figures B.5 and B.6 respectively. Note that the mesh configuration used for the quasi-3D simulations, as described in Section B.3.3.4, was applied only after achieving mesh-independent results. The results show that the numerically predicted

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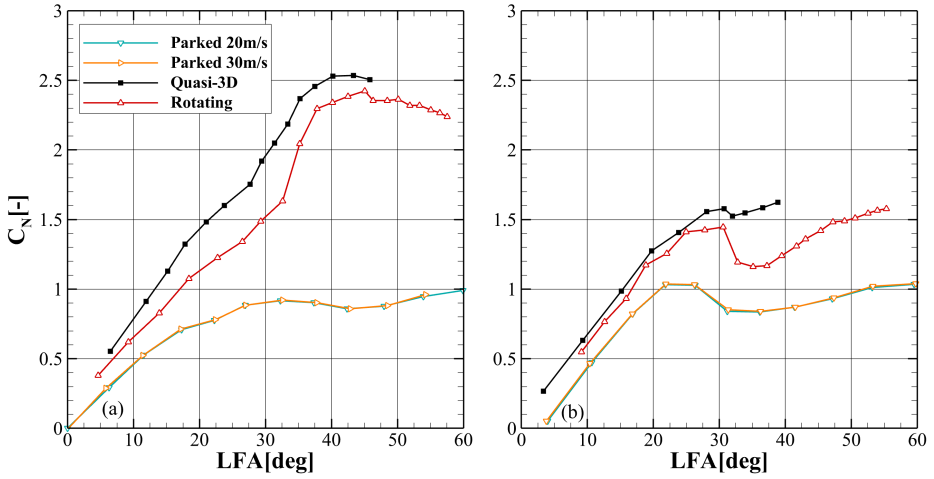


Figure B.5: Normal force coefficient polars for rotating, non-rotating and quasi-3D simulations, [(a): $r/R = 30\%$, (b): $r/R = 47\%$].

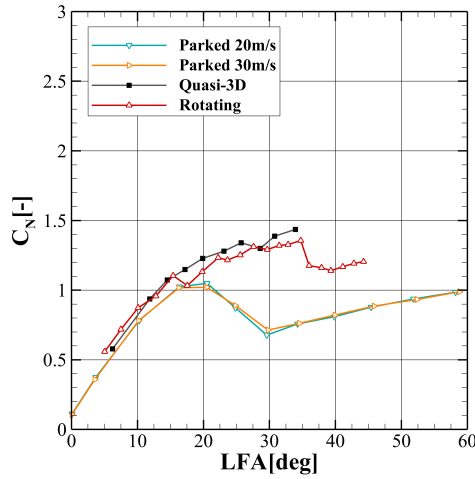


Figure B.6: Normal force coefficient polars for rotating, non-rotating and quasi-3D simulations, [$r/R = 63\%$].

normal force coefficient at the 30% radial position is consistently higher than the experimentally measured values. In contrast, for the 47% and 63% radial locations, the quasi-3D predictions remain within approximately 6% of the experimental data, demonstrating considerably better agreement. Although the numerical model overpredicts the normal

force coefficient at the 30% radius, the simulations reproduce the same characteristic trend with increasing LFA, namely, three distinct slopes corresponding to different aerodynamic regimes as the inflow angle evolves.

The discrepancy observed at the 30% radius can be attributed primarily to the presence of vortical structures originating from the blade root. As discussed in [227], the 30% radial station lies immediately downstream of the geometric transition region between 12% and 25% blade radius (see [226]), and it is situated near the maximum chord location. Consequently, root-induced vortices have a stronger influence at this blade position, modifying the local pressure distribution and resulting in deviations between numerical and wind tunnel data. This behaviour is also reflected in the reduced slope of the normal force coefficient in the linear region at low LFA for both rotating and parked conditions. In contrast, the 47% and 63% radial stations are less affected by root-generated flow distortions and therefore exhibit higher consistency between measurements and quasi-3D predictions. It should be noted that a wide range of physical parameters, such as root vortices, spanwise variations in twist and chord, and local Reynolds number effects, simultaneously influence the aerodynamics of a real blade. However, such three-dimensional and radial dependent effects are inherently absent in quasi-3D CFD simulations, which solve the governing equations at a constant radius without spanwise variation. Consequently, some level of disagreement between numerical predictions and wind tunnel measurements is unavoidable.

To gain a more detailed understanding of the flow behaviour at individual blade sections, surface streamline distributions and radial flow contour plots obtained from quasi-3D simulations of the S809 airfoil at the 47% radial position and at an angle of attack of 12° are shown in figure B.7. Note that the lateral boundaries of the computational domain were

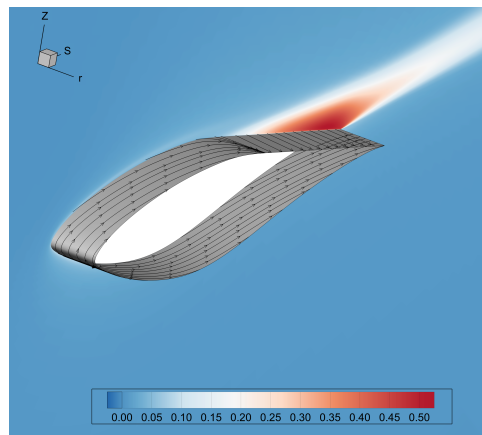


Figure B.7: Surface streamlines and v_r/V contour [$Re = 0.75 \times 10^6$, $\alpha = 12^\circ$, $r/R = 47\%$, $C/r = 0.232$].

treated using periodic boundary conditions. The streamline patterns reveal a pronounced radial deflection in the separated flow region on the suction side of the airfoil, driven by the combined influence of Coriolis and centrifugal forces and by the reduced streamwise momentum typical of rotating blade aerodynamics. These observations are consistent with findings reported in earlier studies.

B.3.3 DESIGN METHOD

DESIRABLE FLATBACK AIRFOIL CHARACTERISTICS

As discussed in Section B.2.3, the desirable characteristics of wind turbine airfoils can be classified into structural and aerodynamic properties. Moreover, the relevance of these properties depends strongly on the blade's spanwise location. In the root region, structural considerations dominate the design process, whereas at the mid-span and tip regions, aerodynamic performance becomes increasingly important. Because flatback airfoils possess inherently large thicknesses, they are typically employed in the inboard portion of the blade; therefore, the discussion below focuses on desirable characteristics of flatback airfoils intended for root region applications.

From a structural point of view, the sectional moment of inertia, the position of maximum thickness, and the compatibility with the thinner outboard airfoil family represent the most influential parameters [14]. A high sectional moment of inertia is highly desirable in the inboard region of large wind turbine blades due to the substantial bending moments that develop near the blade root. Increasing the sectional moment of inertia contributes directly to improved stiffness and can simultaneously reduce both blade weight and solidity by enabling the use of less material and, in some cases, shorter chord lengths. These structural advantages are particularly valuable for the new generation of very large blades, where transportation and manufacturability introduce significant constraints.

The location of the maximum thickness is also an essential design parameter, both from the standpoint of aerodynamic stability [141] and from the perspective of ensuring compatibility within the overall airfoil family. A blade designed with a compatible sequence of airfoil sections allows for smoother geometric transitions, reduces manufacturing complexity, and helps minimize production costs. Additionally, the position of maximum thickness plays a critical role in the placement of the internal spar, which is fundamental for achieving the required structural integrity of the blade. This placement of the maximum thickness ensures that the newly optimized airfoils remain consistent with modern thin airfoil designs, such as the Risø-B1 series or the DU9X-W airfoil families.

Aerodynamically, wind turbine airfoils are expected to demonstrate several key characteristics, including a high maximum lift coefficient [19], a large lift-to-drag ratio [14, 19], insensitivity of $C_{L,max}$ to leading edge roughness [14, 19, 141], a low zero-lift pitching

Table B.2: Desirable characteristics of flatback airfoils and their relative importance.

Structural	
High sectional moment of inertia	xxxxx
Geometrical compatibility with the outboard thin airfoils	xxx
Aerodynamical	
High maximum lift coefficient ($C_{L,max}$)	xxxxx
Insensitivity of $C_{L,max}$ to the leading edge roughness	xx
High lift-to-drag ratio (L/D)	xx
Low pitching moment coefficient	x

B

moment coefficient (C_{M0}) [141], and reduced aeroacoustic noise emissions [152]. Among these, the most important aerodynamic requirement for inboard thick airfoils is a high maximum lift coefficient. In contrast to the outboard airfoils, high lift/drag ratio is not an important design parameter for the inboard thick airfoil sections [141]. However, it gets more important as the flatback airfoils become thinner and move towards the middle of the blade. With a higher $C_{L,max}$, these thick airfoils are capable of generating greater torque at lower wind speeds and with shorter lever arms.

The sensitivity of $C_{L,max}$ to surface roughness, particularly that forming on the leading edge due to accumulated ice, dust, dirt, insects, or corrosion, is a well established challenge in airfoil design. Such roughness can rapidly reduce both $C_{L,max}$ and the stall angle of attack, ultimately leading to aerodynamic performance degradation as well as increased noise emission. To mitigate these adverse effects, the insensitivity of $C_{L,max}$ to leading edge roughness was pursued through two primary design objectives. First, the natural transition point on the suction side was shifted as close as possible to the leading edge at approximately three degrees below the stall angle of attack. Second, a high $C_{L,max}$ under roughness-induced conditions was targeted [14, 228]. Although this characteristic is generally less critical for thin outboard airfoils, it remains relevant and should be incorporated into the optimization process to support higher annual energy production. Additional aerodynamic parameters, such as the pitching moment coefficient and the lift-to-drag ratio—were monitored and controlled at the design point, typically located a few degrees below the maximum lift angle of attack, to maintain favorable performance and structural behavior across the operating range. A low pitching moment coefficient is advantageous because a high pitching moment induces torsional deformation, altering the local angle of attack. However, its significance is reduced at the inboard region, where the structure is considerably stiffer and more capable of resisting torsional loads.

Finally, although modern wind turbine design places increasing emphasis on low noise emissions due to environmental regulations, this factor is less critical for the inboard region. Wind speeds near the blade root are relatively low, and because acoustic emission scales approximately with the fifth power of wind velocity, the noise generated in the root

region is substantially lower than that produced near the tip [158]. All favorable airfoil characteristics relevant to flatback airfoil design are summarized in table B.2.

B

DESIGN AND OPTIMIZATION ALGORITHM

As discussed previously, the design and optimization of an airfoil is inherently a multidisciplinary problem that involves the simultaneous consideration of several aerodynamic and structural parameters. These parameters must be evaluated repeatedly throughout the optimization procedure to ensure that the final geometry satisfies both performance requirements and engineering constraints. In general, an optimization problem aims to identify an appropriate design variable vector $X(x_i)$, with x_i for $i = 1, \dots, n$, that minimizes (or maximizes) an objective function $F(x_k)$, while satisfying a set of constraints $G_j(x_i)$ for $j = 1, \dots, m$, which restrict the allowable design space. The objective function evolves as the optimization progresses because its value depends directly on the changes introduced to the components of the design variable vector. The multidisciplinary objective function consists of high sectional moment of inertia, maximum thickness chord position, high maximum lift, high L/D ratio, and short transition point length on the suction side 3 degrees before the stall angle of attack. A constraint was set for the pitching moment coefficient at the design angle of attack to avoid high values.

The optimization process consists of an iterative loop in which aerodynamic and structural characteristics of candidate airfoils are evaluated repeatedly. A schematic overview of the optimization algorithm is presented in figure B.8, where the overall procedure is shown to comprise three major segments.

In the first stage, the airfoil geometry is generated. It should be emphasized that the design variable vector is composed of the control points defining the airfoil contour; therefore, every modification to these points directly alters the resulting shape. In the second stage, the geometric and structural properties of the newly generated airfoil are analyzed in a sequential manner. The first step involves calculating the geometric parameters of interest, most notably the position of the maximum thickness along the chord. It is important to note that parameters such as the trailing edge thickness and overall airfoil thickness can be prescribed within the airfoil generator and remain constant throughout the optimization loop. Furthermore, this step involves computing the structural characteristics of the generated airfoil. Once the geometric evaluation is completed, a computational grid is generated around the airfoil. This grid is subsequently used by the RANS solver to compute the aerodynamic characteristics, including the maximum lift coefficient, stall angle of attack, lift-to-drag ratio and pitching moment. These aerodynamic quantities are typically evaluated at the design angle of attack range, which is selected to extend approximately three degrees below the angle corresponding to $C_{L,max}$. To determine the transition location, a fast and robust viscous-inviscid interaction method is employed, allowing an

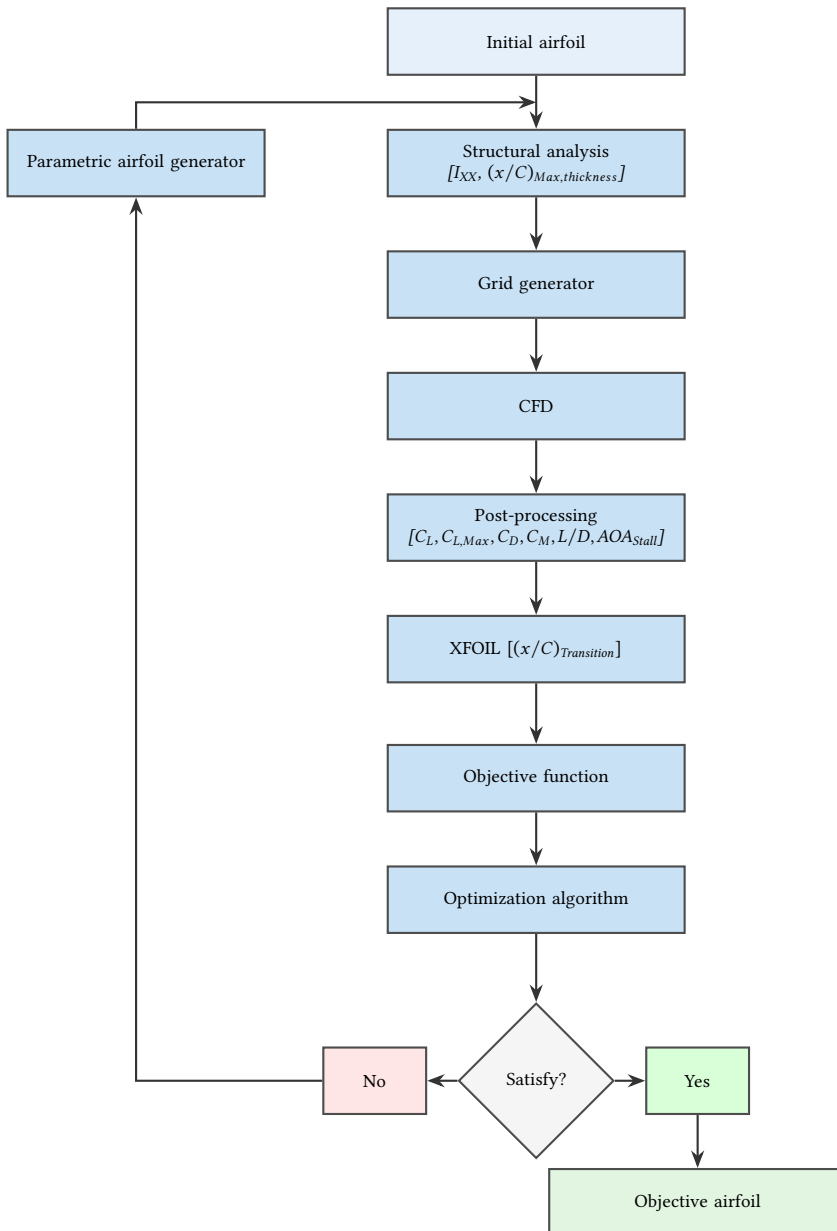


Figure B.8: Flow chart of the airfoil optimization process.

efficient estimation of boundary layer behaviour without the need for excessively costly computations.

At this point, all geometric, aerodynamic, and structural properties needed to construct the objective function are available. With this information, the objective function value is evaluated, completing one full iteration of the optimization loop. The result is then passed back to the optimization algorithm, which determines whether the next iteration is required or whether convergence has been achieved.

B

AIRFOIL GENERATOR

In an airfoil design and optimization problem, the parameters that define the geometry of the airfoil constitute the design variables or the design variable vector. A fundamental requirement in such applications is that the resulting airfoil exhibits smooth and continuous surfaces on both the pressure and suction sides. The airfoil geometry generator must therefore be capable of producing highly flexible shapes while maintaining geometric smoothness. This flexibility ensures that the optimizer can explore the entire design space, thereby reducing the likelihood of convergence to a local extremum. At the same time, the dimensionality of the design space should remain sufficiently small to minimize computational cost and to increase optimization efficiency.

Over the past decades, several methodologies have been developed for parameterizing airfoil shapes. In the present research, the "Synaps airfoil generator" was employed to construct smooth airfoil geometries. This generator uses Bézier-Spline curves, which offer a robust and mathematically elegant method for defining continuous aerodynamic surfaces.

A Bézier-Spline is a parametric curve defined as:

$$P(t) = \sum_{i=1}^{n+1} N_{i,k}(t) P_i, \quad t_{\min} \leq t < t_{\max}, \quad (\text{B.14})$$

where t_i denotes a sequence of real values forming the *knot vector*:

$$t_0 \leq t_1 \leq \dots \leq t_{m-1}, \quad (\text{B.15})$$

and P_i represent the control points ($n + 1$ points in total). The function $N_{i,k}(t)$ is the basis function of order k , with the constraint $1 < k \leq n + 1$. The basis function is defined recursively as:

$$N_{i,1}(t) = \begin{cases} 1, & \text{if } t_i \leq t < t_{i+1}, \\ 0, & \text{otherwise,} \end{cases} \quad (\text{B.16})$$

$$N_{i,k}(t) = \frac{t - t_i}{t_{i+k-1} - t_i} N_{i,k-1}(t) + \frac{t_{i+k} - t}{t_{i+k} - t_{i+1}} N_{i+1,k-1}(t). \quad (\text{B.17})$$

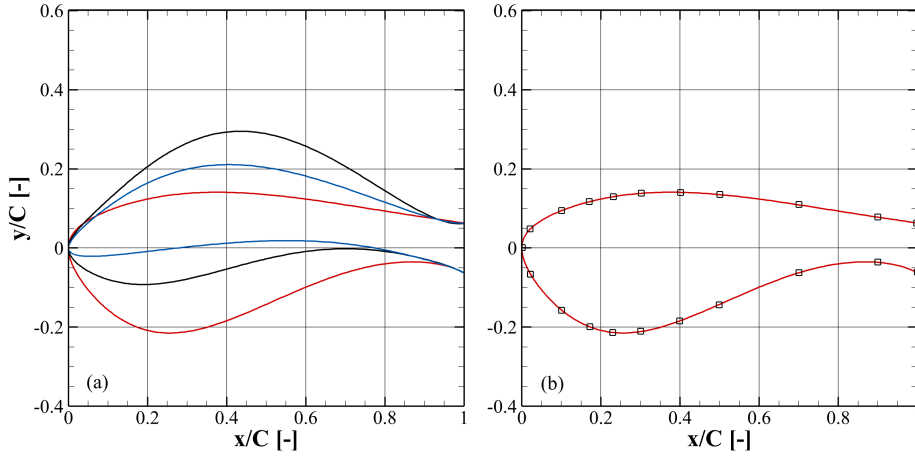


Figure B.9: Flexibility of the airfoil generator and airfoil generator control points.

The knot vector is defined as:

$$T = [t_i, t_{k+(n+1)}], \quad (\text{B.18})$$

subject to the condition:

$$t_i \leq t_{i+1} \quad \forall i. \quad (\text{B.19})$$

Higher values of k yield smoother curves, whereas lower values of k produce sharper transitions between the basis curves. For airfoil design, excessively low values of k result in geometrical discontinuities or abrupt curvature changes, which are undesirable in aerodynamic optimization.

In the Synaps airfoil generator, the relevant geometric parameters, such as airfoil thickness, trailing edge gap, and the control point positions, are specified in an input file. In the present optimization framework, the airfoil thickness, the trailing edge gap, and the x -coordinates of the control points were treated as fixed quantities. Consequently, the y -coordinates of the control points formed the design variable vector. For each airfoil, nine movable control points were defined on both the pressure and suction sides, as illustrated in table B.3 and figure B.9 (b). The control points at the leading edge ($x = 0$) and trailing edge ($x = 1$) were fixed to preserve geometric consistency and boundary conditions.

Due to the steep geometric gradients near the leading edge, a higher density of control points was allocated in this region to enhance the flexibility of the generated shapes. Using too few control points near the leading edge typically results in excessive sensitivity of the airfoil geometry to small perturbations, which can adversely affect robustness and the mesh generation process. Figure B.9 (a) shows examples of airfoils generated by the Synaps code, demonstrating the wide range of geometries that can be produced.

Table B.3: Control point distribution along the chord line used in the airfoil geometry generator.

Surface	0.00	0.02	0.10	0.17	0.23	0.30	0.40	0.50	0.70	0.90	1.00
Upper surface (x/C)	0.00	0.02	0.10	0.17	0.23	0.30	0.40	0.50	0.70	0.90	1.00
Lower surface (x/C)	0.00	0.02	0.10	0.17	0.23	0.30	0.40	0.50	0.70	0.90	1.00

The output of the airfoil generator is a data file containing the normalized surface coordinates of the airfoil. These coordinates serve as direct input to the mesh generator in the subsequent computational workflow.

GRID GENERATOR

To solve the governing flow equations, the computational domain must first be discretized into smaller subdomains. The accuracy, stability, and convergence rate of the numerical simulation depend strongly on the quality of the mesh, which includes the geometric quality of individual elements, the local mesh density in regions with high spatial gradients, and the overall number of elements in the domain. Although increasing the mesh density improves resolution, it also significantly increases computational cost. Therefore, constructing an optimized mesh requires an appropriate balance among element quality, element distribution, and total cell count. In the present work, GAMBIT grid generation software was used to automatically create high quality structured meshes around the generated airfoil geometries during the optimization process. The mesh topology consists of a multi-block configuration, with a C-grid topology applied in the block surrounding the airfoil. The C-grid arrangement is particularly advantageous for airfoil simulations, as it produces high quality elements in the wake region behind the trailing edge, thereby improving drag prediction throughout the optimization. This block is then connected into an O-grid topology. Because the airfoil geometry changes continuously during the optimization, it is essential to construct a robust and accurate boundary layer mesh for every candidate shape. This was achieved using the boundary layer meshing functionality available in GAMBIT. The boundary layer was discretized using 40 layers of grid points normal to the airfoil surface. The first cell height was set to 1×10^{-6} m, resulting in a non-dimensional wall distance of $Y^+ \approx 1.0$, which is appropriate for accurately resolving the viscous sublayer in low Reynolds number aerodynamics. A growth rate of 1.05 was used to ensure sufficient resolution within the viscous sublayer and buffer region. The outer boundary of the first O-grid block was positioned approximately one chord length away from the airfoil surface to improve the controllability of mesh quality in the near-field region. The complete computational domain was extended to a distance of 30 chord lengths in all directions to avoid numerical interference from the far-field boundaries. Sample

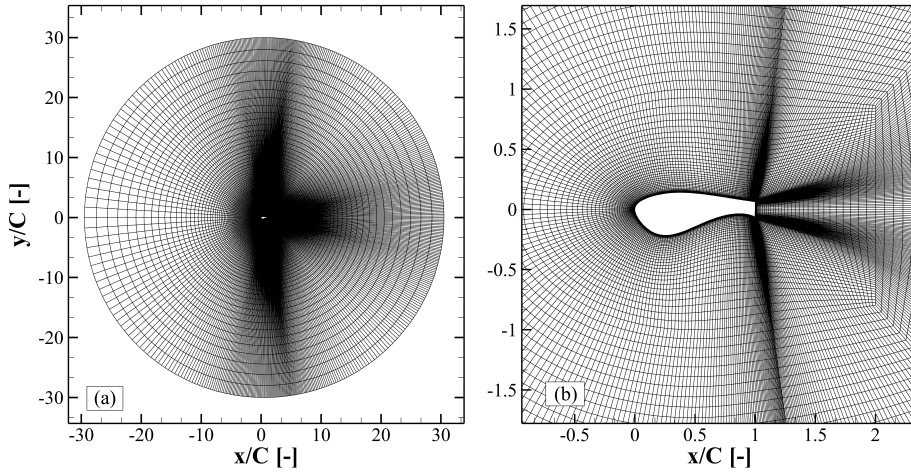


Figure B.10: The meshed flow domain.

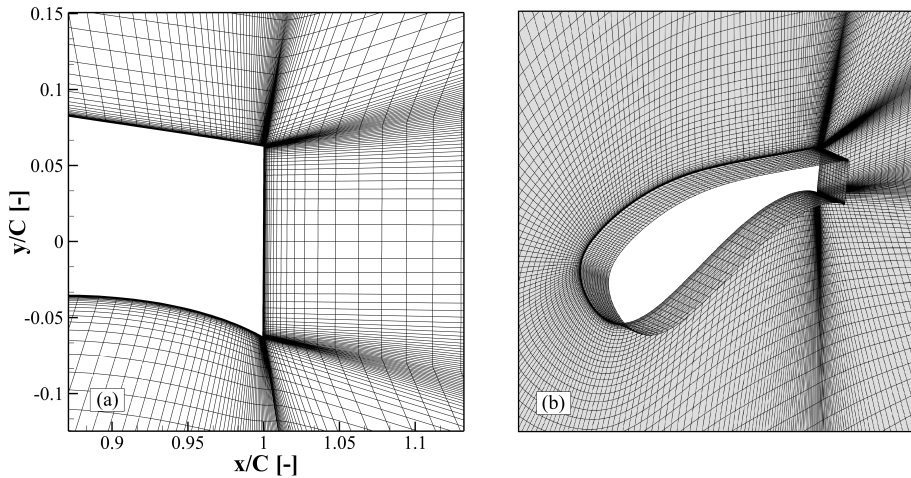


Figure B.11: Mesh distribution, [(a): Trailing edge mesh, (b): Quasi-3D mesh quality].

meshes generated during the optimization process are shown in figures B.10 and B.11. In the quasi-3D mode of the optimization, the flow domain is constructed by extruding the 2D-mesh into the third dimension. The resulting quasi-3D domain, shown in figure B.11 (b), was extruded to a thickness of 0.1 chord lengths in the spanwise direction and contained 10 cells. The final mesh contains approximately 320×120 cells in the chordwise and normal directions, respectively. Note that 50 cells placed at the trailing edge to resolve the wake vortices. Moreover, for the DES computations, the domain was extruded by 0.5 chord

lengths in the spanwise direction, resulting in 50 cells in that direction to resolve the large-scale unsteady structures more accurately. To ensure consistency across all iterations of the optimization loop, the automatic mesh generation process was implemented using a GAMBIT journal file. This script defines all meshing parameters, including node count, grid distribution, growth rates, and block topology. By using this automated approach, the same high quality mesh structure was generated for each airfoil candidate, guaranteeing comparability and reliability of the simulation results throughout the optimization process.

FLOW SOLVER

To simulate the flow field and evaluate the aerodynamic performance of the airfoils throughout the optimization process, the commercial CFD solver Fluent was employed. Fluent is based on the finite-volume method, which discretizes the governing flow equations over control volumes and supports multi-block structured, unstructured, and hybrid mesh topologies. Since the flow field in this study can be considered incompressible, a pressure-based algorithm was adopted for the numerical solution. In this formulation, pressure-velocity coupling is achieved through the solution of a pressure correction equation combined with an iterative procedure. During the optimization loop, the SIMPLE algorithm was selected due to its robustness in achieving convergence and its comparatively low computational cost. For the unsteady aerodynamic analysis of the optimized airfoils presented in section B.4.4, the PISO algorithm was used instead. Although PISO is computationally more expensive than SIMPLE, it provides superior temporal accuracy and is therefore more suitable for unsteady flow simulations. A second-order upwind discretization scheme was employed for the spatial discretization of both the flow and turbulence equations to minimize numerical diffusion and ensure appropriate solution accuracy. For turbulence modeling within the optimization process, the two-equation $k-\omega$ SST model was employed. This model is well suited for flows involving adverse pressure gradients and separation, making it an appropriate choice for airfoil simulations in dynamic stall or near stall conditions.

To further analyze the unsteady aerodynamic behavior of the optimized airfoils (see section B.4.4), the DES turbulence model was also employed. DES is a hybrid RANS-LES approach: it employs RANS modeling in attached flow regions and transitions to an LES-type formulation in separated flow zones. Although DES provides improved prediction of large-scale unsteady structures, it is substantially more computationally expensive than pure RANS approaches and therefore unsuitable for use within the iterative optimization loop. The DES model used in this work is based on the underlying $k-\omega$ SST RANS formulation.

In addition to the CFD simulations, the XFOIL code [144] was used for prediction of the transition point 3 degrees before the stall angle of attack on the suction side of the airfoil

during the optimization. This value is then used in the objective function to decrease the sensitivity of the lift coefficient to the leading edge roughness. Note that this transition point location is not used in the RANS code. XFOIL is a viscous-inviscid interaction method that employs a linear-vorticity, stream-function panel method in the inviscid region and a two-equation lagged-dissipation integral boundary layer formulation in the viscous region. The viscous and inviscid solutions are strongly coupled through a surface transpiration model [229, 230]. XFOIL also incorporates a specific treatment for blunt trailing edge geometries. Transition prediction in XFOIL is based on the e^n method, which relies on local linear stability theory [231]. In this method, the velocity profile along the airfoil surface is used to compute the local amplification rate of unstable waves. The amplification rate is then integrated along the streamline to obtain the n -factor, and transition is predicted when the disturbance amplitude e^n exceeds a specified threshold. Although several studies have shown that the e^n method yields reasonably accurate transition predictions [232], its reliability decreases in the presence of separated flows [233]. For the optimization procedure in this work, an n -factor of 9 was selected to increase the sensitivity of the performance of the airfoil to the leading edge roughness during the optimization. This leads to better final optimized airfoils.

VALIDATION OF 2D-FLOW SOLVER

In this section, two independent wind tunnel measurement campaigns are used as reference data to validate the numerical predictions of the 2D-flow solver.

The first measurement campaign was conducted in the low-speed wind tunnel at Virginia Tech University [234]. Aerodynamic and aeroacoustic measurements were performed on the DU97-W-300 airfoil [235] and its flatback variant. The airfoil geometry was provided by Delft University upon request. The flatback configuration of the DU97-W-300 airfoil was generated by gradually increasing the thickness aft of the maximum thickness location, resulting in a blunt trailing edge with a width of 10% of the chord. Figure B.12 (a) illustrates both the original and flatback variants of this airfoil. The test models had a span of 1.8 m and a chord length of 0.914 m. The tests were performed at Reynolds numbers of $Re = 3 \times 10^6$. Flatback airfoil results from this campaign were used to validate the 2D numerical simulations. Additional details regarding the aerodynamic and aeroacoustic measurements can be found in [158, 165, 166, 234]. Due to the relatively high solid-blockage ratio of the DU97-W-300-10 model in the Virginia Tech wind tunnel and the possibility of small installation misalignments, the uncertainties in the lift and drag coefficients were estimated to be approximately $\pm 5\%$ for this airfoil. The uncertainty in the effective angle of attack was estimated as $\pm 0.5^\circ$ [166]. For the DU97-W-300-10 airfoil, only the clean configuration was used for drag measurements.

The second measurement campaign was carried out in the aeronautical wind tunnel at UC Davis [35]. The airfoil tested at was the FB-3500-0875 flatback airfoil, derived from the FB-3500-0005 sharp trailing edge airfoil. This airfoil family was developed in Phase I of

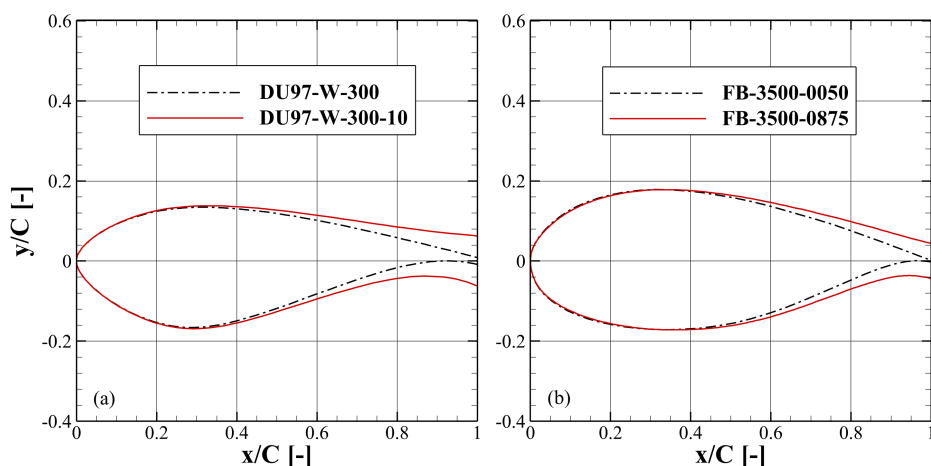


Figure B.12: Comparison between sharp trailing edge and flatback airfoil variants, [(a): DU97-W-300-xx, (b): FB-3500-0xxx].

the BSDS [156]. Figure B.12 (b) shows both airfoil sections. The FB-3500-0875 model had a relative thickness of 35% and a trailing edge bluntness of 8.75%. A chord length of 0.203 m was used, allowing tests at a Reynolds number of $Re = 666 \times 10^3$. Lift was measured using a force balance, while drag was determined via wake-survey measurements. Note that for the tripped DU97-W-300-10 airfoil, trip strips were positioned at $x/C = 0.05$ on the suction side and $x/C = 0.10$ on the pressure side. For the FB-3500-0875 airfoil, the trip locations were $x/C = 0.02$ (suction) and $x/C = 0.05$ (pressure).

All CFD simulations were conducted at Reynolds numbers of $Re = 3 \times 10^6$ for the DU97-W-300-10 airfoil and $Re = 666 \times 10^3$ for the FB-3500-0875 airfoil. The mesh blocking strategy for the 2D RANS simulations followed the approach described in Section B.3.3.4. To confirm grid independence, several grid densities were evaluated. Furthermore, two models were used in the 2D steady RANS simulations: the two-equation $k-\omega$ SST model [236] and the transitional four-equation SST model [237]. These models are well suited for flows with separation and adverse pressure gradients.

Figures B.13, B.14, and B.15 compare the wind tunnel data and CFD predictions for both the DU97-W-300-10 and FB-3500-0875 flatback airfoils. The measured DU97-W-300-10 data are shown with their associated uncertainties. The predicted lift coefficients from the CFD simulations agree closely with the measurements for both airfoils, with deviations generally within $\pm 5\%$. The DU97-W-300-10 measurements cover only the linear lift range, whereas the FB-3500-0875 data include the full angle of attack sweep. As shown in figure B.15 (a), the transitional CFD model accurately captures the stall angle for the FB-3500-0875 airfoil, while the fully turbulent model predicts a delayed stall, which is consistent with the known behavior of RANS turbulence models. The drag coefficient comparisons for both airfoils are shown in figures B.14 and B.15 (b). For the FB-3500-0875 airfoil, the agreement between

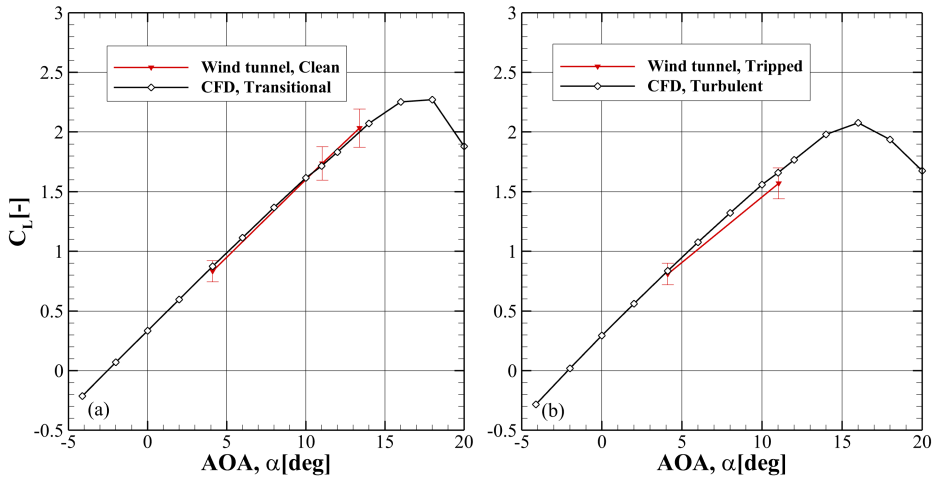


Figure B.13: Lift coefficient comparison of DU97-W-300-10 flatback airfoil, [$Re = 3 \times 10^6$, (a): Clean, (b): Tripped].

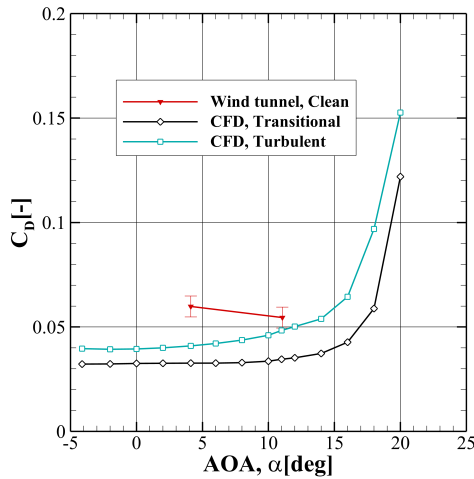


Figure B.14: Drag coefficient comparison of DU97-W-300-10 flatback airfoil, [$Re = 3 \times 10^6$].

numerical and experimental drag is moderate, with CFD generally underpredicting the drag for both clean and tripped configurations. For the DU97-W-300-10 airfoil, the discrepancies between predicted and measured drag are more pronounced.

It is important to note that both wind tunnel campaigns used a wake-survey technique to determine the drag coefficient. However, as shown in studies by Biao, Bragg, and Van

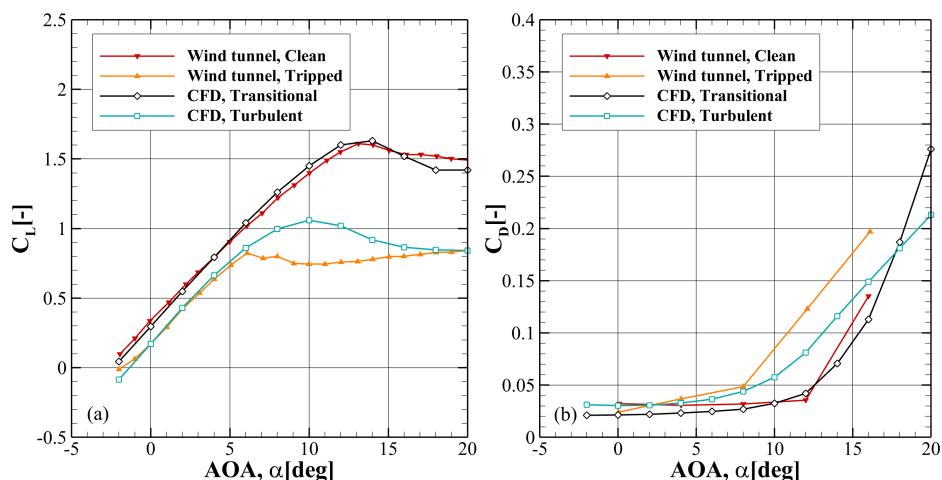


Figure B.15: Lift and drag coefficient comparison of FB-3500-0875 flatback airfoil, [$Re = 0.6666 \times 10^6$].

Dam [115, 238, 239], the wake-survey method is not reliable for separated flows. The commonly used equations for determining profile drag in wind tunnels were developed by Betz and Jones [235, 240]. These relations are valid only for steady, two-dimensional flows without significant separation [238].

As discussed in Taylor's analysis [241], applying the Betz–Jones relations in separated flow conditions can lead to large errors due to the underlying assumptions:

- Negligible Reynolds stresses along the streamline,
- Constant total pressure along the streamline.

Therefore, the wake-survey technique becomes unreliable in unsteady and highly turbulent wake regions associated with separated flows [238]. Additional investigations, such as employing a force balance, are recommended to obtain accurate drag coefficients for flatback airfoils.

STRUCTURAL ANALYSIS AND GEOMETRICAL PARAMETERS

The structural characteristics and key geometrical attributes of each airfoil were evaluated prior to and throughout the optimization process. The XFOIL code was employed to compute the required structural property, namely the sectional skin second moment of inertia, which serves as an indicator of the airfoil's bending stiffness. In addition to this capability, an in-house analysis tool was developed to determine several geometrical parameters of interest, including the chordwise position of maximum thickness, the

back-camber magnitude on the pressure side near the trailing edge, and the locations of maximum thickness on both the pressure and suction surfaces. Among the set of computed geometrical quantities, only the chordwise position of maximum thickness was explicitly incorporated as a constraint in the optimization process. This constraint ensures that the optimized airfoil maintains a structurally feasible and physically meaningful thickness distribution, particularly in regions subject to manufacturing or structural limitations. The remaining geometrical parameters were monitored throughout the optimization to assess their evolution, verify the geometric integrity of the generated airfoils, and ensure that no undesirable or unrealistic geometric features were introduced as the optimizer explored the design space.

OBJECTIVE FUNCTION SETUP

The definition of the objective function F is a critical step in multi-objective optimization, particularly when the design goals conflict with one another. Although numerous strategies exist for constructing objective functions, they can generally be divided into three principal categories that are widely used in multidisciplinary optimization problems.

The first category consists of constructing a single scalar objective function. In this approach, all individual objectives are combined into one expression by applying weighting factors that specify the relative importance of each objective. This method significantly reduces computational effort and simplifies the optimization procedure. However, appropriate scaling of the various design goals is required in order to formulate a meaningful combined objective function, and determining these scaling and weighting factors remains a challenging task [222]. The choice of the factors is strongly dependent on the designer's experience, and inappropriate scaling can alter the topography of the solution space [242]. For these reasons, this approach often yields different objective functions—and thus different optimal solutions—depending on the weighting strategy, which raises concerns regarding its reliability [242–244].

The second category treats each objective independently, based on the concept of dominance. According to this principle, a solution A dominates a solution B if all objective values of A are at least as good as those of B , and at least one objective of A is strictly better [245]. The set of solutions that are no longer dominated is known as the Pareto-optimal set, which forms an n -dimensional surface in the design space. This surface can be examined by a decision maker to perform trade-off analysis and select the final design. However, because the decision maker's judgment is subjective, and because Pareto surfaces are difficult to interpret in high-dimensional spaces, this approach becomes impractical when many objectives or many design variables are involved [242].

The third category is the lexicographic (preemptive) method [246], in which the objectives are ranked in order of priority. The optimization is then performed sequentially

B

according to this ranking. Although this method is straightforward to conceptualize and easy to apply for small problems, it requires a clear understanding of the relative importance of each objective from the outset. In addition, the hierarchical nature of this approach leads to increased coding and implementation complexity [242].

Despite their widespread use, the methods described above all face difficulties arising from uncertainties in evaluating different candidate designs [242]. To mitigate these issues, later studies introduced methods that rely on imprecise or linguistically defined problem specifications [247]. In these methods, the level of desirability of each design goal is divided into categories such as unacceptable, highly undesirable, undesirable, tolerable, desirable, and negligibly desirable. By adopting linguistic categories, the need for explicit weighting or rescaling among objectives is removed. However, this approach also increases numerical complexity and remains computationally demanding [242].

In the present research, the optimized shapes of the flatback airfoils were obtained using the optimization software CAOne [248], which provides several algorithms capable of addressing multiple aerodynamic and structural objectives simultaneously. The SA algorithm was selected as the optimization method. The strategy used in this study is based on fuzzy logic, where the objectives are formulated using verbal descriptions rather than narrowly specified numerical values. This approach differs from multi-level fuzzy methods presented in earlier studies [247, 249]. Instead, it represents an extended form of the single-objective concept while allowing the use of various optimization algorithms [242].

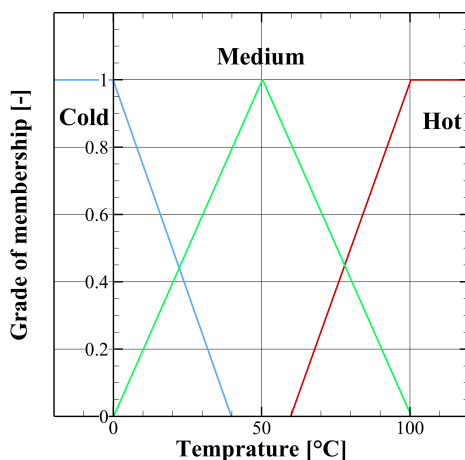


Figure B.16: Fuzzy logic diagram for temperature.

Fuzzy logic is a multi-valued logic that reflects the way humans make decisions. Unlike Boolean logic, in which every statement is either entirely true or entirely false, fuzzy logic allows truth values to lie anywhere within the continuous interval $[0, 1]$. For example,

Table B.4: The grade of membership for each temperature distribution.

Temperature [°C]	Cold	Medium	Hot
0	1	0	0
25	0.375	0.5	0
50	0	1	0
75	0	0.5	0.375
100	0	0	1

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if white corresponds to 0 and black corresponds to 1, then all shades of gray represent intermediate truth values. Fuzzy logic was introduced by Lotfi Zadeh in 1965 [250]. A commonly used example is shown in figure B.16, where each temperature possesses a degree of membership in linguistic sets such as “cold,” “medium,” or “hot.” In the desirable temperature range, a specific degree of membership can be assigned to each temperature, as shown in table B.4. This step is known as fuzzification.

As in Boolean logic, fuzzy systems use logical operators such as AND, OR, IF, and THEN. For instance, the statement IF temperature is medium OR hot THEN consequent (X) assigns the value X whenever the temperature belongs sufficiently to the “medium” or “hot” sets. By combining such statements, a complete fuzzy decision system can be constructed. However, linguistic expressions alone are not suitable for numerical analysis. Therefore, a step called defuzzification is required to convert linguistic evaluations into numerical values. Several defuzzification methods exist, including Adaptive Integration (AI), Constraint Decision Defuzzification (CDD), Center Of Area (COA), Center Of Gravity (COG), and Center of Maximum (COM). In this research, the COM method was adopted, as it yields a representative compromise value and is computationally efficient [251].

A fuzzy-logic-based system requires three main steps [242]:

- Define the membership functions for all design objectives, specifying desirable, tolerable, and unacceptable ranges.
- Construct a fuzzy reasoning system consisting of logical expressions that describe the relationships among the objectives.
- Apply a defuzzification method (COM in the present study) to convert the linguistic evaluations into numerical values.

In this research, each design objective as well as the overall objective of the optimization was divided into three categories: desirable, tolerable, and unacceptable. Next, the limits of the membership functions were defined for each objective. Working in two-dimensional membership spaces is conceptually clearer than manipulating weightings in high-dimensional objective spaces [242].

After defining the membership functions, the fuzzy reasoning system was constructed. This system consists of logical expressions such as:

- IF the maximum thickness chord position is DESIRABLE AND the maximum C_L is DESIRABLE THEN the design is DESIRABLE.
- IF the transition point on the suction side is DESIRABLE AND L/D is TOLERABLE THEN the design is TOLERABLE.

Finally, the COM method was applied to convert the fuzzy inference results into numerical values. The COM method yields a representative value corresponding to the dominant membership functions and offers good computational efficiency [251].

The final objective function is given by [242]:

$$F^{**}(\bar{x}) = \frac{\sum_k x_{c_k} s_k}{\sum_k s_k}, \quad (\text{B.20})$$

where F^{**} is the fuzzy-based objective function, $\bar{x}$ is the vector of design variables, x_{c_k} is the center of gravity of the membership function associated with objective k , s_k is the degree of membership produced by the logical rules, and k is the number of fuzzy sets. A detailed explanation of this method is provided in [242].

B.3.4 EXPERIMENTAL SETUP

WIND TUNNEL

The wind tunnel experiments were conducted in the low-speed wind tunnel (MUB) of the Technical University of Braunschweig. The MUB is a closed-return, Göttingen-type atmospheric wind tunnel equipped with three interchangeable test sections featuring different contraction ratios. Two of these test sections are closed, while one is configured as an open test section.

The measurements for this research were performed in the closed test section with a cross sectional area of 1300×1300 mm and a test section length of 6000 mm. In this configuration, the nozzle contraction ratio is 4.9:1, which results in a turbulence intensity of approximately 0.2% at an airspeed of 53 m/s. The maximum achievable flow velocity in the empty test section is 60 m/s. Two-dimensional test models can be mounted horizontally in the test section using two turntables positioned at the sidewalls. These turntables allow precise alignment of the model and provide unobstructed optical access during flow field measurements. To maintain a constant static pressure distribution throughout the test section, the ceiling and floor are installed with a 0.2° upward divergence. This adjustment compensates for boundary layer growth along the test section walls. A fine oil spray seeding system was used to generate atomized tracer particles for the PIV measurements. The

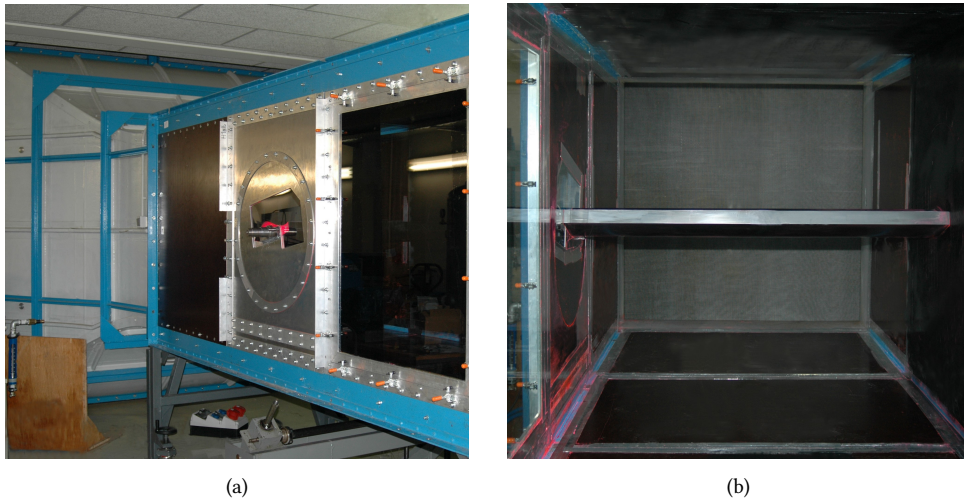


Figure B.17: Wind tunnel, [(a): Exterior view of the test section with turntable side wall, (b): Interior of the test section as seen from downstream].

seeding units were positioned upstream of the settling chamber to ensure uniform seeding throughout the test section. The wind tunnel is driven by a 300 kW variable-speed DC motor located outside the tunnel housing. The motor transmits power to a single stage ducted fan propeller via a belt drive. To maintain stable flow conditions during the experiments, particularly with respect to air temperature, a heat exchanger with a total capacity of 300 kW is installed inside the settling chamber. The settling chamber also contains four turbulence reducing screens to further homogenize the inflow conditions. Exterior and interior views of the wind tunnel are shown in figures B.17 (a) and (b) respectively.

TEST AIRFOIL

The aerodynamic experiments were conducted on one of the optimized airfoil sections, namely the HB-FB-350-150 airfoil. This airfoil has a relative thickness of 35% and a trailing edge bluntness of 15%. The geometry of the HB-FB-350-150 section is shown in figure B.18. The wind tunnel model had a chord length of 300 mm and a span of 1298 mm. The outer skin of the model was manufactured from carbon fiber composite, providing a stiff and lightweight structure suitable for aerodynamic testing. A high-grade steel spar, with an outer diameter of 33.7 mm and a wall thickness of 2.6 mm, acted as the primary load carrying element. In addition, six wooden ribs were distributed along the span to transfer aerodynamic loads to the spar and to maintain the airfoil contour.

To mount the model securely in the wind tunnel, two M10 bolts were installed on each

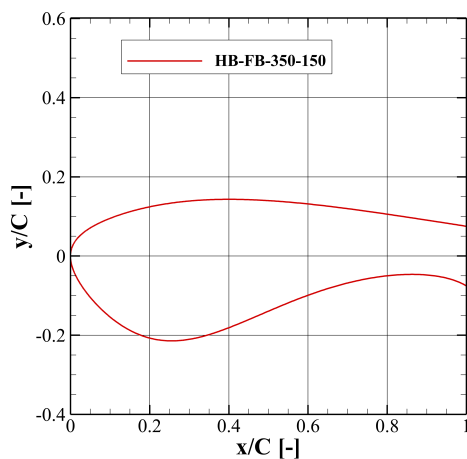
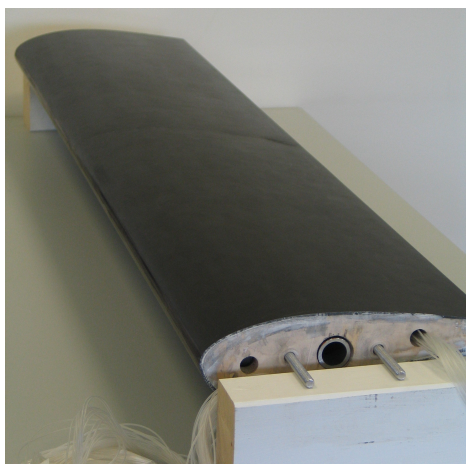
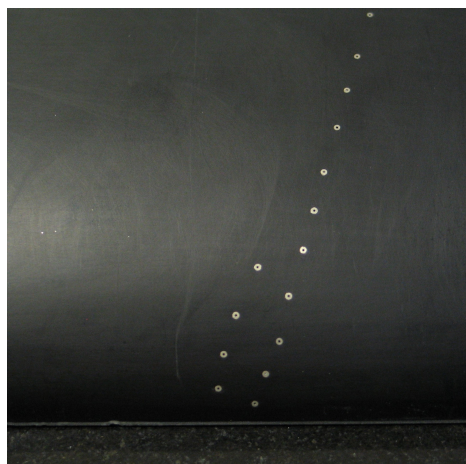


Figure B.18: HB-FB-350-150 flatback airfoil shape.



(a)



(b)

Figure B.19: HB-FB-350-150 model, [(a): Wind tunnel model, (b): Pressure tabs distribution at the leading edge area].

side of the airfoil section for attachment to the rotating end-plates. The structural design incorporated a safety factor of 4.0 to avoid any risk of model failure and to protect the wind tunnel hardware throughout the experiments. Dimensional inspections showed a maximum deviation of approximately 0.2 mm along the span on both the pressure and suction sides, confirming the high geometric accuracy of the model. The model was instrumented with a

total of 75 pressure taps positioned at the mid-span: 34 taps on the suction side, 37 taps on the pressure side, and 4 taps located at the trailing edge. Each pressure tap had an internal diameter of 0.5 mm and an external diameter of 1 mm. To capture the strong pressure gradients typically present near the leading and trailing edges, the tap density was increased in these regions. In addition, to minimize interference between upstream and downstream pressure ports, the taps were installed with a slight inclination relative to the chord line. During testing, the model was mounted horizontally and in an inverted configuration, as shown in figure B.17. The model was fixed to the support system on both sides. This support system allowed accurate adjustment of the angle of attack, and the geometric zero angle was set using a precision bubble level. To simulate leading edge roughness effects, trip tape was applied at 2% of the chord on the suction side and 17% of the chord on the pressure side. The trip tape had a thickness of 0.16 mm and a width of 3 mm. The wind tunnel model and the distribution of pressure taps are shown in figure B.19.

INSTRUMENTATION

The surface pressure measurements were acquired using a PSI 8400 SDI multichannel pressure data-acquisition system. This high speed pressure scanner is capable of recording up to 200 Hz with an accuracy of $\pm 0.05\%$ of the full-scale output. The system was connected to three ESP-32 pressure scanners, each with a measurement range of ± 5 PSID.

Each scanner can record 32 pressure channels simultaneously. Prior to the experiments, and again after every five measurement runs, all scanners were zeroed and calibrated relative to one another to ensure consistency and accuracy. At each angle of attack, 100 samples were collected over a total sampling period of 20 seconds. The free-stream velocity in the wind tunnel was determined from the differential static pressure measured between the settling chamber and the nozzle using a Setra 239 high accuracy differential pressure transducer. The angle of attack of the model was measured using a Lucas LSRP-90 digital inclinometer with a precision of 0.02%. This instrument was mounted directly on the model support system, as shown in figure B.20.

A LaVision PIV system was employed to analyze the wake flow behind the trailing edge of the airfoil. Particle illumination was provided by a Litron Nano T double-pulse Nd:YAG laser operating at 200 mJ/pulse, with a wavelength of 532 nm and a pulse separation of 5–7 ns. Image acquisition was performed using a LaVision Imager Pro X 11M camera equipped with an air-cooled CCD sensor, operating at a double-frame rate of 5 Hz and providing a resolution of 10.7 megapixels. The camera was fitted with a Zeiss Macro-Planar T* 2/50 lens and was mounted on the wind tunnel sidewall, imaging the test section through the turntable window. The camera arrangement is shown in figure B.21 (a). Laser illumination of the wake was achieved by positioning the laser system beneath the wind tunnel and directing the beam upward through the test section floor. A series of optical

lenses and mirrors was used to guide and shape the beam into a laser sheet, as shown in figure B.21 (b). The laser sheet was positioned at the mid-span of the model. Before the PIV measurements, a calibration plate was placed in the laser sheet at the region of interest. The plate contained a grid with 10 mm spacing, which allowed the conversion of pixel coordinates into physical coordinates during the calibration process. Standard 2D2C PIV

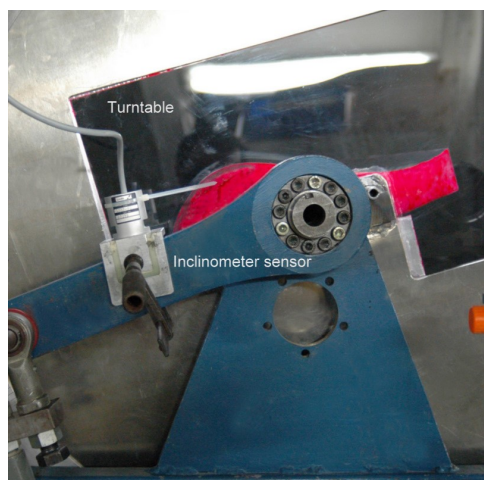
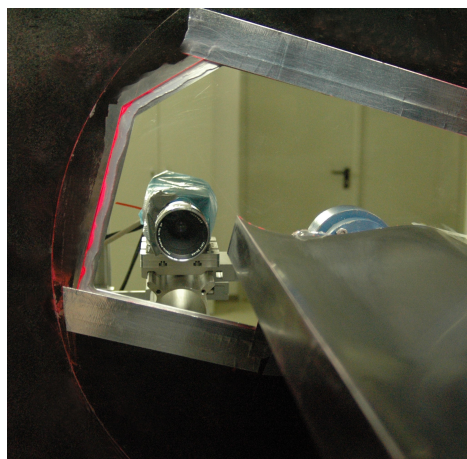


Figure B.20: Model support system and inclinometer.



(a)



(b)

Figure B.21: PIV setup, [(a): Camera position at the trailing edge of the airfoil, (b): Laser setup, beneath the test section].

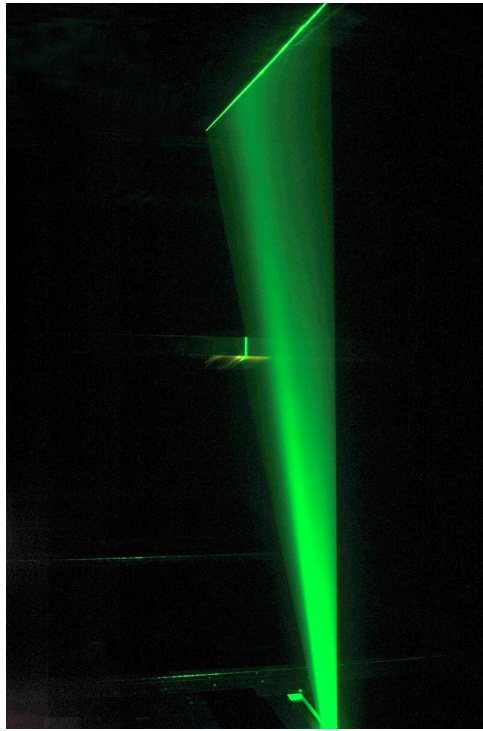
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Figure B.22: Laser sheet hits the model trailing edge from floor of the test section.

measurements were performed at several angles of attack using a multipass autocorrelation procedure. At each angle, 500 image pairs were recorded at a sampling rate of 2 Hz. The recorded images were subsequently processed using DaVis V7.0 for vector calculation and visualization. Figure B.22 presents the laser sheet location within the test section.

B.4 RESULTS

B.4.1 INTRODUCTION

This part is organized into three main components. The first section presents the optimized airfoil sections obtained using a two-dimensional flow solver, with particular attention to their aerodynamic performance and design characteristics. The second section extends this analysis by evaluating the optimized airfoil sections generated with a quasi-3D solver, highlighting the influence of three-dimensional flow effects on the optimized geometries. Finally, the third section addresses the aerodynamic behavior of flatback airfoils, providing a comprehensive discussion of their performance, flow features, and potential advantages for relevant applications.

B.4.2 2D-FLOW SOLVER BASED OPTIMIZATION OF FLATBACK AIRFOILS

The HB-FB flatback airfoil family, comprising variations in both maximum thickness and trailing edge bluntness, has been designed and optimized for application in large variable-speed, pitch-regulated multi-megawatt wind turbines at a Reynolds number of $Re = 6 \times 10^6$. This family consists of airfoils with four maximum-thickness levels: 35%, 40%, 45%, and 50% of the chord. For the airfoils with maximum thicknesses of 35% and 40%, two trailing edge bluntness configurations were developed. In total, six flatback airfoil sections were produced through the optimization process. The geometrical characteristics of these optimized airfoils are summarized in table B.5. To compare the optimized airfoil results numerically, the flatback variant of the Delft DU97-W-300 was selected as a reference. Since the original airfoil has a maximum thickness of 30% of the chord, the maximum thickness of the new DU97-W-XXX airfoils was increased systematically without altering the camber line.

In the naming convention used for this airfoil family, the first number denotes the overall

Table B.5: The optimized flatback airfoil sections.

Airfoil	Maximum thickness position (x/C)	Maximum thickness [%]	Trailing edge thickness [%]
HB-FB-350-125	0.28	35	12.5
HB-FB-350-150	0.28	35	15.0
HB-FB-400-150	0.28	40	15.0
HB-FB-400-175	0.28	40	17.5
HB-FB-450-175	0.28	45	17.5
HB-FB-500-200	0.28	50	20.0

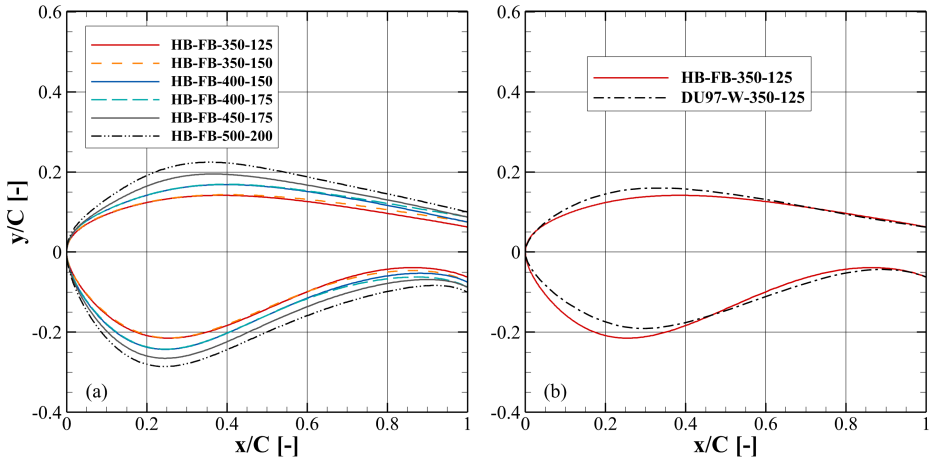


Figure B.23: Optimized flatback airfoils, [(a): HB-FB-XXX-YYY flatback airfoils, (b): Comparison between the HB-FB-350-125 and DU97-W-350-125 airfoils].

maximum thickness expressed as a percentage of the chord, whereas the second number specifies the trailing edge bluntness. Figure B.23 (a) illustrates the resulting flatback airfoil family. As shown in the figure, the optimized geometries rely on two primary mechanisms to generate lift. The first mechanism involves the formation of a suction peak on the suction side of the airfoil. The second mechanism is the generation of elevated pressure near the blunt trailing edge on the pressure side, commonly referred to as rear-loading. This rear-loading contribution operates in a manner comparable to a small deflected flap at the trailing edge.

For conventional sharp trailing edge airfoils, it is inherently difficult to increase the trailing edge pressure without inducing adverse aerodynamic side effects. Such airfoils require the suction side curvature near the trailing edge to align smoothly with the pressure side. This geometric constraint increases the risk of flow separation at moderate to high angles of attack, due to the stronger adverse pressure gradients imposed in the aft region of the airfoil. The flatback configuration avoids this limitation and permits a controlled pressure rise at the trailing edge without triggering premature flow separation. Furthermore, the figure reveals a minor geometric variation near the leading edge among the optimized airfoils of identical maximum thickness. This variation likely arises from the imposed design constraints, such as the prescribed location of the maximum thickness position and the requirement to promote transition to turbulence a few degrees below the stall angle of attack. More pronounced differences appear downstream of the maximum thickness position, where the optimization adjusts the pressure side contour to satisfy the rear-loading behavior. The airfoils also demonstrate that the maximum height on the suction side is lower than that on the pressure side for the optimized flatback designs. This characteristic is a direct consequence of the constraint related to robustness against leading edge roughness,

which drives the suction side geometry toward a reduced pressure gradient and thus decreases sensitivity to surface contamination.

Figure B.23 (b) presents a comparison between the optimized HB-FB-350-125 airfoil and the modified DU97-W-350-125 flatback airfoil. As shown in the figure, the optimized geometry exhibits a reduced thickness on the suction side compared to the DU97-W-350-125 reference. This reduction increases the maximum height of the pressure side contour relative to the chord line. As a result, the optimized airfoil develops a more pronounced aft camber on the pressure side, especially near the trailing edge region, than the modified DU97-W-350-125 airfoil. The remaining HB-FB-XXX-YYY airfoils demonstrate similar geometric behavior when compared with their DU97-W-XXX-YYY counterparts. It should also be noted that the maximum thickness position differs between the two families, i.e., in the optimized HB-FB series it is located at 28% of the chord, whereas in the DU97-W-XXX-YYY family it is positioned at 30% of the chord.

Figure B.24 (a) shows the simulated lift coefficient polars of the HB-FB-350-125 and DU97-W-350-125 flatback airfoils for both fully turbulent and transitional flow regimes. The results indicate that, over the mid-range angles of attack, both airfoils exhibit nearly identical lift coefficient levels in both flow conditions. At higher angles of attack, however, the HB-FB-350-125 airfoil achieves a noticeably higher maximum lift coefficient as well as a higher stall angle than the DU97-W-350-125 reference. It is important to emphasize that, within the wind energy sector, the fully turbulent polars of an airfoil are typically of greater relevance than those corresponding to clean transitional conditions. This is due to the fact that operational wind turbine blades seldom remain free of surface contamination. In practice, insects, dust accumulation, icing, and coating degradation frequently roughen the surface and thereby suppress laminar flow, creating predominantly turbulent conditions

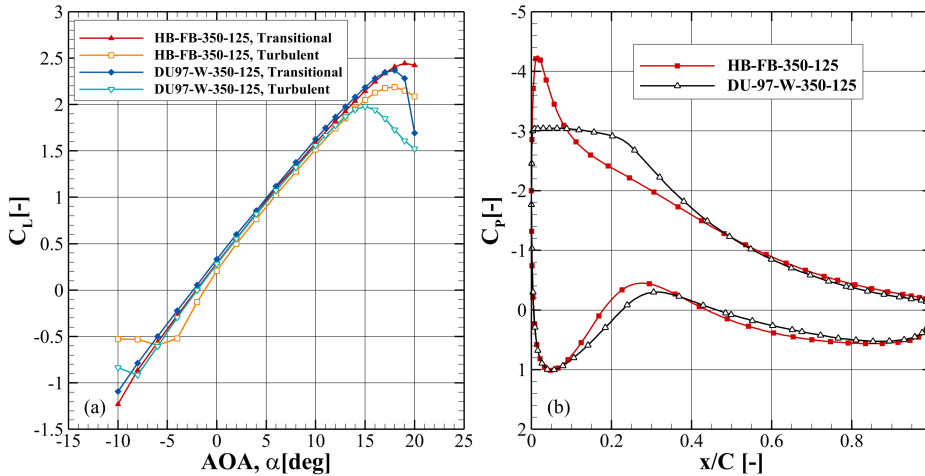


Figure B.24: Aerodynamic characteristics comparison between the HB-FB-350-125 and DU97-W-350-125 airfoils, [$Re = 6 \times 10^6$, (a): C_L , (b): C_p at $\alpha = 12^\circ$].

during most of the turbine's operational lifetime. Furthermore, the figure shows that under turbulent conditions the HB-FB-350-125 airfoil indicates a lower absolute value of the negative maximum lift coefficient and a lower negative stall angle of attack compared with the DU97-W-350-125 airfoil. This behavior at negative angles of attack is primarily a consequence of the greater thickness of the pressure side contour in the HB-FB-350-125 airfoil, which intensifies its sensitivity to leading edge roughness when subjected to reversed flow conditions. These aerodynamic characteristics are particularly relevant for the blade in a parked or idling state, where the structure may experience substantial wind gusts and thus a wide range of angles of attack. Because the HB-FB-350-125 airfoil stalls at lower absolute angle of attack and reaches lower magnitude negative lift coefficients, the resulting aerodynamic loads on the blade are reduced, which contributes positively to structural safety under extreme wind scenarios.

Figure B.24 (a) depicts that the difference between the simulated fully turbulent and transitional lift coefficient polars is similar for both airfoils at low to mid-range angles of attack. At higher angles of attack, however, the discrepancy between the two flow regimes becomes smaller for the HB-FB-350-125 airfoil than for the DU97-W-350-125 airfoil. This behavior indicates that the HB-FB-350-125 airfoil exhibits a lower sensitivity to leading edge roughness. This reduced sensitivity can be understood by considering the underlying design strategy. As discussed earlier, the optimized airfoil features a lower suction side height and a more pronounced aft camber compared with the DU97-W-350-125 airfoil. The resulting suction side geometry promotes a more favorable pressure recovery process. In practical terms, the airfoil becomes less dependent on strong suction peaks on the suction side and more reliant on positive pressure generated by the enhanced aft camber on the pressure side. Consequently, the optimized airfoil generates lift with a reduced contribution from suction forces and an elevated contribution from overpressure near the trailing edge.

Figure B.24 (b) presents a comparison of the simulated pressure coefficient distributions at an angle of attack of 12° and a Reynolds number of $Re = 6 \times 10^6$ for the HB-FB-350-125 and DU97-W-350-125 airfoils. The results clearly show that the HB-FB-350-125 airfoil exhibits a lower pressure gradient on the suction side than the DU97-W-350-125 geometry. A reduced suction side pressure gradient diminishes the airfoil's susceptibility to flow unsteadiness and delays the onset of stall. Consequently, the HB-FB-350-125 airfoil achieves higher stall angles of attack. On the pressure side, the HB-FB-350-125 airfoil generates a stronger overpressure compared with the DU97-W-350-125 airfoil. This aerodynamic mechanism naturally increases the nose-down pitching moment. Although this higher pitching moment might be undesirable at outboard blade locations, it does not pose a critical concern in the inboard region of large wind turbine blades, where the structure is significantly stiffer. Moreover, a stronger nose-down pitching moment can act beneficially during gust events by reducing the local angle of attack, thereby lowering the aerodynamic loading on the blade.

The simulated C_L - C_D polars for both airfoils in fully turbulent and transitional flow regimes are presented in figure B.25 (a). The results demonstrate that, across most of

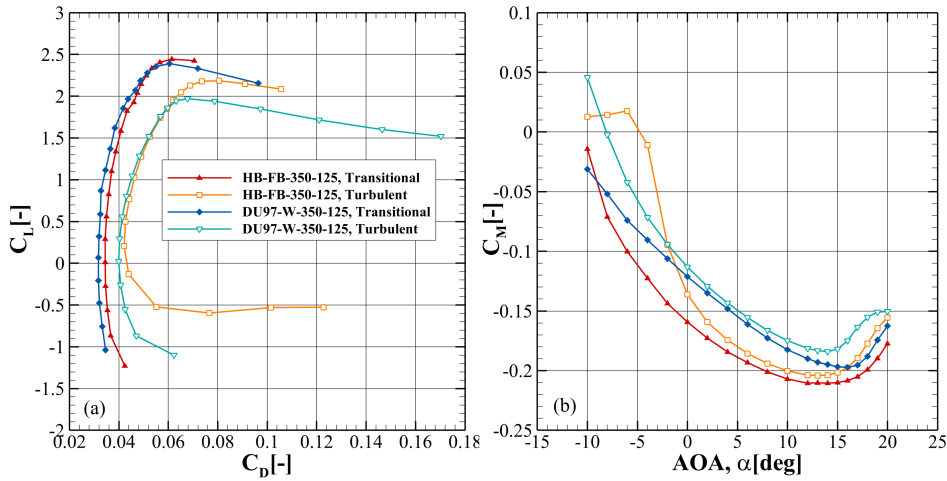


Figure B.25: Aerodynamic characteristics comparison between the HB-FB-350-125 and DU97-W-350-125 airfoils, [$Re = 6 \times 10^6$, (a): C_L - C_D , (b): C_M].

the operating range, the DU97-W-350-125 airfoil exhibits slightly lower drag than the HB-FB-350-125 airfoil. However, at higher angles of attack the drag coefficients of both airfoils converge and become nearly identical.

Figure B.25 (b) shows the simulated pitching moment coefficient polars at the quarter-chord location for both airfoils under fully turbulent and transitional flow regimes. The trends confirm the stronger nose-down moment for the HB-FB-350-125 airfoil, consistent with the pressure distribution characteristics.

Finally, it should be emphasized that the HB-FB-350-125 airfoil was designed with additional attention to structural performance. In particular, the skin second moment of area about the chordwise axis was increased, enhancing the structural stiffness of the airfoil.

In general, the same aerodynamic tendencies are observed across the entire set of optimized airfoils. Table B.6 summarizes the key aerodynamic and structural characteristics of both the DU97-W-XXX-YYY and HB-FB-XXX-YYY airfoil families. All aerodynamic properties were computed using Fluent under fully turbulent flow conditions at a Reynolds number of $Re = 6 \times 10^6$. The results demonstrate that the optimized airfoils attain higher maximum lift coefficients compared with the older DU97-W series. Furthermore, the optimized airfoils show higher stall angles of attack and higher design angles of attack. These differences become more pronounced as the maximum thickness increases, which is particularly advantageous for blade root applications. Wind turbine blades operate at relatively large geometric angles of attack in the root region. Achieving the desired design angle of attack typically requires a substantial twist near the root, which can introduce several structural challenges. By increasing the design angle of attack at the airfoil level, the required blade twist can be reduced, thereby mitigating some of these structural concerns.

Table B.6: Characteristics of the HB-FB-XXX-YYY and DU97-W-XXX-YYY flatback airfoils.

Airfoil	Maximum thickness (x/C)	Trailing edge thickness (t/C)	Design C_L	Maximum C_L (α°)	Maximum L/D (α°)	Solid I_{xx} (m^4)	Skin I_{xx}/t (m^3)
HB-FB-350-125	0.28	0.125	1.95	2.17 (17°)	31.52 (14°)	1.55e-3	3.27e-2
HB-FB-350-150	0.28	0.150	2.11	2.31 (18°)	28.83 (15°)	1.62e-3	3.41e-2
HB-FB-400-150	0.28	0.150	1.99	2.16 (17°)	29.62 (14°)	2.39e-3	4.46e-2
HB-FB-400-175	0.28	0.175	2.10	2.27 (18°)	27.33 (15°)	2.44e-3	4.56e-2
HB-FB-450-175	0.28	0.175	1.89	2.06 (16°)	26.90 (13°)	3.36e-3	5.71e-2
HB-FB-500-200	0.28	0.200	1.79	1.89 (14°)	25.75 (12°)	4.66e-3	7.22e-2
DU97-W-350-125	0.30	0.125	1.86	1.97 (15°)	31.81 (13°)	1.51e-3	3.24e-2
DU97-W-350-150	0.30	0.150	1.97	2.07 (16°)	29.31 (14°)	1.58e-3	3.38e-2
DU97-W-400-150	0.30	0.150	1.77	1.88 (14°)	29.43 (12°)	2.31e-3	4.38e-2
DU97-W-400-175	0.30	0.175	1.88	1.97 (15°)	27.83 (13°)	2.38e-3	4.49e-2
DU97-W-450-175	0.30	0.175	1.66	1.73 (13°)	25.93 (11°)	3.34e-3	5.68e-2
DU97-W-500-200	0.30	0.200	1.54	1.58 (11°)	22.61 (10°)	4.66e-3	7.18e-2

Additionally, the optimized airfoils provide a larger margin between the design and stall angles of attack when compared with the older airfoils. This expanded margin reduces the likelihood of stall under normal operating conditions.

It is also notable that the optimized airfoils possess higher design lift coefficients, which enhances aerodynamic performance in the blade root region where the aerodynamic lever arm is shorter. The lift-to-drag ratio remains of the same order-of-magnitude for both airfoil families, indicating no significant penalty in aerodynamic efficiency. From a structural standpoint, the optimized airfoils exhibit higher sectional moments of inertia than the older

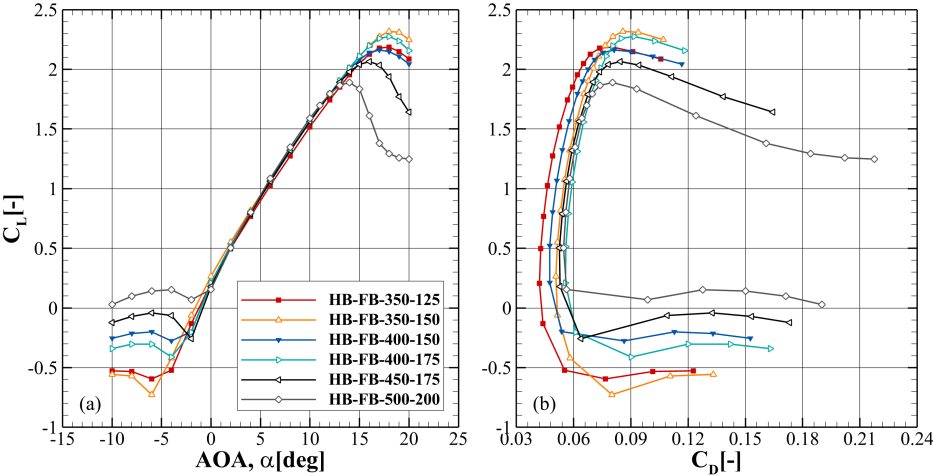


Figure B.26: Polar curves of the HB-FB-XXX-YYY flatback airfoils in turbulent flow regime, $[Re = 6 \times 10^6]$.

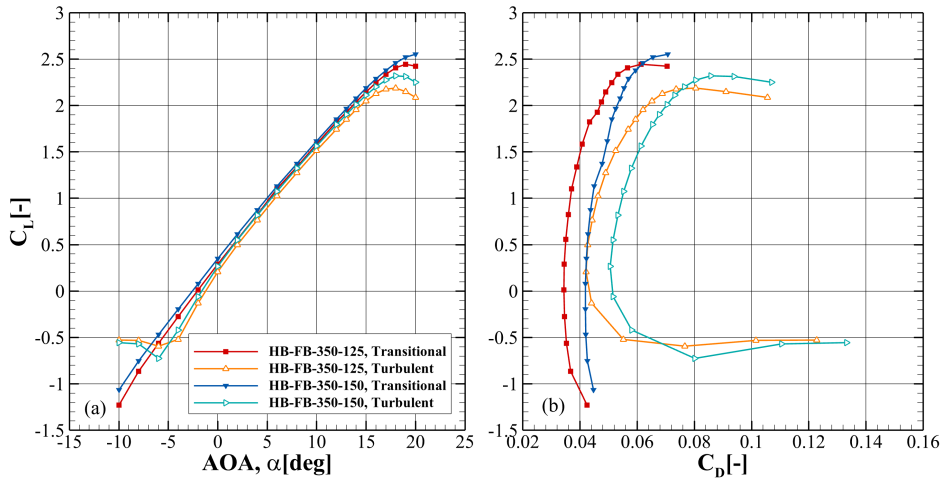


Figure B.27: Polar curves of the HB-350-125 and HB-350-150 airfoils, [$Re = 6 \times 10^6$].

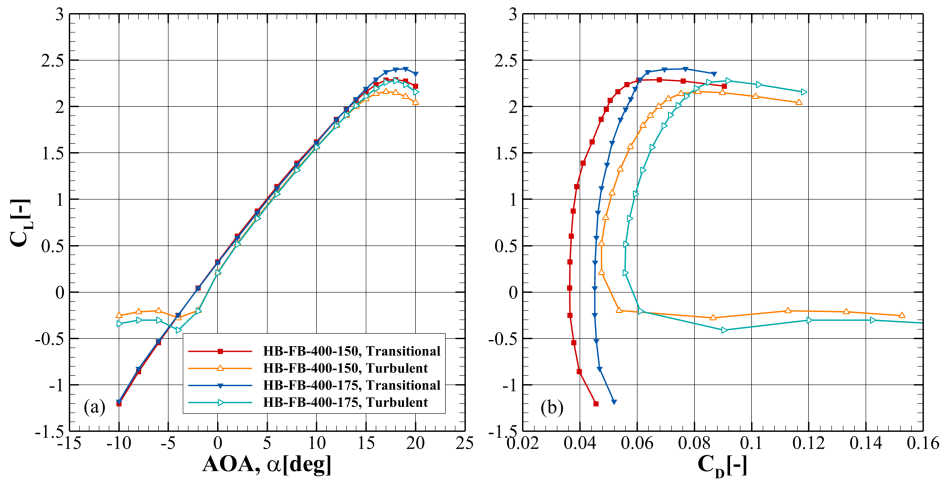


Figure B.28: Polar curves of the HB-400-150 and HB-400-175 airfoils, [$Re = 6 \times 10^6$].

DU97-W airfoils. Table B.6 further shows two consistent trends. First, for airfoils with the same maximum thickness, an increase in trailing edge thickness results in higher stiffness and higher design lift coefficients, although it also leads to a reduction in the maximum lift-to-drag ratio. Second, for airfoils with the same trailing edge thickness, increasing the maximum thickness reduces the maximum lift coefficient, the design lift coefficient, and the stall angle of attack.

Figure B.26 presents the simulated polar characteristics of the HB-FB-XXX-YYY flatback

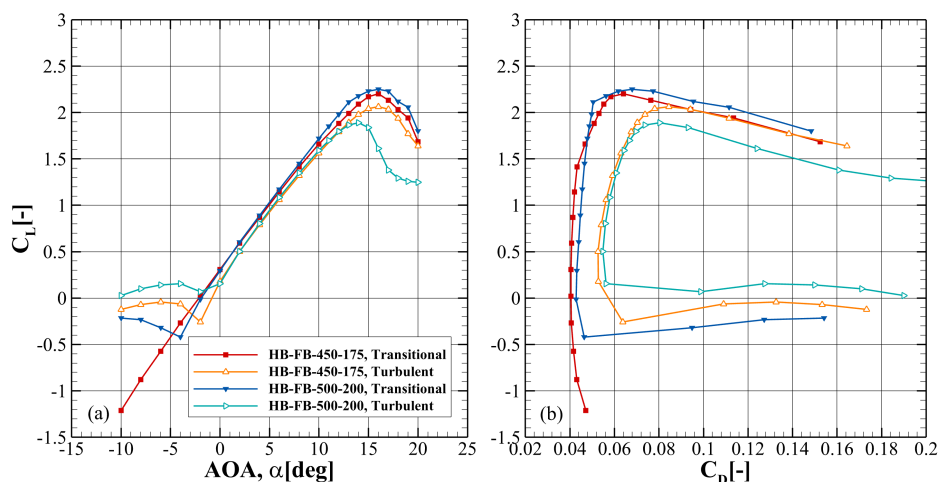


Figure B.29: Polar curves of the HB-450-175 and HB-500-200 airfoils, [$Re = 6 \times 10^6$].

airfoil family, covering maximum thickness values from 35% to 50% and several trailing edge thickness configurations, under fully turbulent flow conditions at a Reynolds number of $Re = 6 \times 10^6$. The results show a clear trend, i.e., increasing the trailing edge thickness leads to a higher maximum lift coefficient. In addition, both the positive and negative stall angles of attack increase as the trailing edge thickness becomes larger. Despite these changes, the lift curve slope remains essentially unaffected by the variation in trailing edge thickness. The C_L - C_D polars further reveal that the drag coefficient rises as the trailing edge thickness increases. This increase in drag is primarily attributable to the additional form drag associated with a larger blunt region at the trailing edge, which intensifies pressure drag and contributes to overall aerodynamic losses.

Figures B.27 through B.29 present the simulated polar characteristics of the HB-FB-XXX-YYY airfoil family for both natural transition and fully turbulent flow regimes at a Reynolds number of $Re = 6 \times 10^6$.

CONCLUSIONS

The primary objective of this part was to apply a multidisciplinary optimization approach for the design and refinement of flatback airfoils intended for the inboard region of modern large-scale multi-megawatt wind turbine blades. Six airfoil geometries were developed, spanning maximum thickness ranging from 35% to 50% and incorporating various trailing edge gaps. These airfoils were designed using two-dimensional Navier-Stokes CFD solvers, and the optimization procedure was carried out with a Simulated Annealing algorithm, a stochastic optimization technique often applied in combination with fuzzy-logic-based decision strategies. To assess the accuracy of the numerical predictions, an extensive wind

tunnel campaign was conducted, providing experimental validation of the aerodynamic performance.

The multidisciplinary optimization process demonstrated substantial aerodynamic and structural improvements in the flatback airfoil designs. The optimized airfoils consistently achieved higher maximum lift coefficients, larger stall angles of attack, higher lift-to-drag ratios, and increased design angles of attack compared with their non-optimized counterparts. Additionally, the optimized airfoils exhibited reduced negative stall lift coefficients, reduced negative stall angles of attack, diminished sensitivity to leading edge roughness, and higher sectional moments of inertia, characteristics that are especially advantageous for inboard blade performance and structural integrity.

CFD simulations further revealed clear trends associated with variations in trailing edge thickness for airfoils of fixed maximum thickness. As the trailing edge gap increased, the maximum lift coefficient, stall angle of attack in both positive and negative directions, airfoil stiffness, and drag coefficient all increased. In contrast, the lift-to-drag ratio decreased, while the lift curve slope remained essentially unchanged. These trends provide valuable insight into the aerodynamic and structural behavior of flatback airfoils and offer guidance for tailoring future designs to meet the aerodynamic and structural demands of large modern wind turbine blades.

B.4.3 QUASI-3D-FLOW SOLVER BASED OPTIMIZATION OF FLATBACK AIRFOILS

As previously discussed, the complex three-dimensional flow structures present near the blade root strongly influence the boundary layer development and the distribution of surface pressure around the airfoil. These effects contribute to an increase in the lift coefficient as well as a rise in the stall angle of attack. To examine how such three-dimensional flow phenomena influence the airfoil optimization process, all airfoil families in this section were optimized using a quasi-3D flow solver at a Reynolds number of $Re = 6 \times 10^6$. For this purpose, distinct chord-to-radius (C/r) ratios were assigned to each airfoil family. Specifically, C/r values of 0.1, 0.2, 0.3, and 0.4 were selected for the 35%, 40%, 45%, and 50% relative thickness configurations, respectively. These ratios correspond to the representative operating regions of each airfoil family within contemporary multi-megawatt wind turbine blades and reflect the typical spanwise distribution of geometric loading in the inboard region.

It is important to note that, in order to assess the potential of optimization in the presence of 3D-flow effects, and to allow for direct comparison with the previously optimized HB-FB-XXX-YYY airfoil series, which was refined using a 2D Navier–Stokes solver, the geometric constraints of the new optimized airfoils were preserved. Parameters such as overall thickness, trailing edge thickness, maximum thickness position, design angle of attack, design lift coefficient, and pitching moment characteristics were maintained to match those of the 2D-optimized reference family. This approach ensured that the influence of quasi-3D flow physics could be isolated and analyzed without altering the fundamental geometrical features of the baseline airfoils. The primary objective was to enhance the structural characteristics and aerodynamic performance at the design angle of attack while preserving the original constraint set to ensure consistency in the comparative assessment.

Table B.7: Geometrical parameters of the HB-FB-XXX-YYY-R flatback airfoil family.

Airfoil	Maximum thickness position (x/c)	Relative thickness [%]	Trailing edge thickness [%]
HB-FB-350-125-R	0.28	35	12.5
HB-FB-350-150-R	0.28	35	15.0
HB-FB-400-150-R	0.28	40	15.0
HB-FB-400-175-R	0.28	40	17.5
HB-FB-450-175-R	0.28	45	17.5
HB-FB-500-200-R	0.28	50	20.0

The HB-FB-XXX-YYY-R flatback airfoil family, composed of airfoils with varying maximum thickness and trailing edge bluntness, has been developed and refined for application in large variable-speed, variable-pitch multi-megawatt wind turbines. The airfoil series is organized into four principal thickness classes, i.e., 35%, 40%, 45%, and

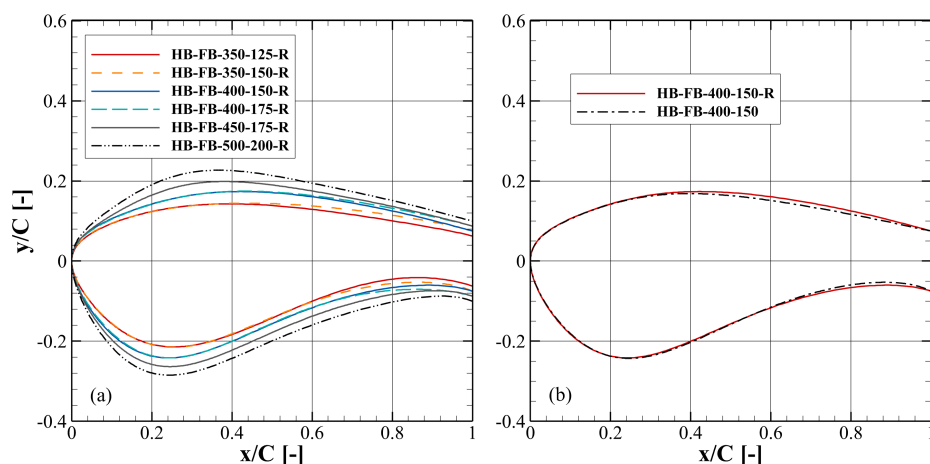


Figure B.30: Optimized flatback airfoils, [(a): HB-FB-XXX-YYY-R flatback airfoils, (b): Comparison between the HB-FB-400-150 and HB-FB-400-150-R airfoils].

50% relative thickness. For the 35% and 40% thickness families, two trailing edge gap configurations were defined to investigate the aerodynamic and structural consequences of different bluntness levels. In total, six flatback airfoils were generated and optimized. The final geometric characteristics for the airfoil family are summarized in table B.7.

The airfoil naming convention follows a systematic format in which the first numerical term specifies the relative thickness, while the second term represents the trailing edge bluntness. For the quasi-3D optimization stage, the suffix “-R” was added to the existing naming sequence to indicate that the geometry has been optimized while accounting for rotational effects. The overall classification and subdivision strategy of the airfoil family is visualized in figure B.30 (a).

Figure B.30 (b) provides a detailed comparison between the geometry of the 2D optimized HB-FB-400-150 airfoil and its quasi-3D optimized counterpart, the HB-FB-400-150-R. The comparison reveals that the 3D-optimized configuration exhibits a reduced back-camber and a more pronounced convex curvature within the pressure recovery region on the suction side, downstream of the maximum thickness location. These geometric adjustments alter the aft section flow behavior and reflect the influence of rotationally-induced three-dimensional effects captured by the quasi-3D optimization process. In contrast, the forward portion of the airfoil, i.e., from the leading edge to the maximum thickness chord position, shows minimal deviation from the geometry of the 2D optimized airfoil. This limited change is most likely a consequence of the imposed roughness insensitivity constraints, which restrict significant modifications to the leading edge region to preserve robustness under operational surface contamination.

The alterations in suction and pressure side curvature introduced in the 3D-optimized design yield beneficial structural outcomes. Due to the increased separation between the

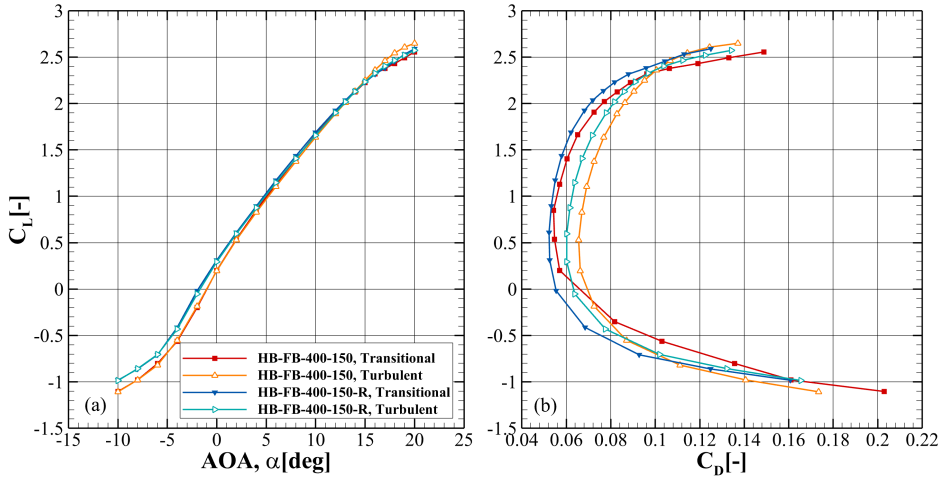


Figure B.31: Polar curves for the HB-FB-400-150 and HB-FB-400-150-R airfoils, [$Re = 6 \times 10^6$, $C/r = 0.2$].

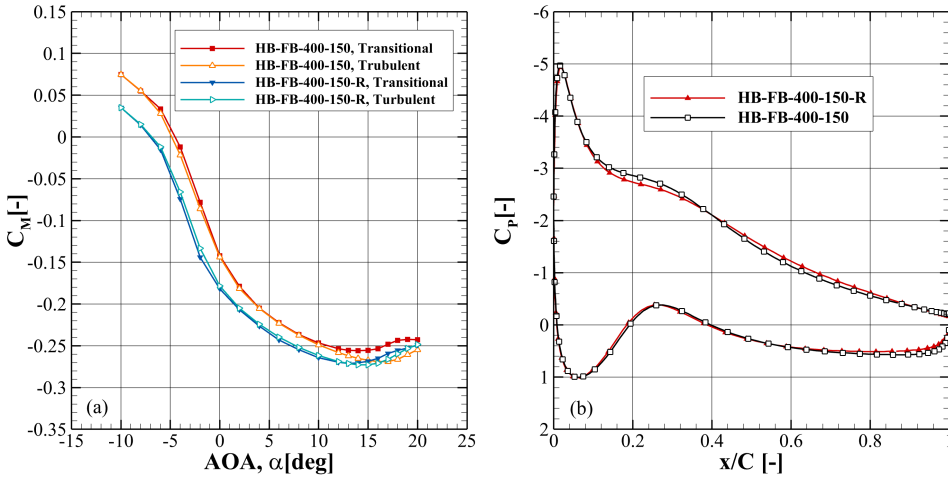


Figure B.32: Aerodynamic characteristics comparison between the HB-FB-400-150 and HB-FB-400-150-R airfoils, [$Re = 6 \times 10^6$, $C/r = 0.2$, (a): C_M , (b): C_P at $\alpha = 14^\circ$].

mass distribution and the neutral axis, both the solid and skin second moments of inertia of the HB-FB-400-150-R airfoil rise by 5.8% and 4.5%, respectively. This enhancement contributes to improved resistance against bending loads and results in a structurally more efficient airfoil while maintaining the aerodynamic characteristics required for inboard blade performance.

The simulated lift coefficient and lift-to-drag polar curves for both the HB-FB-400-150 and HB-FB-400-150-R airfoils at a chord-to-radius ratio of $C/r = 0.2$ under both turbulent and transitional flow conditions are shown in figure B.31. The results indicate that the two airfoils display nearly identical lift behavior over the mid to high range positive angles of attack, where the aerodynamic loading is dominated by attached or mildly separated flow. Despite this similarity, the maximum lift coefficient of the quasi-3D optimized HB-FB-400-150-R airfoil is marginally lower than that of the purely 2D optimized airfoil. This reduction can be attributed to geometric adjustments applied during the 3D-optimization process, which alter the aft-camber and pressure recovery characteristics.

At low and moderately negative angles of attack, the HB-FB-400-150-R airfoil displays a lower lift coefficient compared with the HB-FB-400-150 airfoil. This difference shows the influence of rotationally-induced flow physics, which affect suction side development and stall onset on the 3D-optimized airfoil. The distinction between the turbulent and transitional lift curves is minimal for both airfoils, indicating that the flow characteristics in this thickness class are relatively insensitive to the presence or absence of transitional boundary layer behavior over most of the operational angle of attack range.

A clear aerodynamic advantage of the HB-FB-400-150-R airfoil emerges in the drag characteristics. For a given lift coefficient, the 3D-optimized airfoil displays a lower drag coefficient than the 2D optimized counterpart. This improvement is linked to modifications to the pressure recovery region and reduced aft section losses, which collectively contribute to more favorable aerodynamic loading. As a consequence, the L/D ratio is higher across a broad range of angles of attack, demonstrating a more aerodynamically efficient airfoil design suited for the inboard region of large wind turbine blades.

Figure B.32 (a) compares the simulated pitching moment coefficient curves for both the HB-FB-400-150 and HB-FB-400-150-R airfoils at the quarter-chord location for $C/r = 0.2$ and a Reynolds number of $Re = 6 \times 10^6$. The results show that the 3D-optimized airfoil attains a slightly higher pitching moment coefficient over a broad interval of angles of attack. This difference is modest but systematic, implying that the aft loading modifications, which resulted improved structural properties lead to a minor increase in nose-down pitching moment. The distinction between turbulent and transitional predictions remains small for both airfoils, indicating that the moment behavior is relatively insensitive to boundary layer transition effects within the examined operating range. Similar tendencies have been observed for the other airfoils in the optimized family, confirming that this behavior is consistent across the entire set of airfoils.

Figure B.32 (b) presents the pressure coefficient distributions for the HB-FB-400-150 and HB-FB-400-150-R airfoils at a design angle of attack of 14° and a chord-to-radius ratio of $C/r = 0.2$. A clear difference is observed in the aft region of both surfaces. The quasi-3D optimized HB-FB-400-150-R airfoil displays lower pressure levels on the pressure side near the trailing edge when compared with the 2D optimized configuration. In contrast, the suction side of the 3D-optimized airfoil shows a higher negative pressure within the pressure recovery region, particularly downstream of the maximum thickness chord

Table B.8: Comparison of the characteristics of both flatback airfoil families, $[Re = 6 \times 10^6]$.

Airfoil	Design C_L (α°)	Design L/D (α°)	Design C_M (α°)	C/r [-]	Solid I_{XX} (m^4)	Skin I_{XX}/t (m^3)
HB-FB-350-125	2.01 (14°)	29.41 (14°)	0.21 (14°)	0.1	1.55e-03	3.27e-02
HB-FB-350-150	2.16 (15°)	26.81 (15°)	0.23 (15°)	0.1	1.62e-03	3.41e-02
HB-FB-400-150	2.13 (14°)	23.57 (14°)	0.26 (14°)	0.2	2.39e-03	4.46e-02
HB-FB-400-175	2.25 (15°)	20.74 (15°)	0.27 (15°)	0.2	2.44e-03	4.56e-02
HB-FB-450-175	2.23 (14°)	18.01 (14°)	0.31 (14°)	0.3	3.36e-03	5.71e-02
HB-FB-500-200	2.94 (17°)	16.95 (17°)	0.39 (17°)	0.4	4.66e-03	7.22e-02
HB-FB-350-125-R	2.01 (14°)	30.90 (14°)	0.21 (14°)	0.1	1.61e-03	3.38e-02
HB-FB-350-150-R	2.16 (15°)	28.08 (15°)	0.23 (15°)	0.1	1.70e-03	3.54e-02
HB-FB-400-150-R	2.13 (14°)	24.74 (14°)	0.26 (14°)	0.2	2.53e-03	4.66e-02
HB-FB-400-175-R	2.25 (15°)	22.16 (15°)	0.27 (15°)	0.2	2.57e-03	4.74e-02
HB-FB-450-175-R	2.23 (14°)	18.73 (14°)	0.31 (14°)	0.3	3.47e-03	5.84e-02
HB-FB-500-200-R	2.93 (17°)	17.30 (17°)	0.39 (17°)	0.4	4.77e-03	7.35e-02

location. Despite these changes in pressure distribution, the produced lift coefficient at the design angle of attack remains practically unchanged, consistent with the previously discussed lift curve results. This shows that the geometric adjustments applied during the quasi-3D optimization process shift the pressure loading distribution without altering the overall lift generation at the design condition.

Table B.8 summarizes the structural and aerodynamic properties of the airfoil families optimized through 2D and quasi-3D methodologies. The aerodynamic characteristics were computed with the quasi-3D solver under fully turbulent conditions at a Reynolds number

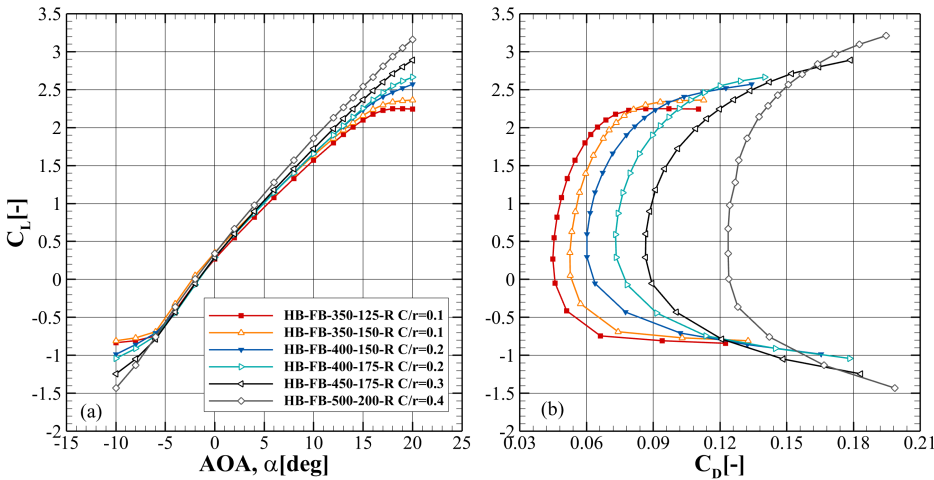


Figure B.33: Polar curves of HB-FB-XXX-YYY-R flatback airfoils, $[Re = 6 \times 10^6]$.

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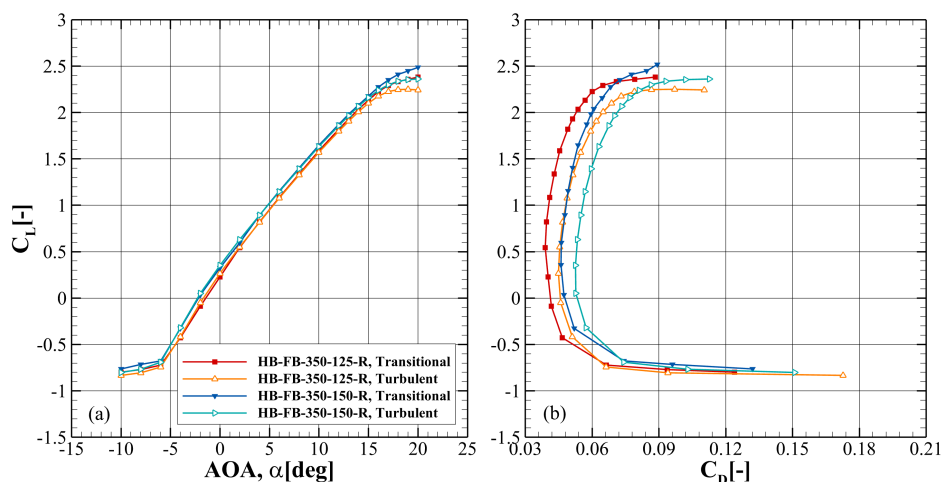


Figure B.34: Polar curves of HB-FB-350-125-R and HB-FB-350-150-R airfoils, [$Re = 6 \times 10^6$, $C/r = 0.1$].

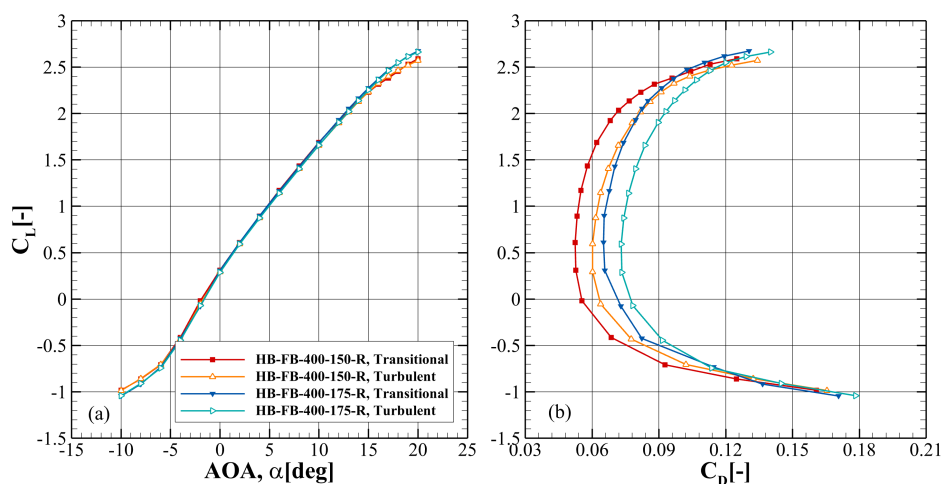


Figure B.35: Polar curves of HB-FB-400-150-R and HB-FB-400-175-R airfoils, [$Re = 6 \times 10^6$, $C/r = 0.2$].

of $Re = 6 \times 10^6$. The comparison demonstrates that both airfoil families generate the same design lift level, confirming that the design point performance requirement is met by each optimization strategy. However, the airfoils refined through the quasi-3D approach show clear improvements in structural capability, as well as a higher lift-to-drag ratio across the design range, which points toward a more efficient aerodynamic configuration. The results

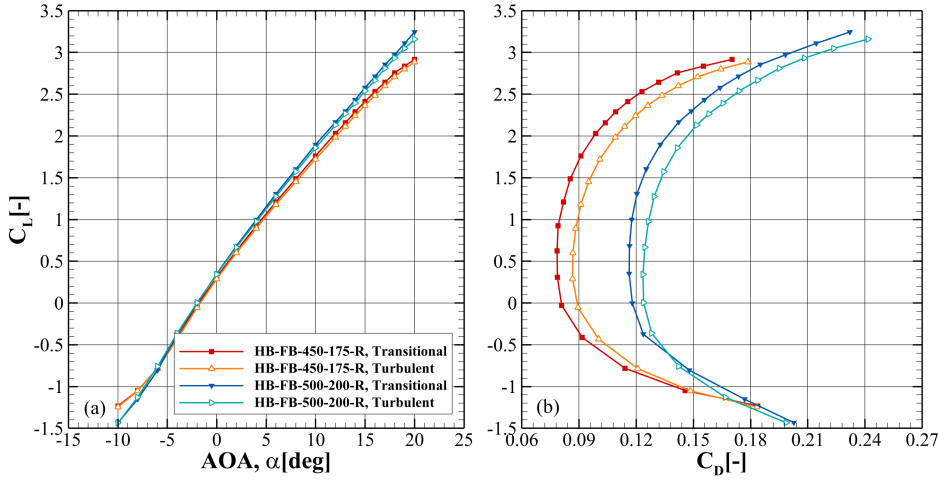


Figure B.36: Polar curves of HB-FB-450-175-R ($C/r = 0.3$) and HB-FB-500-200-R ($C/r = 0.4$) airfoils, [$Re = 6 \times 10^6$].

further reveal systematic trends within the airfoil families. For designs, sharing the same relative thickness, the design lift coefficient, design angle of attack, design pitching moment coefficient, and sectional stiffness all rise as the trailing edge thickness increases. At the same time, the lift-to-drag ratio declines due to increased pressure losses and enhanced form drag near the blunt trailing edge. The observed increase in design angle of attack with increasing relative thickness is a valuable aerodynamic characteristic for the design of the blade root region. A higher design angle of attack reduces the twist requirement near the root, which in turn can lead to lower blade mass and reduced manufacturing cost. These trends demonstrate that the quasi-3D optimization process enhances both aerodynamic efficiency and structural robustness, while also supporting blade designs that achieve reduced geometric complexity and improved manufacturability in the heavily loaded inboard region of modern wind turbines.

Figure B.33 presents the simulated polar characteristics of the HB-FB-XXX-YYY-R flatback airfoil family, covering maximum thickness values from 35% to 50%, and multiple trailing edge thickness configurations. These results were obtained under fully turbulent flow conditions at a Reynolds number of $Re = 6 \times 10^6$ and for several chord-to-radius ratios. The aerodynamic trends reveal that, for a fixed relative thickness and constant C/r value, an increase in trailing edge thickness leads to higher drag levels across the operational range. In addition, the lift curve slope at high angles of attack becomes steeper as the trailing edge thickness is increased. The data also show that increasing the C/r ratio consistently raises the stall angle of attack, even when the relative thickness becomes larger. This behavior is advantageous for the blade root region, where the local angle of attack can increase rapidly due to the comparatively low rotational speed close to the hub. A higher stall margin in this part of the blade contributes to improved aerodynamic

reliability under off design loading conditions and reduces the likelihood of premature stall onset in regions governed by strong three-dimensional flow effects.

Figures B.34 through B.36 provide the simulated polar characteristics of the HB-FB-XXX-YYY-R airfoil family for both natural transition and fully turbulent flow regimes at a Reynolds number of $Re = 6 \times 10^6$.

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CONCLUSIONS

The primary objective of this study was to investigate the influence of 3D-rotational effects appearing near the root region of wind turbine blades on the multidisciplinary optimization of thick flatback airfoils. To accomplish this, a new HB-FB-R flatback airfoil family was designed and analyzed. This family consists of six distinct airfoil sections with relative thickness values ranging from 35% to 50% each with a different trailing edge gap. A quasi-3D dimensional Navier-Stokes computational fluid dynamics solver was employed to generate the airfoil shapes. The model demonstrated very good agreement with the NREL Phase VI measurements, predicting stall delay behavior of the S809 airfoil across several radial positions along the blade. The optimization process relied on the Simulated Annealing method combined with fuzzy logic procedures, and multiple aerodynamic and structural objective functions were considered simultaneously to balance performance and robustness.

The optimized airfoil family, designed based on quasi-3D effects, demonstrates substantially improved sectional stiffness as well as a higher lift-to-drag ratio compared with the earlier airfoil family optimized under standard two-dimensional flow conditions. Comparisons among the quasi-3D optimized designs show that increasing trailing edge thickness while maintaining constant relative thickness and a fixed C/r ratio leads to higher drag and a steeper lift curve slope at elevated angles of attack. Furthermore, larger C/r ratios consistently raise the stall angle of attack, even when relative thickness is increased. This trend is advantageous for blades with large chord lengths near the root, where the local angle of attack rises due to the reduced rotational speed.

A comparison of the aerodynamic polars between the two airfoil families reveals that the influence of 3D-optimization is most pronounced at the design angle of attack and at higher angles of attack. At these conditions, radial flow modifies the suction side pressure distribution, allowing the optimizer to adapt the aft geometry to the true operating behavior of root region airfoils. At lower angles of attack, the flow remains largely attached, and therefore the discrepancy between two-dimensional and three-dimensional predictions becomes minor. For this reason, the geometry of the new family shows its greatest differences downstream of the maximum thickness location, particularly on the suction side where radial flow and strong three-dimensional effects dominate.

With adequate computational capacity, the quasi-3D stall delay model developed here

can be used for optimization of thick airfoils equipped with various passive or active flow control concepts such as vortex generators, trailing edge splitters, boundary layer suction, or blowing systems. Moreover, higher fidelity turbulence models such as the Reynolds Stress Model, Scale Adaptive Simulation, or Detached Eddy Simulation can be integrated with the quasi-3D approach to further enhance the predictive capability of the flow solver, especially when such add on devices are employed.

In conclusion, the findings of this study highlight the critical importance of three-dimensional flow physics in the design and optimization of flatback airfoils for the blade root region. Based on the results, it is recommended that each airfoil section be optimized at its corresponding design C/r ratio to achieve the highest possible aerodynamic and structural performance and ultimately improve the overall efficiency and reliability of modern wind turbine blades.

B.4.4 AERODYNAMICS OF FLATBACK AIRFOILS IN 2D AND 3D-ROTATIONALLY AUGMENTED FLOWS

In this section, the aerodynamics of flatback airfoils are examined. The first part presents a review of wind tunnel experiments and the 2D aerodynamic characteristics of flatback airfoils. The second part investigates the aerodynamic behavior of flatback airfoils under 3D-rotational effects.

Figure B.37 presents a comparison between the measured and simulated pressure coefficient distributions for the HB-FB-350-150 flatback airfoil at a constant lift coefficient for both clean and tripped surface conditions. In both cases, the pressure distributions correspond to angles of attack close to 8° , where the measured lift coefficients were 1.32 for the clean configuration and 1.29 for the tripped configuration. Overall, the comparisons demonstrate a strong level of agreement between the experimental data and the CFD results, with only a minor deviation in the trailing edge region. The numerical simulations capture the peak suction and pressure levels on the airfoil surfaces with high accuracy. Moreover, the results indicate that the location of the laminar separation bubble on the pressure side is predicted reliably, as illustrated in figure B.37 (a). The bubble location appears as a distinct jump in the pressure distribution at approximately $x/c = 0.35$. Figure B.38 shows the laminar separation bubble on the pressure side of the clean airfoil at an angle of attack of 8° based on surface flow visualization. It should be noted that the two discontinuities observed in the pressure distribution plots in figure B.37 (b) on both the suction and pressure sides, result from the presence of the trip strips used during the measurements.

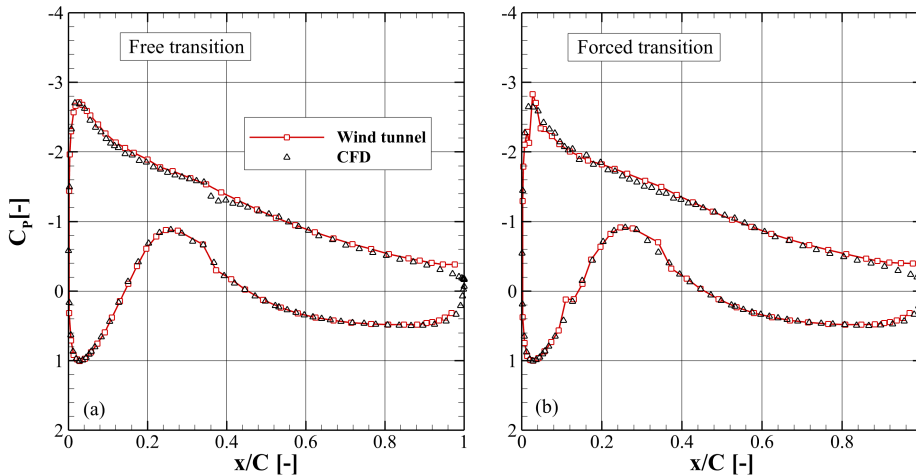


Figure B.37: Pressure coefficient distribution comparison, $[Re = 0.95 \times 10^6, \alpha = 8^\circ]$.

The pressure distribution results in figure B.37 also indicate an important aerodynamic

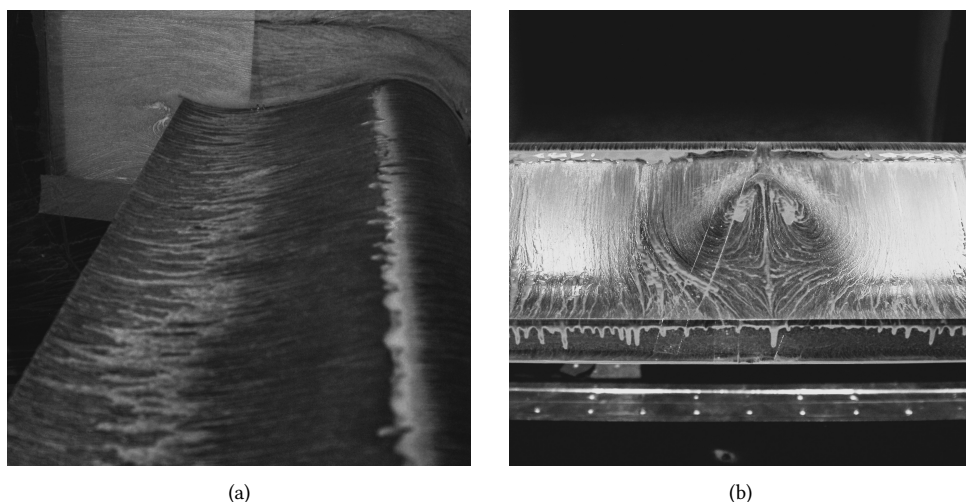


Figure B.38: Surface flow visualization on the pressure side in free transition flow regime, [$Re = 0.95 \times 10^6$, (a): $\alpha = 8^\circ$, (b): $\alpha = 0^\circ$].

characteristic of flatback airfoils. In contrast to sharp trailing edge airfoils, the pressure recovery on both the suction and pressure sides occurs predominantly in the trailing edge region for both clean and tripped conditions. This leads to comparatively high pressure gradients near the trailing edge. Such behavior is advantageous for flatback airfoils, as they maintain the low pressure region on the suction side and the elevated pressure on the pressure side up to the trailing edge, which contributes to a higher overall lift coefficient. Furthermore, the upward shift in the suction side pressure coefficient near the trailing edge reduces the magnitude of the adverse pressure gradient, when compared to sharp trailing edge airfoils for the same relative thickness. This reduction decreases the likelihood of boundary layer separation at higher angles of attack, thereby improving the aerodynamic stability of the airfoil.

The comparisons between the corrected measured and predicted lift coefficients for the HB-FB-350-150 flatback airfoil under clean and tripped conditions at the Reynolds number of $Re = 0.95 \times 10^6$ are presented in figure B.39 (a). The results show a strong correspondence between the measured and simulated lift coefficients within the linear region of the lift curve for both flow conditions. However, the CFD results overpredict the stall angle of attack for both clean and tripped configurations. This behavior is common when a two-equation $k-\omega$ turbulence model is applied, as this model often delays flow separation and produces a more gradual stall onset compared to experimental observations. A noticeable discrepancy between the measured and simulated results at 0° angle of attack is observed for the free transition flow condition. This difference arises from the flow state on the pressure side at this angle. Surface flow visualization revealed the formation of a spade-shaped pattern on the pressure side near mid-span. This feature indicates a localized

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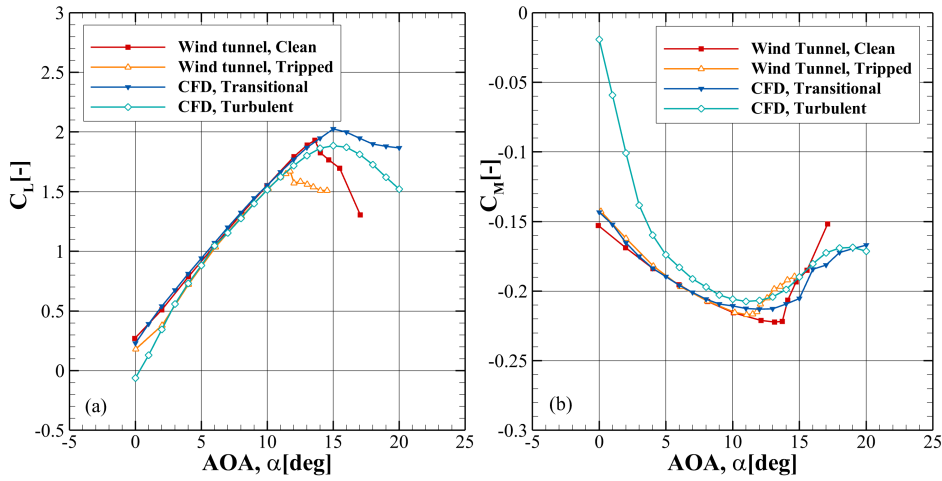


Figure B.39: The comparisons between the measured and simulated polars, [$Re = 0.95 \times 10^6$, (a): C_L , (b): C_M].

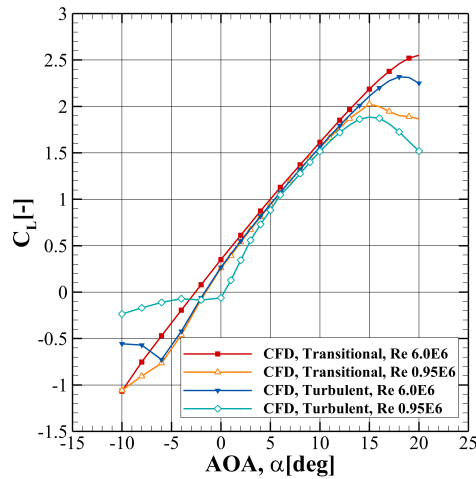


Figure B.40: The effect of the Reynolds number on the lift coefficient polars.

instability that develops before the onset of stall and alters the pressure distribution in a way that is not fully reproduced by the CFD model. Figure B.38 (b) illustrates the surface flow pattern at 0° angle of attack for the clean airfoil, providing direct evidence of this instability.

A comparison of the corrected measured lift coefficients for the HB-FB-350-150 flatback

airfoil under clean and tripped surface conditions demonstrates that the airfoil undergoes a gradual and well-behaved stall in both cases. A smooth stall pattern is valuable for reducing unsteady structural loading under gusty or rapidly varying wind conditions, which are frequently encountered in wind turbine operation. The results reveal only minor differences in the magnitude of the lift coefficient between the free and forced transition flow regimes and approximately a 2° reduction in the stall angle for the tripped configuration. This outcome confirms that the airfoil maintains a low sensitivity to leading edge roughness, a central design objective during its development. In addition, the comparatively high stall angle delivered by this flatback airfoil provides a comfortable margin between the design angle of attack and the onset of stall. Such a margin is desirable for blade regions between the root and the mid-span, where operational angles vary and structural requirements are demanding. A small separation between design and stall angles can lead to operational issues, because even modest fluctuations in angle of attack, which is common in turbulent inflow conditions, may trigger premature stall. Although this issue can be mitigated through the use of vortex generators, these devices introduce extra drag and require periodic maintenance due to wear. Despite the overall robustness of the airfoil, the earlier stall observed for the tripped case remains visible in figure B.39 (a).

Comparisons between the corrected measured and simulated pitching moment coefficient polars at the Reynolds number of $Re = 0.95 \times 10^6$ for clean and tripped conditions are shown in figure B.39 (b). The results indicate good agreement within the mid-range of angles of attack for the clean airfoil. Deviations appear near 0° and close to the stall region, which originate from the delayed separation predicted by the RANS approach employed in the CFD model. For the tripped configuration, the accuracy of the simulations decreases further, a trend also reported in [166]. The pronounced pitching moment levels at high angles of attack prior to stall arise from the rear-loaded nature of the flatback airfoil. The additional camber on the pressure side near the trailing edge behaves in a manner similar to a small flap, exerting a downward force on the airfoil nose and increasing the magnitude of the negative pitching moment. A comparable effect occurs in sharp trailed airfoils fitted with Gurney flaps, where a modification near the trailing edge increases the pitching moment strength through a similar aerodynamic mechanism.

Figure B.40 presents the simulated lift coefficient polars for transitional and fully turbulent flow conditions at Reynolds numbers of $Re = 0.95 \times 10^6$ and $Re = 6 \times 10^6$. The results demonstrate that the airfoil shows a strong dependence on Reynolds number. For both natural transition and fully turbulent simulations, the stall angle shifts by approximately 3° . Despite this change in stall behavior, the lift coefficients within the linear portion of the polar remain very close for the two Reynolds numbers and for both turbulence treatments. This shows that the airfoil retains stable aerodynamic behavior in the pre-stall region while experiencing noticeable modifications in post-stall characteristics as Reynolds number increases.

NEAR-WAKE CHARACTERIZATION OF A FLATBACK AIRFOIL USING PARTICLE IMAGE VELOCIMETRY

Figures B.41, B.42, and B.43 present the measured mean, RMS, and instantaneous velocity contours together with the corresponding streamline patterns for angles of attack of $\alpha = 8^\circ$, $\alpha = 12^\circ$, and $\alpha = 15.5^\circ$ obtained in the wind tunnel in free transition flow regime. Note that, the small-scale fluctuations visible beneath the pressure side originate from incomplete illumination in that region, caused by limited laser sheet coverage beginning at the sharp trailing edge corner and extending toward the lower-left region of each frame.

At $\alpha = 8^\circ$ and $\alpha = 12^\circ$, two recirculating regions appear within the separation bubble behind the trailing edge. The primary recirculation forms immediately downstream of the suction side and rotates clockwise. Below this structure lies a secondary recirculation rotating in the opposite direction, positioned closer to the pressure side. A comparison between the three angles shows that the separation bubble size changes only moderately between $\alpha = 8^\circ$ and $\alpha = 12^\circ$. At $\alpha = 15.5^\circ$; however, stall produces a substantial modification, i.e., the primary recirculation migrates upward above the suction side while enlarging and deforming, extending further toward the leading edge as the angle of attack rises. The secondary recirculation also grows, spanning nearly the full height of the trailing edge region, and its vortex core shifts toward the suction side due to the strong adverse pressure gradient that develops near the trailing edge.

The mean velocity contours, normalized by the freestream velocity, reveal large velocity gradients within the shear layer that separates the recirculating zone from the external flow. A distinct low velocity region within the separation bubble is also evident.

The normalized RMS velocity contours highlight the unsteady behavior in the wake. The shear layer appears clearly as the zone of elevated RMS values downstream of the blunt trailing edge. The center of this layer corresponds to the region of maximum RMS velocity. At $\alpha = 8^\circ$ and $\alpha = 12^\circ$, two RMS peaks are visible, originating from the shear layers formed by the primary and secondary recirculating regions. A slight increase in the length of the separated region with angle of attack is discernible. The shear layer tied to the secondary recirculation is larger than that of the primary recirculation, which can be attributed to geometric differences between the suction and pressure sides pressure recovery mechanism near the trailing edge. The pressure side behaves similarly to a small flap, influencing the structure of the separation region. In the mean velocity fields, the shear layer corresponds to the zone where the streamwise velocity increases from near-zero values to the outer flow, while in the RMS fields it appears as the area of heightened fluctuations. Near stall, the shear layer adopts a pronounced sickle-shaped form, caused by deformation of the recirculation zones and the deformation of the primary and secondary recirculation regions and expansion of the separated bubble behind the airfoil. Across all pre-stall angles, the peak RMS level remains near 0.45 for both recirculation regions. As the airfoil approaches stall, the peak RMS decreases to roughly 0.40, signaling a weakening of the shear layer. Downstream of the local RMS maxima, the RMS intensity decreases steadily for all cases.

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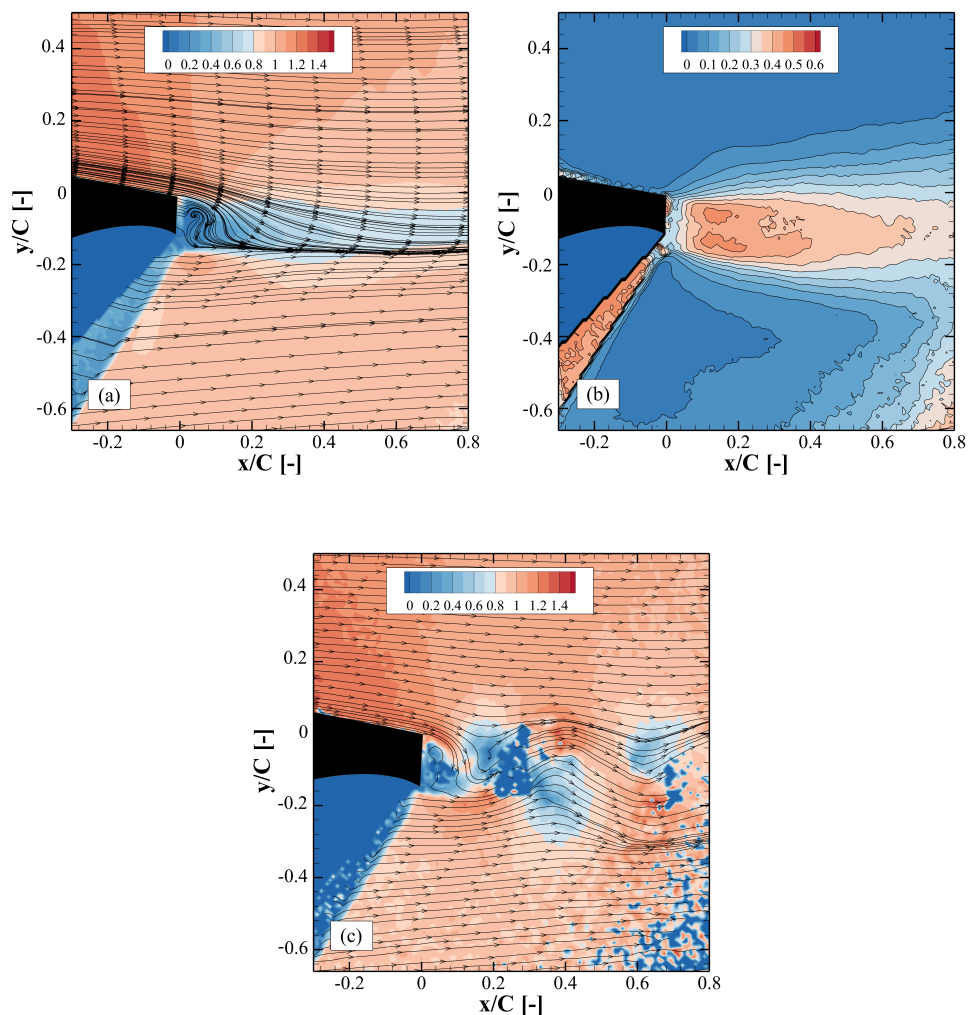


Figure B.41: PIV measurement contours, [$Re = 0.95 \times 10^6$, $\alpha = 8^\circ$, (a): $\overline{v_X}/V$, (b): $v_{x,RMS}/V$, (c): v_x/V].

The normalized instantaneous velocity contours depict, at $\alpha = 8^\circ$, the secondary recirculating region is prominent, drifting slowly downstream. A small vortex near the suction side reveals the formation of the primary recirculation. At $\alpha = 12^\circ$, both recirculating regions are clearly present. Two low velocity structures downstream of the trailing edge appear at both angles, generated by turbulent vortex shedding. At $\alpha = 15.5^\circ$, three recirculating regions are detected, i.e., the primary vortex above the suction side, a large separated vortex positioned downstream of it, and a smaller secondary recirculation located at the trailing

edge on the pressure side. Two additional, weaker vortices farther downstream correspond to decayed remnants of the primary and secondary vortices. At this angle of attack, the separation bubble, identified by the extended low velocity region, is considerably larger than at lower angles.

The flow accelerates from the stagnation point toward both the suction and pressure sides. Depending on the Reynolds number, transition to turbulence occurs at different streamwise locations, governed either by a laminar separation bubble at low Reynolds

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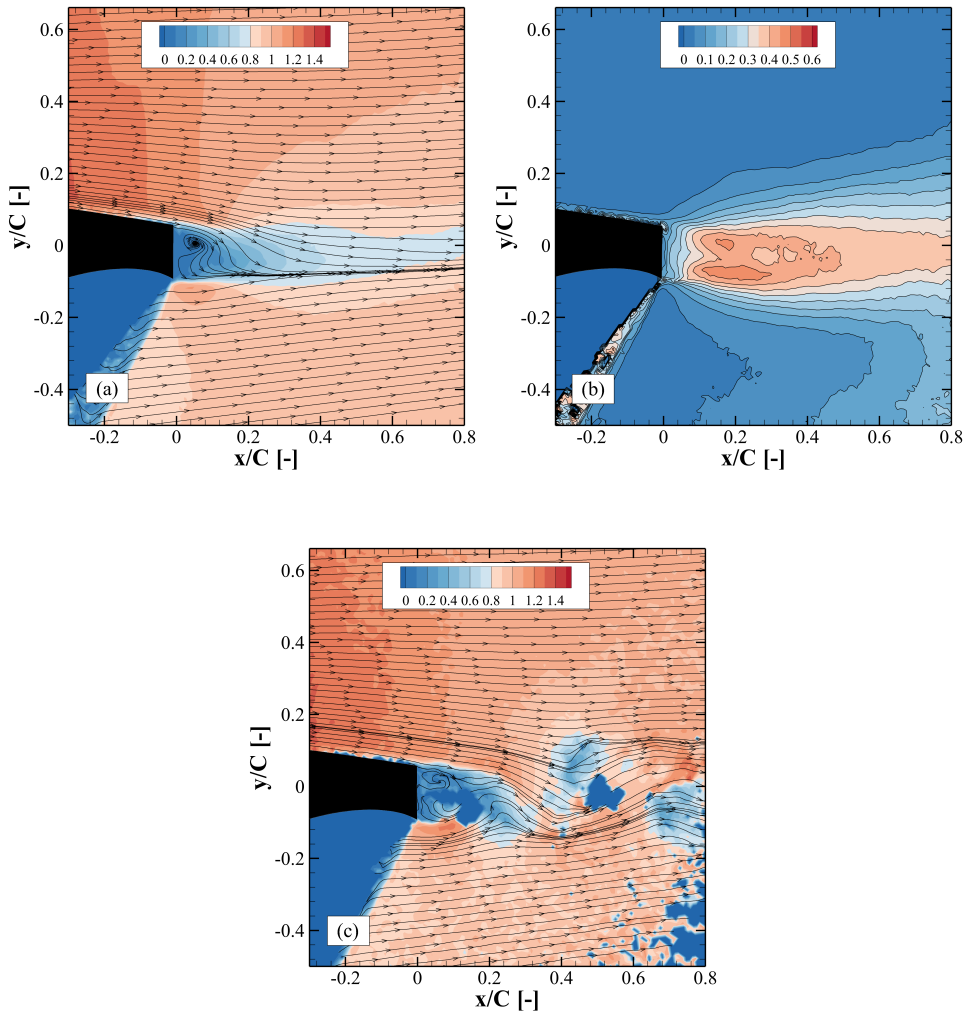


Figure B.42: PIV measurement contours, [$Re = 0.95 \times 10^6$, $\alpha = 12^\circ$, (a): $\overline{v_X}/V$, (b): $v_{x,RMS}/V$, (c): v_x/V].

numbers or by natural transition at higher Reynolds numbers. The turbulent boundary layer eventually separates at the trailing edge due to the strong adverse pressure gradient, generating a shear layer between the separated region and the high velocity external flow. Final pressure recovery takes place in this region on both sides of the airfoil. Vortical motion arising near the trailing edge corners interacts with the faster external flow and mixes it with the slower flow within the separated region. This separated zone is inherently unsteady, with both size and shape varying over time. At low angles of attack, the mixing

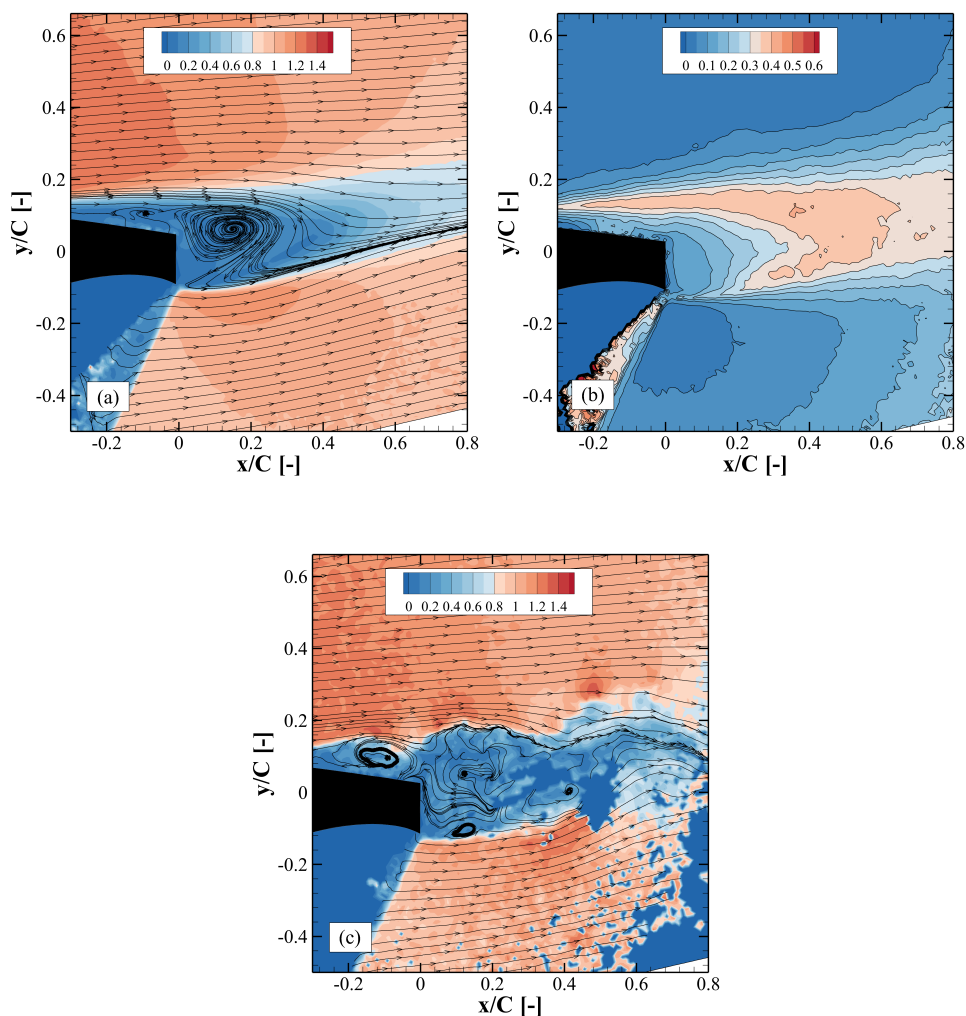


Figure B.43: PIV measurement contours, $[Re = 0.95 \times 10^6, \alpha = 15.5^\circ]$, (a): $\overline{v_x}/V$, (b): $v_{x,RMS}/V$, (c): v_x/V .

is sufficient to allow pressure recovery at the trailing edge, keeping the suction side flow attached. At higher angles of attack, however, the separated bubble introduces substantial unsteadiness into the turbulent boundary layer on the suction side, eventually driving a secondary separation that enlarges with increasing angle of attack and ultimately causes stall.

3D-EFFECTS ON THE AERODYNAMICS OF FLATBACK AIRFOILS

In this section, the three-dimensional aerodynamic characteristics of the flatback airfoils are examined. Figure B.44 presents the surface skin friction coefficient of the HB-FB-350-150-R airfoil for chord-to-radius ratios (C/r) of 0.1, 0.2, and 0.3 at a Reynolds number of $Re = 6 \times 10^6$ and an angle of attack of $\alpha = 8^\circ$ under natural transition condition. The results were obtained using a quasi-3D simulation method. The skin friction distributions reveal a modest increase in the friction coefficient on both the suction and pressure sides as the C/r ratio increases. This trend becomes more evident toward the trailing edge area, where 3D-effects intensify the velocity gradients and thereby increase the wall shear stress. Despite the rise in skin friction with higher C/r ratios, the transition location remains effectively unchanged. The transition point can be identified by the rapid increase in skin friction level that marks the onset of turbulence, as the turbulent boundary layer generates significantly higher shear stress compared to the laminar regime.

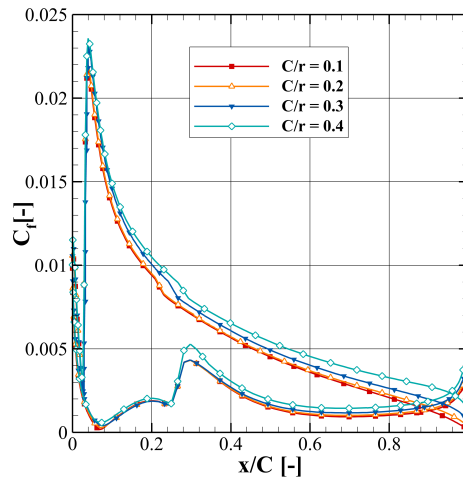


Figure B.44: Skin friction coefficient distribution for HB-FB-350-150-R airfoil, [$Re = 6 \times 10^6$, $\alpha = 8^\circ$].

Figure B.45 depicts the normalized radial velocity v_r/V contour, for the HB-FB-350-150-R airfoil at the Reynolds number of $Re = 6 \times 10^6$ and four C/r ratios of 0.1, 0.2, 0.3, and 0.4 at an angle of attack of $\alpha = 15^\circ$, obtained from quasi-3D simulations. As discussed earlier, 3D-effects exert their strongest influence on regions where the flow is already separated. Because a separation bubble forms downstream of the blunt trailing edge of flatback airfoils, the 3D-flow environment drives the separated region radially toward the blade tip. The magnitude of this radial motion rises steadily with increasing C/r ratio. As it clear from figures, 3D forces affect not only the separated bubble, but also the boundary layer around the airfoil particularly for C/r ratio above 0.3. This radial transport reduces the static pressure near the trailing edge region and consequently contributes to an increase in the overall drag of the airfoil. The mechanism resembles a progressive intensification of momentum redistribution near the trailing edge, which becomes more pronounced as the C/r ratio grows.

The outcome of this radial motion behind the flat trailing edge surface is further illustrated in figure B.46, which shows the drag coefficients calculated for the flat region of the trailing edge of the HB-FB-350-150-R airfoil. These values are normalized by the airfoil length and reported for C/r ratios of 0.1, 0.2, and 0.3 at $\alpha = 15^\circ$ angle of attack, based on quasi-3D simulations. The results demonstrate that, for fixed angle of attack and Reynolds number, the drag coefficient of the trailing edge region rises progressively with increasing C/r ratio. It is also important to note that at this angle of attack the flow remains attached to the suction side for all examined cases, meaning that the drag increase originates primarily from 3D-flow effects in the wake and not from suction side separation.

To study the flow behavior around flatback airfoils with higher precision, a set of DES CFD simulations was performed for the HB-FB-350-150 flatback airfoil. A detailed understanding of the flow field, particularly in the near-wake region, is essential for further refinement of flatback airfoil geometries, especially for applications where drag/noise reduction and wake management are critical. Conventional RANS turbulence models rely on empirical closures that use statistical assumptions to characterize turbulence length and time scales. Due to their statistical nature, these models struggle to provide accurate predictions of unsteady separated flows, as documented in earlier investigations [252]. This limitation becomes more pronounced in regions dominated by large-scale vortex shedding and complex wake dynamics.

In contrast, LES methods resolve the energetic, large-scale turbulent structures directly while modeling only the smaller motions that fall below the grid resolution through sub-grid scale models [253]. As a result, LES provides a predicted flow field that aligns much more closely with physical reality than what can be achieved through traditional RANS closures. This capability makes LES particularly suitable for studying separated flows, wake structures, and unsteady aerodynamic phenomena. However, LES requires extremely fine grid resolution, especially at high Reynolds numbers where the smallest turbulent scales decrease significantly. Consequently, the computational cost becomes prohibitive for most practical aerodynamic configurations. A practical alternative is offered

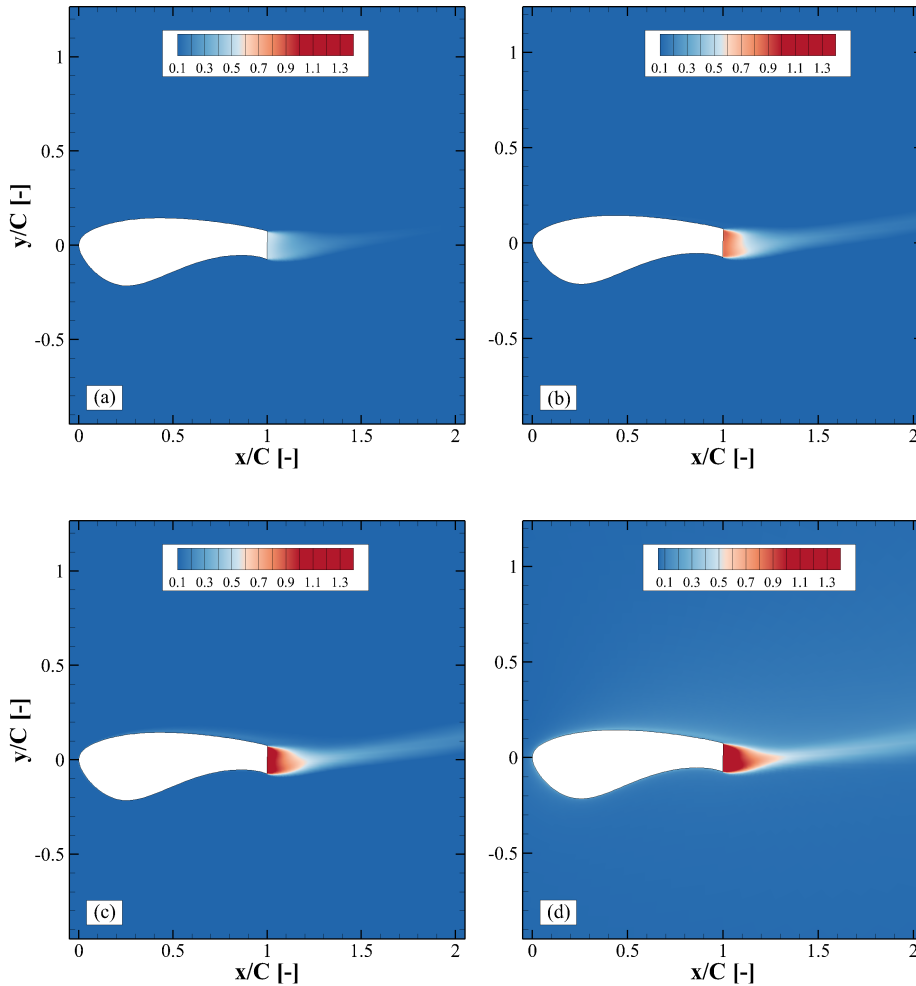


Figure B.45: v_r/V Contour for the HB-FB-350-150-R airfoil, [$Re = 6 \times 10^6$, $\alpha = 15^\circ$, (a): $C/r = 0.1$, (b): $C/r = 0.2$, (c): $C/r = 0.3$, (d): $C/r = 0.4$].

by hybrid RANS-LES approaches such as DES. In DES, the RANS formulation is applied within near-wall and attached flow regions, where turbulence structures are small and predominantly influenced by viscosity. Away from the surface, particularly in separated regions where large-scale turbulent motions dominate, an LES formulation is activated together with an appropriate sub-grid scale model. This combination allows DES to capture essential unsteady flow physics with far fewer computational resources than a full LES, while maintaining a significantly higher level of fidelity compared to pure RANS. In the present study, the $k-\omega$ SST turbulence model serves as the underlying RANS closure within the DES methodology.

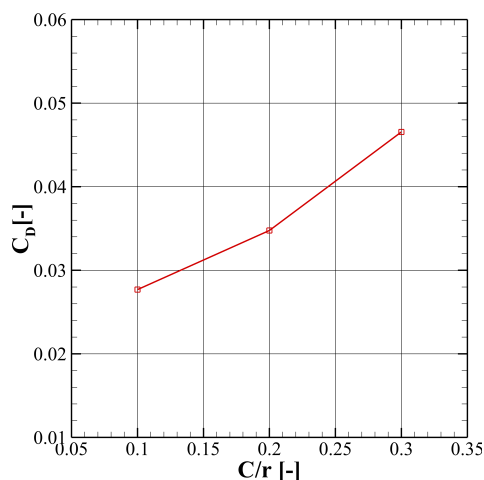


Figure B.46: Drag coefficient of the trailing edge flat area for HB-FB-350-150-R airfoil, [$Re = 6 \times 10^6$, $\alpha = 15^\circ$].

To assess the accuracy of the DES methodology, a comprehensive set of CFD simulations was performed for the HB-FB-350-150 flatback airfoil at angles of attack of $\alpha = 4^\circ$ and $\alpha = 8^\circ$ at a Reynolds number of $Re = 0.95 \times 10^6$. The simulations covered steady RANS, unsteady RANS, and DES approaches, and the computed results were compared directly to the corresponding wind tunnel measurements. For the DES calculations, the bounded central-differencing scheme was applied to the discretization of the momentum equations, while a second-order implicit transient scheme was employed for both unsteady RANS and DES computations. All unsteady simulations used a non-dimensional time step of $\Delta t V/C = 0.005$, and the magnitudes of unsteady fluctuations in aerodynamic coefficients were quantified through their Root-Mean-Square (RMS) values. Table B.9 provides a comparison of the aerodynamic forces, unsteady coefficient fluctuations, and Strouhal numbers obtained

Table B.9: Comparison between the mean measured and simulated aerodynamic coefficients for the HB-FB-350-150 flatback airfoil at $\alpha = 4^\circ$, $\alpha = 8^\circ$, and $Re = 0.95 \times 10^6$.

α [°]	Measurement Method	C_L [-]	C_D [-]	$C_{L,RMS}$ [-]	$C_{D,RMS}$ [-]	Strouhal Number [-]
4	Wind tunnel (Tripped)	0.718	–	–	–	–
4	$k-\omega$ (SST) Steady	0.705	0.0528	–	–	–
4	$k-\omega$ (SST) Unsteady	0.723	0.0573	0.0389	0.00418	0.19
4	DES	0.752	0.0797	0.1012	0.0153	0.20
8	Wind tunnel (Tripped)	1.129	–	–	–	–
8	$k-\omega$ (SST) Steady	1.126	0.0547	–	–	–
8	$k-\omega$ (SST) Unsteady	1.130	0.0597	0.0421	0.00501	0.19
8	DES	1.133	0.0824	0.1227	0.01801	0.21

from wind tunnel data and the various steady and unsteady CFD approaches. The time-averaged lift and drag coefficients were extracted from the unsteady solutions to create a consistent basis for comparison with the experimental data.

The results show a generally good agreement between the measured and predicted lift coefficients. For both angles of attack, the DES computations yield slightly higher lift values than the wind tunnel measurements; however, the deviations remain below 5%, which demonstrates reasonable precision for this class of simulation. The behavior of the drag coefficient shows a different pattern. The drag values computed with the $k-\omega$ SST turbulence model under steady and unsteady conditions remain very close to each other, yet they deviate substantially from those obtained using DES. A similar trend is observed in the unsteady fluctuations of both lift and drag coefficients, compared with the SST model.

Barone et al. [234] measured the trailing edge vortex shedding frequency for a flatback variant of the DU97-W-300 airfoil and reported a Strouhal number of 0.24 based on the trailing edge thickness. Their study also showed that this Strouhal number remained largely insensitive to Reynolds number, angle of attack, and surface tripping. In contrast, the present simulations predict lower Strouhal numbers for all turbulence treatments considered. Among the CFD approaches, DES produces values closest to the measurements, yet the discrepancy remains noticeable. Similar behavior was reported by Stone et al. [165], who observed consistent differences between steady RANS, unsteady RANS, DES, and LES simulations in both aerodynamic coefficients and unsteady frequency content. Their findings emphasized the difficulty of capturing the correct shedding frequency and near-wake dynamics with RANS-based methods, even when hybrid approaches are applied.

To investigate the wake dynamics of the flatback airfoils in greater detail, a series of DES computations was carried out for the HB-FB-350-150-R airfoil at a Reynolds number of $Re = 6 \times 10^6$. The same mesh resolution and non-dimensional time step described previously were applied to ensure consistency across the simulations. Figure B.47 illustrates the temporal development of the instantaneous iso-surface of the normalized Q -criterion at an angle of attack of $\alpha = 8^\circ$. Each snapshot is taken with respect to the dimensionless time of $t^* = 200\Delta t$, capturing the evolution of the dominant vortical structures near and downstream of the trailing edge.

In figure B.47 (a), vortex A begins to form along the suction side edge, while vortex B grows in size and moves downstream of the trailing edge, on the pressure side. By the time step shown in figure B.47 (b), vortex A has strengthened further, and vortex B has moved farther downstream. Their continued downstream progression is clearly visible in figures B.47 (c) and (d). The sequence of images reveals a persistent pattern of turbulent vortex shedding at this high Reynolds number. The periodic nature of this shedding process is of particular importance for blade structural design, as the shedding frequency can excite structural modes and may lead to resonance if not properly accounted for.

Figure B.48 (a) and (b) display three-dimensional views of the flow fields corresponding to figures B.47 (a) and (b). These visualizations show that vortices form simultaneously along the entire span of the airfoil, originating from both the suction side and pressure

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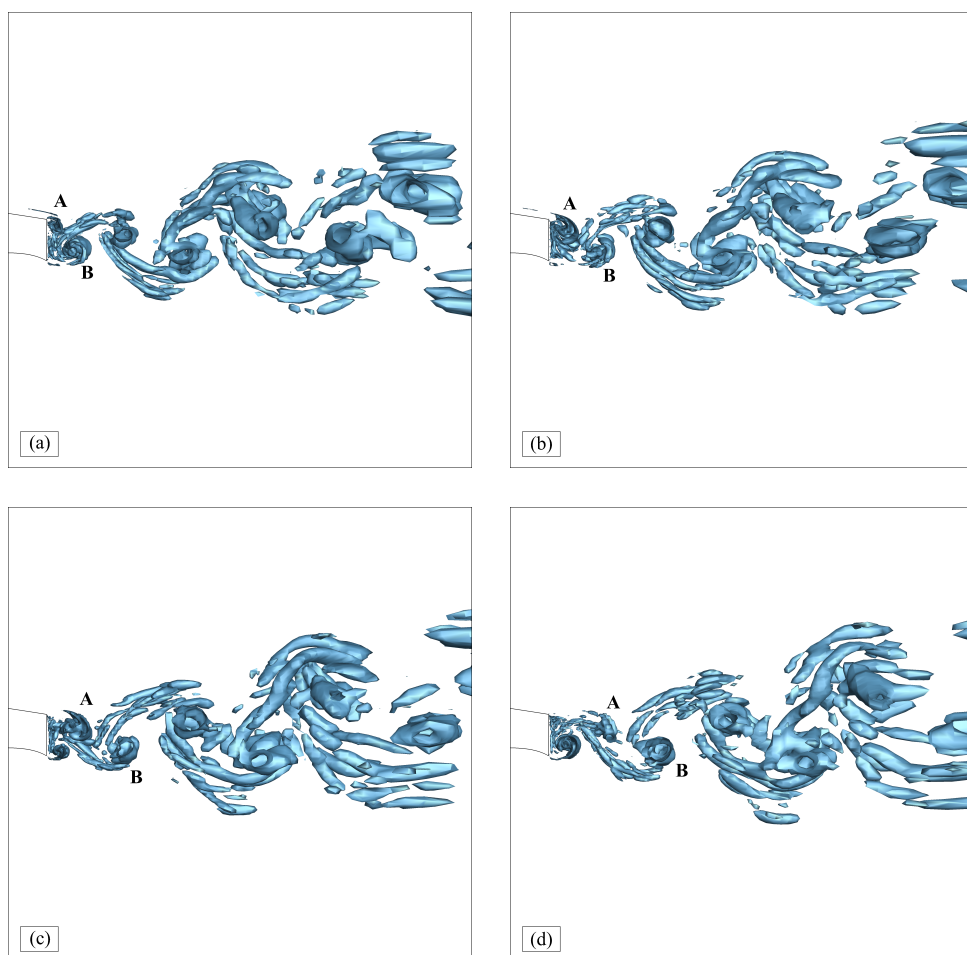


Figure B.47: Instantaneous normalized Q-criterion, [$Re = 6 \times 10^6$, $\alpha = 8^\circ$, $Q = 0.25$, (a): $t = 0 \times t^*$, (b): $t = 1 \times t^*$, (c): $t = 2 \times t^*$, (d): $t = 3 \times t^*$].

side edges of the blunt trailing edge. In addition to the primary vortices, large-scale coherent structures are observed farther downstream, demonstrating the complex and highly three-dimensional character of the wake produced by the flatback geometry.

The normalized vorticity magnitude contours for the corresponding time steps at the mid-span plane are presented in figure B.49. A comparison between these contours and the iso-surface visualizations shown earlier in figure B.47 reveals how high-energy, large-scale vortical structures develop near the blunt trailing edge of the airfoil and progressively decay as they convect downstream. This behavior is consistent with the classical Kolmogorov energy-cascade description, in which kinetic energy is transferred from larger turbulent structures to progressively smaller scales. During vortex stretching, the length of a vortex

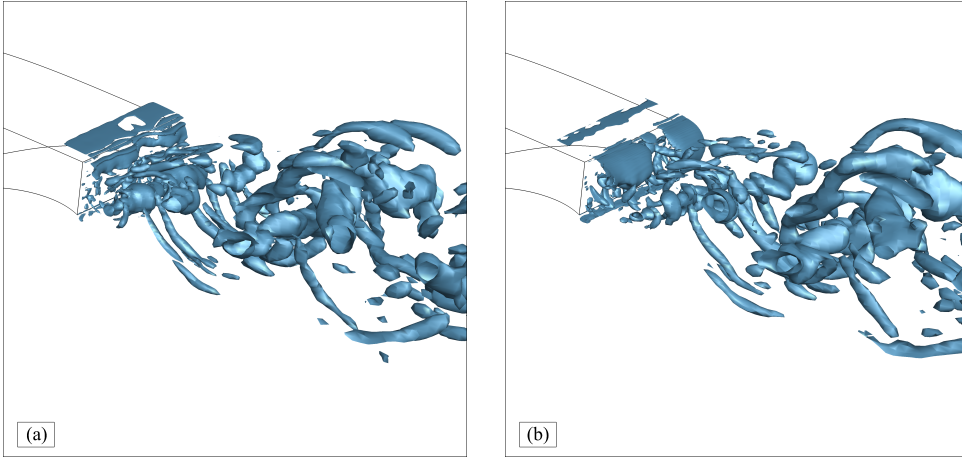


Figure B.48: 3D view of instantaneous iso-surface of normalized Q -criterion, [$Re = 6 \times 10^6$, $\alpha = 8^\circ$, $Q = 0.25$, (a): $t = 0 \times t^*$, (b): $t = 1 \times t^*$].

filament increases, and due to the volumetric conservation of the fluid element (as implied by the continuity equation), its radial dimension decreases. This reduction in radial scale intensifies the vorticity and accelerates the breakdown of larger coherent structures into smaller eddies. The cascade proceeds until the kinetic energy of the smallest scales is dissipated through viscous action and ultimately converted into heat.

The contours clearly show that the highest energy vortices originate near the trailing edge with characteristic length scales comparable to the trailing edge thickness. These energetic structures represent a dominant mechanism behind aeroacoustic noise production, particularly for flatback airfoils, where the blunt trailing edge strengthens vortex shedding. Therefore, effective drag or noise mitigation measures should focus on this region, where the generation of high intensity vortices is most prominent. Furthermore, the DES results indicate that these energetic structures persist over a substantial downstream distance, extending up to approximately 50% of the chord length beyond the trailing edge. This persistence indicates the highly unsteady and three-dimensional nature of the wake and highlights the importance of accurate modeling when assessing noise generation and potential interaction with downstream blade sections.

To investigate the wake behavior of the airfoil under the influence of rotational effects, a series of DES simulations was carried out for the HB-FB-350-150-R airfoil at an angle of attack of $\alpha = 8^\circ$ and a Reynolds number of 6.0×10^6 . The computations were performed for three different C/r ratios of 0.1, 0.2, and 0.3 in order to capture how the relative chord-to-radius ratio affects the three-dimensional wake structures. Figure B.50 presents instantaneous iso-surfaces of the normalized Q -criterion with a value of 0.25, colored by the normalized mean radial (spanwise) velocity, for the three C/r ratios. The results demonstrate that the radial component of the flow increases systematically as the C/r

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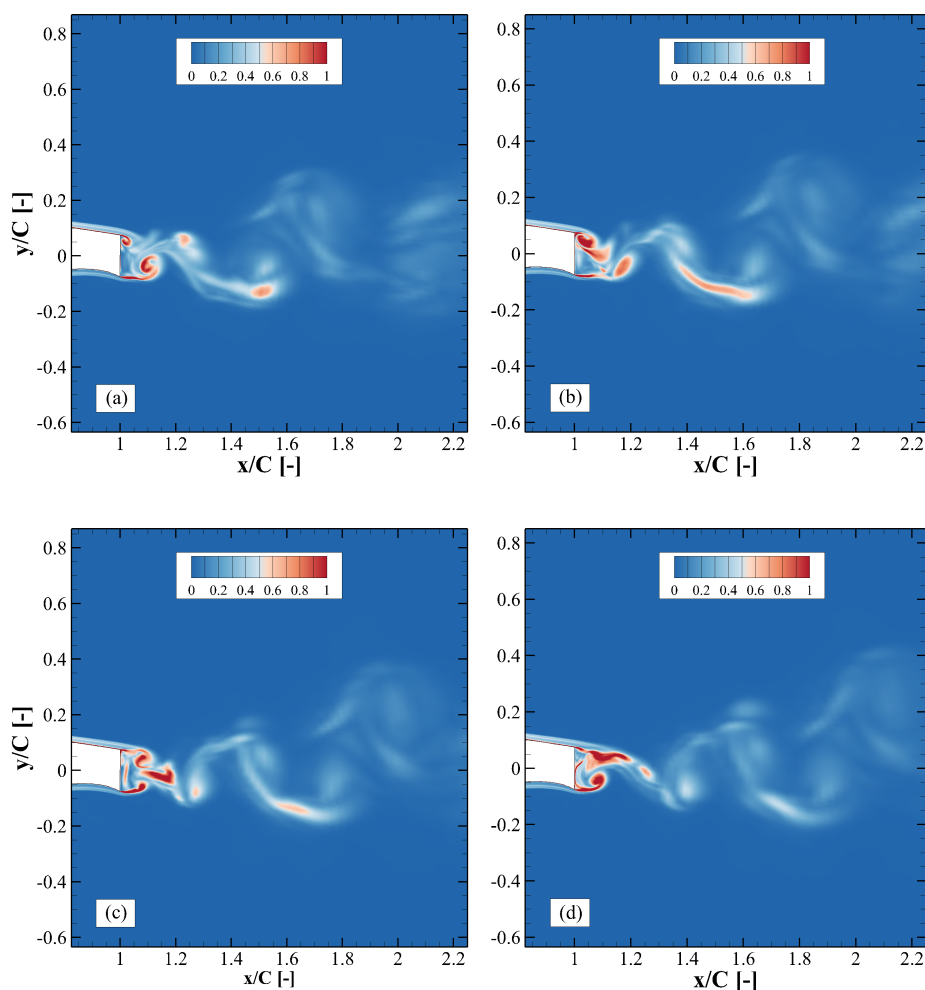


Figure B.49: Instantaneous normalized vorticity strength contour, [$Re = 6 \times 10^6$, $\alpha = 8^\circ$, (a): $t = 0 \times t^*$, (b): $t = 1 \times t^*$, (c): $t = 2 \times t^*$, (d): $t = 3 \times t^*$].

ratio becomes larger. In particular, the strongest radial motion develops very close to the trailing edge surface, i.e., early stage of the vortex formation as clearly visible in the side view. This intensified radial motion is a primary contributor to the rise in drag coefficient observed at higher C/r ratios. The mechanism is linked to the deflection of separated flow into the radial direction, which alters the distribution of momentum in the wake and amplifies the pressure deficit behind the trailing edge. Given this behavior, it would be advantageous to implement devices that suppress or redirect the radial flow emerging from the blunt trailing edge. Potential approaches include placing thin plates or slender flexible rods, similar to a brush configuration, to weaken the radial transport. Another option

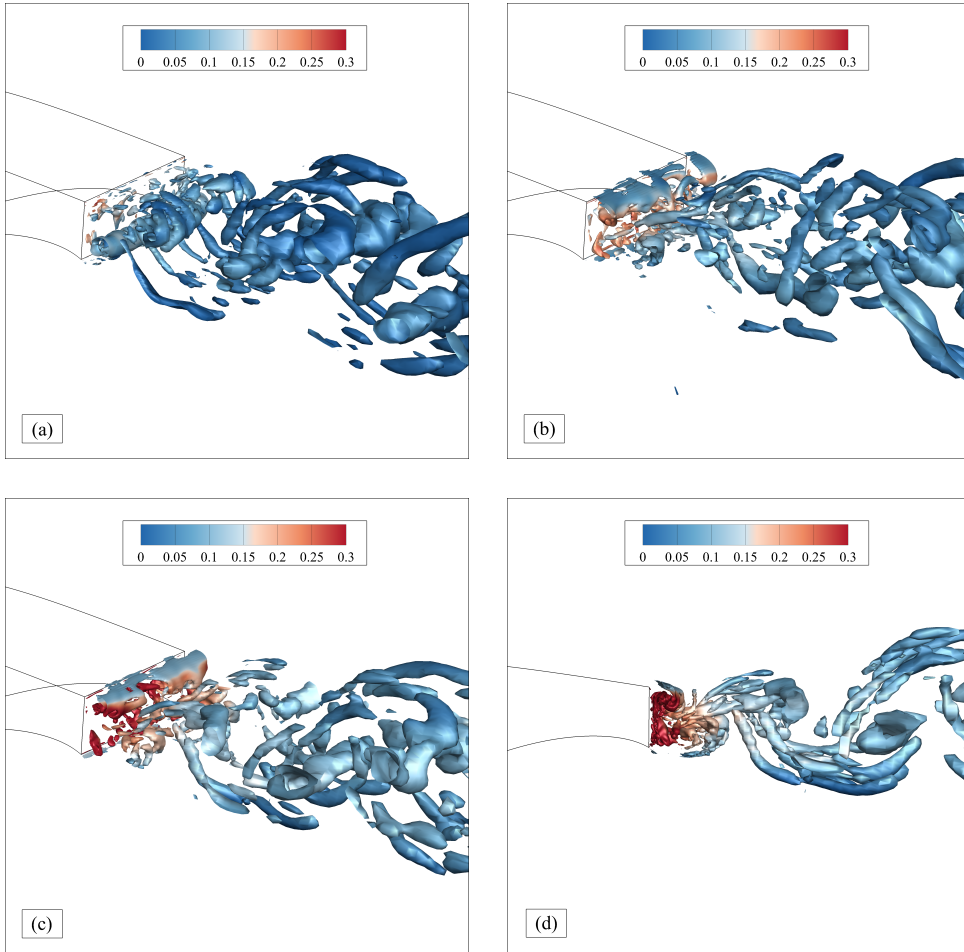


Figure B.50: Instantaneous iso-surface of normalized Q -criterion and v_r/V contour for HB-FB-350-150-R airfoil, [$Re = 6 \times 10^6$, $\alpha = 8^\circ$, $Q = 0.25$, (a): $C/r = 0.1$, (b): $C/r = 0.2$, (c): $C/r = 0.3$, (d): $C/r = 0.3$].

is a guide vane elements that guides the radial motion toward the chordwise direction, thereby mitigating the increase in drag. Such concepts could be tested numerically or experimentally to assess their effectiveness in controlling the radial flow and improving the aerodynamic performance of flatback sections operating under rotational effects.

Figure B.51 shows the normalized RMS value of the streamwise velocity component ($v_{s,RMS}/V$) at mid-span for the HB-FB-350-150-R airfoil at a Reynolds number of $Re = 6 \times 10^6$ and an angle of attack of $\alpha = 8^\circ$, for the three C/r ratios of 0.1, 0.2, and 0.3. The contour plots reveal a pattern that closely resembles the PIV measurements obtained for the non-rotating configuration. As observed in the experiments, two distinct peaks in the streamwise velocity

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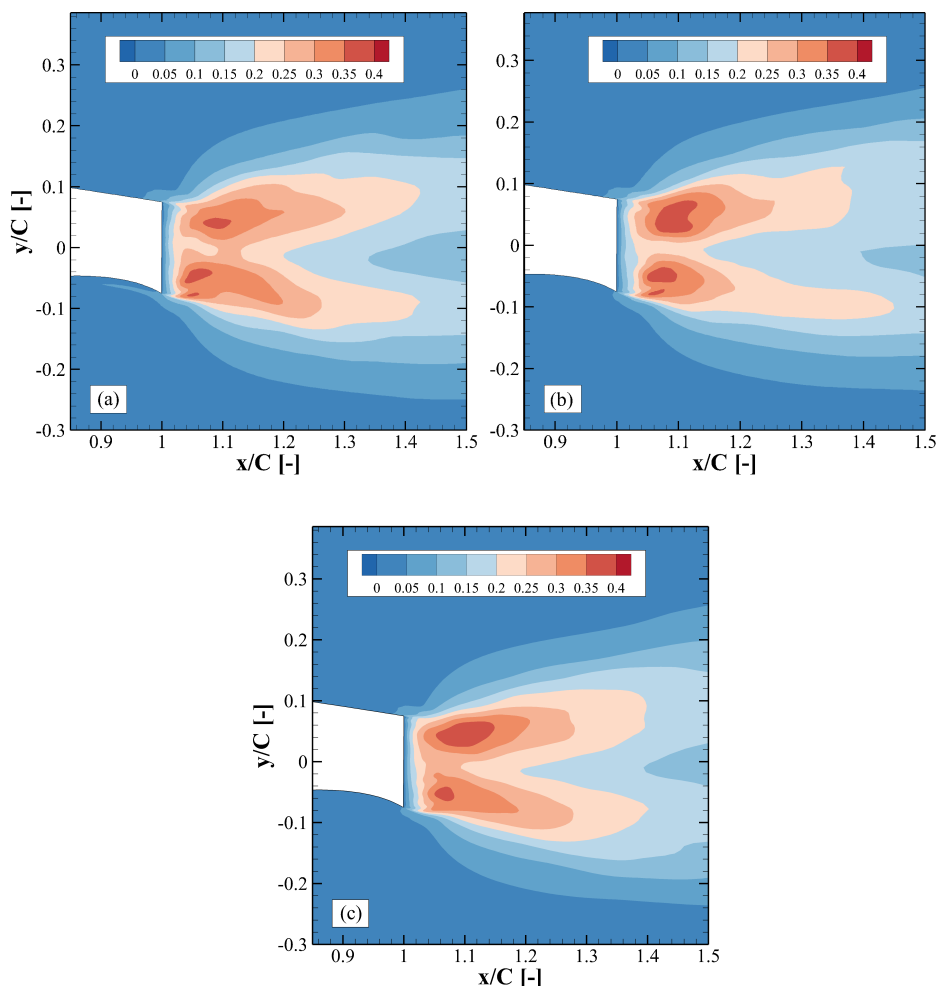


Figure B.51: Normalized $v_{s,RMS}/V$ contour for HB-FB-350-150-R airfoil, $[Re = 6 \times 10^6, \alpha = 8^\circ]$, (a): $C/r = 0.1$, (b): $C/r = 0.2$, (c): $C/r = 0.3$.

fluctuations appear near the trailing edge. These peaks correspond to the shear layers formed by the primary and secondary recirculation regions in the wake of the blunt trailing edge. The presence of two RMS peaks with magnitudes comparable to those obtained from the PIV tests indicates that the fundamental wake dynamics remain similar even when rotational effects are present. This outcome implies that the rotational motion does not significantly influence the streamwise turbulence intensity in the wake. The turbulent vortex shedding process persists under rotation, and the structure of the unsteady flow remains dominated by shear layer instabilities originating at the trailing edge. Furthermore, the results show no significant effect in the streamwise velocity fluctuation levels as the C/r

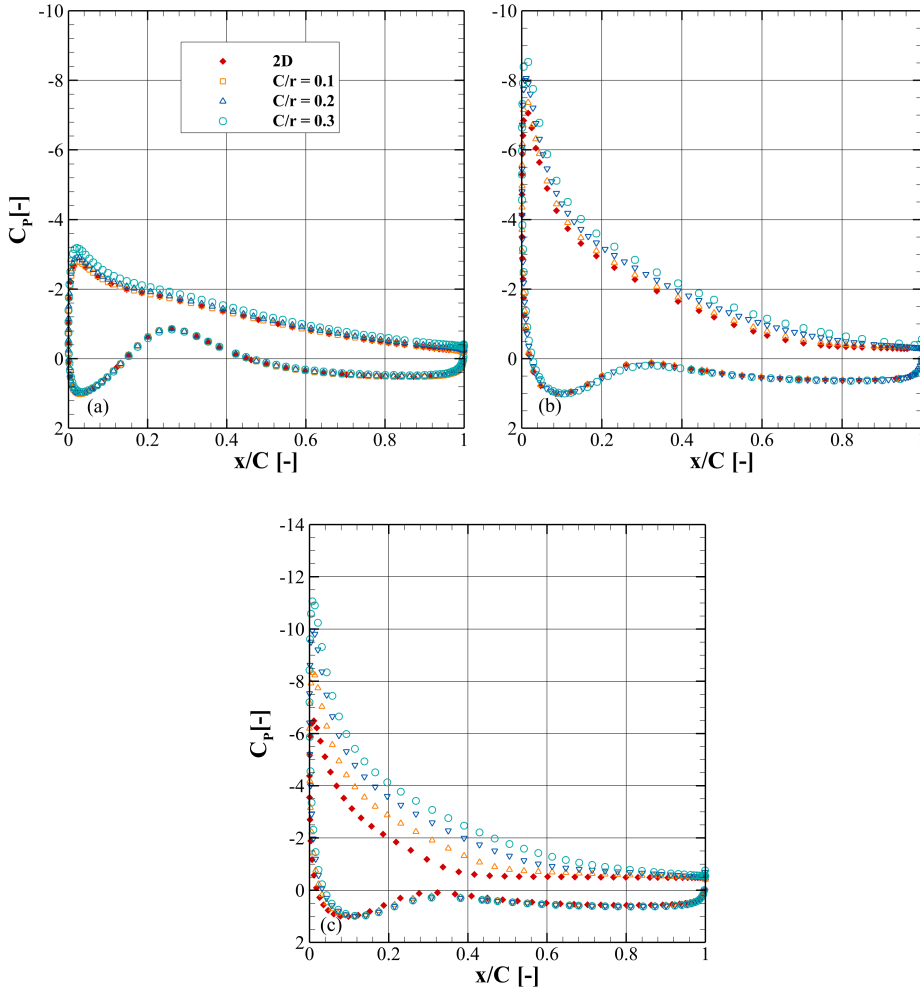


Figure B.52: Pressure coefficient distribution for HB-FB-350-150-R airfoil, [$Re = 6 \times 10^6$, (a): $\alpha = 8^\circ$, (b): $\alpha = 20^\circ$, (c): $\alpha = 24^\circ$].

ratio increases from 0.1 to 0.3. This observation shows that the radial transport induced by rotation, although influential for mean-flow quantities and drag, has only a minor impact on the streamwise unsteady behavior. The streamwise fluctuations continue to be governed primarily by the shear layer dynamics behind the blunt trailing edge, which remain largely unaffected by changes in the C/r ratio.

Figures B.52 show the pressure coefficient distributions for the HB-FB-350-150-R airfoil at a Reynolds number of $Re = 6 \times 10^6$ for the 2D configuration and the three C/r ratios of 0.1, 0.2, and 0.3 at angles of attack of $\alpha = 8^\circ$, $\alpha = 20^\circ$, and $\alpha = 24^\circ$. At $\alpha = 8^\circ$, where the flow remains fully attached, the suction side pressure peak increases slightly as the C/r

ratio grows. At $\alpha = 20^\circ$, which corresponds to an early post-stall condition, the influence of rotation becomes more pronounced than at $\alpha = 8^\circ$. At $\alpha = 24^\circ$, a deep stall condition in the 2D case, the effect of rotation is substantially stronger. This behavior is clearly visible in figure B.52 (c), where the suction peak rises sharply with increasing C/r ratio, and the pressure gradient on the suction side decreases markedly.

The results further indicate that the C/r ratio alters the suction side pressure distribution to a much greater extent than the distribution on the pressure side. At $\alpha = 8^\circ$, the pressure coefficient on the pressure side decreases slightly at higher C/r ratios due to the decrease of the pressure gradient downstream of the location of maximum relative thickness. At higher angles of attack, however, the changes on the pressure side with increasing C/r become minor. As shown, the pressure gradient on the pressure side is significantly lower at elevated angles of attack compared with lower angles, making the rotational effects less influential in this region at lower angles of attack.

As discussed previously, rotational effects exert a stronger influence on separated flows than on attached flows. The primary reason is that, in separated regions, the flow loses a substantial portion of its momentum, allowing rotational forces to dominate and redirect the motion. A similar phenomenon may occur in attached flows when steep pressure gradients reduce the streamwise momentum to a point where rotational forces have a comparable influence. Consequently, rotation can affect not only separated wakes but also attached flow regions subjected to strong pressure gradients. The pressure coefficient trends observed here across different angles of attack and C/r ratios are consistent with earlier investigations on rotational effects and stall augmentation by Shen et al. [205], Bak et al. [213], Schreck et al. [254], and Chaviaropoulos et al. [207]. These studies also reported enhanced suction peaks, altered separation behavior, and significant modification of the suction side pressure field under rotational motion, confirming the trends captured in the present simulations.

Figure B.53 compares the streamline plots for the HB-FB-350-150-R airfoil at a Reynolds number of $Re = 6 \times 10^6$ and an angle of attack of $\alpha = 24^\circ$, for the 2D configuration and for the three C/r ratios of 0.1, 0.2, and 0.3. The streamline visualizations reveal a clear trend in the development of the separated flow region on the suction side. As the C/r ratio increases, the size of the separation bubble decreases steadily. At $C/r = 0.3$, the separation bubble has nearly vanished, indicating a substantial reduction in suction side separation due to the influence of rotation at this high angle of attack. The reduction of the boundary layer thickness at higher C/r ratios is related to the combined effect of centrifugal and Coriolis forces acting on the rotating airfoil section. As the chord-to-radius ratio increases, the centrifugal force strengthens and drives low-momentum fluid outward in the spanwise direction, supplying the boundary layer with additional momentum and slowing its natural thickening under adverse pressure gradients. At the same time, the Coriolis force acts in the streamwise direction, creating a favorable pressure gradient effect by countering the loss of streamwise momentum within the boundary layer. This stabilizes the boundary layer and suppresses the growth of shear layer disturbances that typically

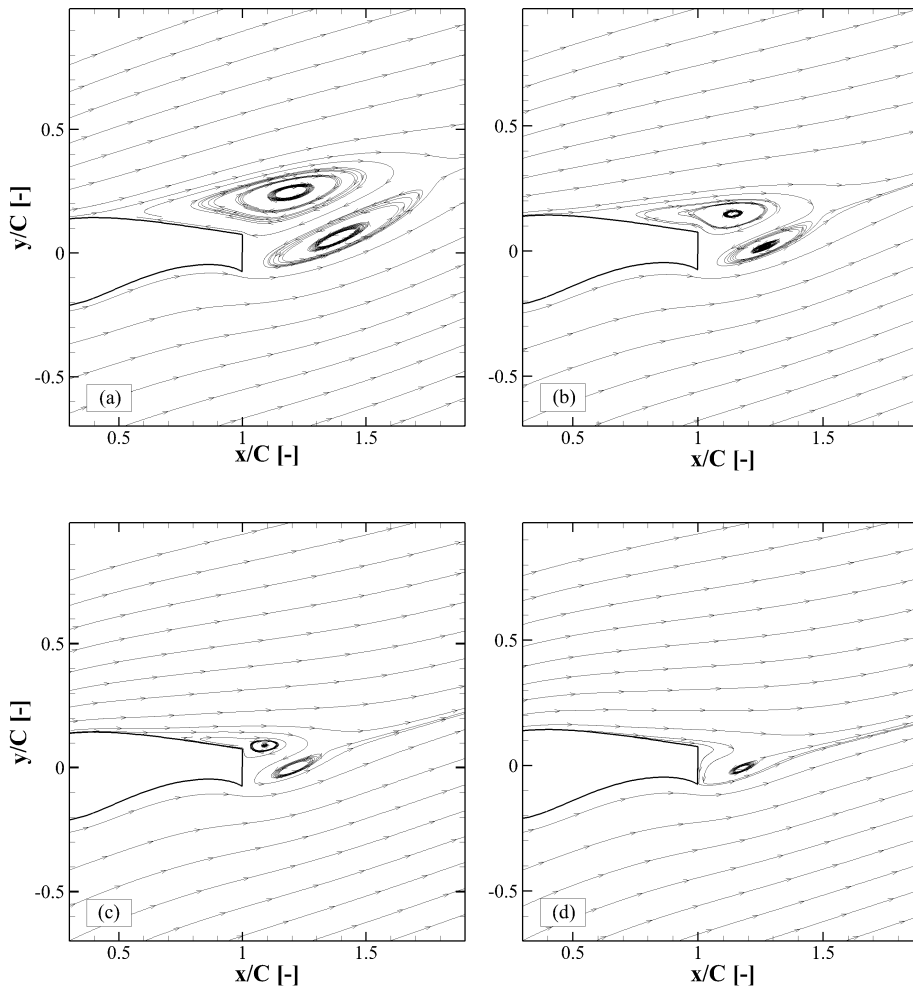


Figure B.53: Streamlines plot for the HB-FB-350-150-R airfoil, [$Re = 6 \times 10^6$, $\alpha = 24^\circ$, (a): 2D, (b): $C/r = 0.1$, (c): $C/r = 0.2$, (d): $C/r = 0.3$].

promote separation. Since both forces intensify as the C/r ratio increases, the suction side flow remains more energized, and the separated region contracts. These mechanisms form the physical basis for stall delay behavior and explain the significant reduction in separation observed at $C/r = 0.3$.

Figure B.54 depicts the surface streamline patterns for the HB-FB-350-150-R airfoil obtained from quasi-3D simulations at a Reynolds number of $Re = 6 \times 10^6$ and a constant C/r ratio of 0.1 for four angles of attack: $\alpha = 8^\circ$, $\alpha = 16^\circ$, $\alpha = 18^\circ$, and $\alpha = 20^\circ$. At $\alpha = 8^\circ$, the flow remains fully attached on the suction side, and the lift coefficient lies within the linear portion of the lift curve. The streamlines over the suction side surface remain

B

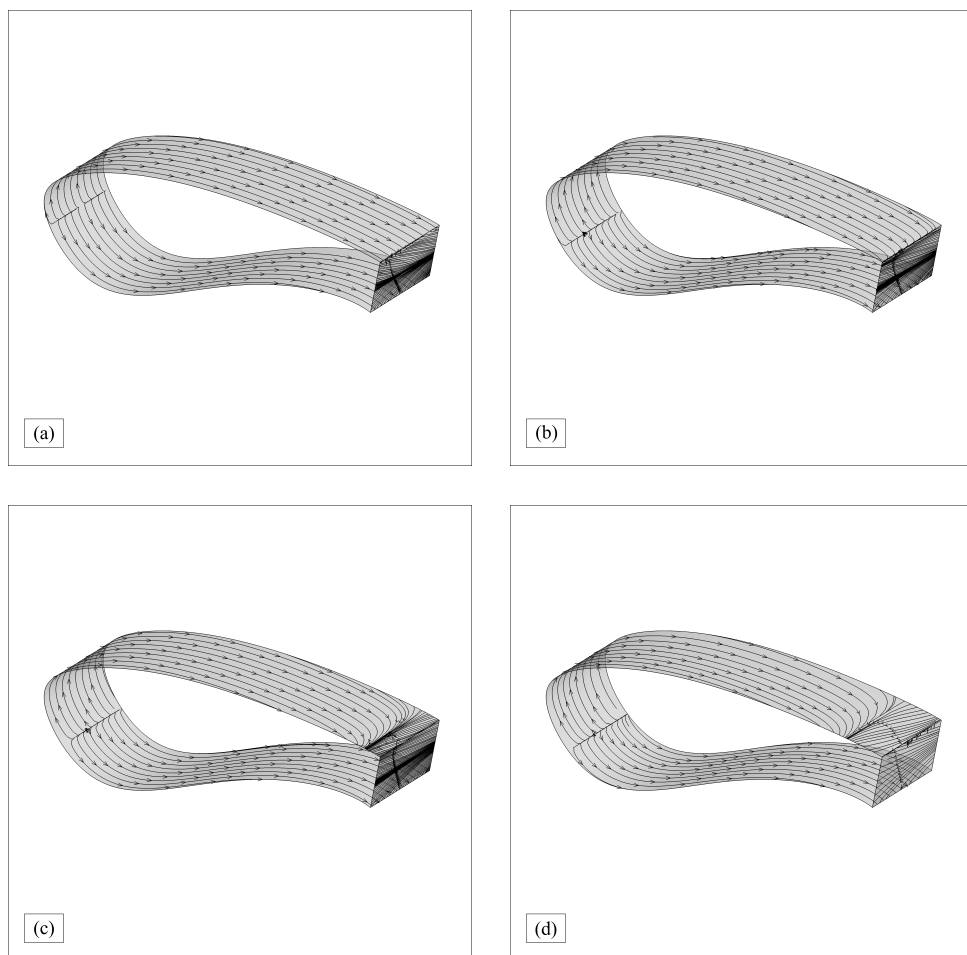
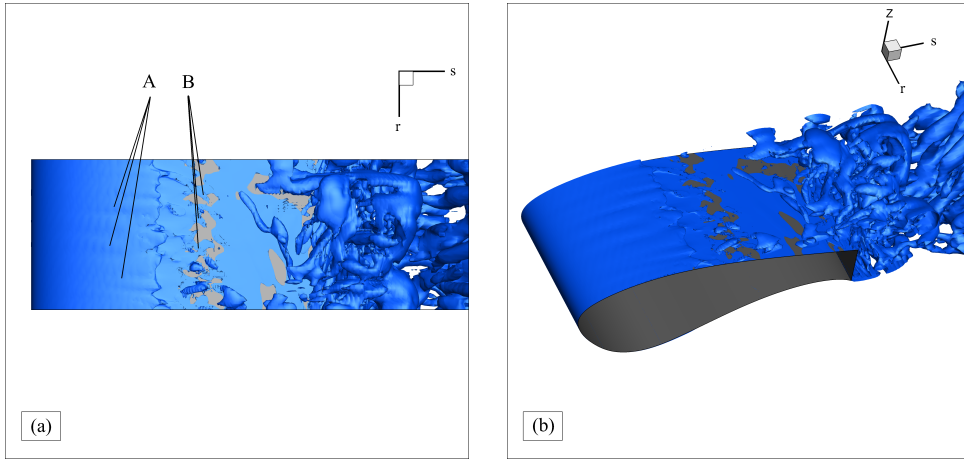


Figure B.54: Surface streamlines plot for the HB-FB-350-150-R airfoil, [$Re = 6 \times 10^6$, $C/r = 0.1$, (a): $\alpha = 8^\circ$, (b): $\alpha = 16^\circ$, (c): $\alpha = 18^\circ$, (d): $\alpha = 20^\circ$].

nearly straight, indicating strong chordwise momentum. On the pressure side, however, the streamlines bend in the radial direction, a consequence of the strong pressure gradient that develops on this side at low angles of attack.

As the angle of attack increases, the radial motion on the pressure side becomes more organized, and the near-surface flow gains stability. At $\alpha = 16^\circ$, which marks the end of the linear lift range, the streamlines on the suction side show a pronounced deflection near the trailing edge. This behavior indicates weakened chordwise momentum and the onset of conditions that precede flow separation. The simulated stall angle for this airfoil at $C/r = 0.1$ is $\alpha = 19^\circ$.

When the angle of attack is increased to $\alpha = 18^\circ$, the formation of a trailing edge



B

Figure B.55: Instantaneous iso-surface of λ_2 -criterion for the HB-FB-350-150-R airfoil, [$Re = 6 \times 10^6$, $\lambda_2 = 2000$, $C/r = 0.1$, $\alpha = 24^\circ$].

separation bubble becomes apparent. This separated region possesses a clear rotational component in the radial (spanwise) direction and an outward radial displacement visible in figure B.54 (c). As the angle of attack rises further, the size of this separated zone increases, and its upstream extent progresses toward the leading edge, as shown in figure B.54 (d). The radial transport generated by the motion of both the primary and secondary separated bubbles at the trailing edge is evident across all streamline plots, demonstrating the strong three-dimensionality of the separated flow, even at moderate levels of rotation.

The λ_2 -criterion was employed to identify the vortex structures on the suction side in the vicinity of the separation onset above the airfoil. Figures B.55 (a) and (b) illustrate iso-surfaces of $\lambda_2 = 2.0 \times 10^3$ for the HB-FB-350-150-R airfoil at a Reynolds number of $Re = 6 \times 10^6$, a C/r ratio of 0.1, and an angle of attack of $\alpha = 24^\circ$, obtained from DES calculations. These visualizations reveal a clear post-stall cross-flow mechanism that governs the three-dimensional flow structure in this operating condition.

Before the onset of full separation, the flow begins to tilt gently toward the radial direction, forming what appear as the A-type structures. Once separation develops, the bending of the flow toward the radial direction intensifies markedly, giving rise to the B-type structures within the separated region. This behavior results from the combined action of centrifugal and Coriolis forces on the low-momentum flow near the surface, which alters the direction of the separating shear layer and amplifies its three-dimensional character.

CONCLUSIONS

The present investigation provides a comprehensive aerodynamic analysis of flatback airfoils using wind tunnel experiments, PIV measurements, RANS/DES CFD simulations, and a quasi-3D Navier–Stokes formulation capable of capturing key rotational augmentation effects. By integrating these methodologies, this work advances the understanding of flatback airfoil aerodynamics in ways that are directly relevant for the design and optimization of next generation large wind turbine blades, especially in the structurally demanding inboard region.

Experimental results from the TU Braunschweig wind tunnel demonstrated that the HB-FB-350-150 airfoil exhibits excellent agreement with CFD predictions in attached flow regimes. The measured and simulated pressure distributions, lift curves, and pitching moment characteristics aligned closely in linear aerodynamic ranges, confirming the accuracy of the numerical approach. particularly noteworthy is the experimentally verified smooth stall behavior and insensitivity to leading edge roughness of the optimized HB-FB-350-150 airfoil, both of which were core design goals of the HB flatback airfoil family. The robust aerodynamic performance under tripped and clean conditions highlight the practicality of flatback sections for maintaining performance in real-world wind energy applications.

The PIV measurements revealed a detailed picture of the near-wake dynamics that has not been extensively documented in previous flatback literature. Two distinct, counter-rotating recirculation regions were consistently observed behind the blunt trailing edge. Their size remained relatively stable at moderate angles of attack but expanded substantially nearing stall. The wake showed strong shear layers characterized by elevated RMS velocity gradients, which are directly tied to vortex shedding and broadband noise generation.

The quasi-3D CFD simulations provided further understanding of the 3D behavior of these flatback airfoils. By incorporating centrifugal and Coriolis forces directly into the Navier–Stokes equations, the numerical model captured the essential physics associated with rotational augmentation. The results revealed a clear increase of radial flow intensity with higher C/r ratios, especially within the separated shear layer and the separated bubble. This radial transport of momentum has two major implications, i.e., it increases the drag of the trailing edge flat region, and it reduces the size of the suction side separation bubble at high angles of attack, thereby modifying the high angle of attack aerodynamic characteristics.

Pressure distribution analyses further confirmed that 3D-rotational effects preferentially modify the suction side, especially in near stall angles of attack where the flow momentum has decayed and rotational forces have relatively greater influence. At high angles of attack, the suction peak increased for higher C/r ratios, and the pressure gradients were significantly altered compared to the 2D case. These results align with classical descriptions of stall delay on rotating blades but extend them to the unique geometry of flatback airfoils, where the trailing edge bluntness amplifies near-wake interactions. Moreover, the results show, as the C/r ratio increases, the skin friction coefficient rises, particularly near the

trailing edge, although the transition point remains relatively insensitive to these rotational forces.

High fidelity DES simulations revealed the formation of coherent vortex structures and a fully developed vortex street behind the trailing edge. These structures were shown to originate simultaneously from both suction and pressure sides and to decay over approximately 0.5 chord lengths downstream. Importantly, the DES results clearly captured the spanwise (radial) migration of coherent structures, a behavior that intensifies with increasing C/r values. This finding emphasizes that rotational augmentation is not limited to modifying steady aerodynamic coefficients; it also influences the unsteady dynamics and wake coherence of flatback airfoils, which has implications for aeroacoustic modeling and load prediction in rotating blades.

In summary, this study provides critical insights into the complex flow physics governing flatback airfoils in rotating environments. The findings indicate the necessity of considering rotational effects during the airfoil design phase to optimize the aerodynamic performance of thick inboard blade sections. Future work should focus on further refining the aeroacoustic models to mitigate the increased noise associated with blunt trailing edge vortex shedding, for those flatback airfoils that are used in the mid-span of the blade, as well as exploring the impact of various trailing edge add-ons, such as splitter plates, on the overall base drag in 3D-flow fields.

Part V: References and Supporting Sections

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LIST OF PUBLICATIONS

PUBLICATIONS FROM THE PRESENT THESIS:

- Mehdi Doosttalab, Carlos Simão Ferreira, Daniele Ragni, Wei Yu, and Christof Rautmann. Evaluating dynamic stall models for thick wind turbine airfoils. *Renewable Energy*, 125096, 2026.
- Mehdi Doosttalab, Carlos Simao Ferreira, Daniele Ragni, Wei Yu, and Christof Rautmann. Vortex generator effects on dynamic stall of thick airfoils. *Renewable Energy*, 255, 123746, 2025.
- Mehdi Doosttalab, Carlos Simao Ferreira, Daniele Ragni, Wei Yu, and Christof Rautmann. Dynamic stall characteristics of thick wind turbine airfoils. *Wind Energy*, 28(11), e70061, 2025.

PUBLICATIONS BASED ON APPENDIX B:

- Mehdi Doosttalab and Olaf Frommann. 3d rotational augmented flow effects on the multidisciplinary optimization of flatback airfoils. *Renewable Energy*, 241, 122246, 2025.
- Mehdi Doosttalab and Olaf Frommann. Multidisciplinary design and verification of the HB flatback airfoil family. *AIAA Journal*, 57(11), 4639–4649, 2019.
- Mehdi Doosttalab and Olaf Frommann. Effects of the design parameters on the multidisciplinary optimization of flatback airfoils for large wind turbine blades. In 13th AIAA/ISSMO multidisciplinary analysis optimization conference, 9096, 2010.

