Horizontal Tailplane-Tip Mounted Tractor Propeller Interaction Effects
An Aerodynamic and Aeroacoustic Experimental Study

A.A. Candade

September 18, 2015

Faculty of Aerospace Engineering - Delft University of Technology
Horizontal Tailplane-Tip Mounted Tractor Propeller Interaction Effects
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MASTER OF SCIENCE THESIS

For obtaining the degree of Master of Science in Aerospace Engineering at Delft University of Technology

A.A. Candade

September 18, 2015

Faculty of Aerospace Engineering · Delft University of Technology
The undersigned hereby certify that they have read and recommend to the Faculty of Aerospace Engineering for acceptance a thesis entitled “Horizontal Tailplane-Tip Mounted Tractor Propeller Interaction Effects” by A.A. Candade in partial fulfillment of the requirements for the degree of Master of Science.

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Advanced propeller propulsion systems potentially provide a significant reduction in fuel burn compared to traditional turbofans. However, the large blade diameters associated with these concepts, along with concerns about noise emissions, motivate the positioning of the propeller as far away from the cabin as possible. From this perspective, a layout with rear-fuselage mounted pusher propellers initially seems most beneficial, but this configuration suffers from installation penalties that might require complicated active systems to overcome. An alternative layout, that is the focus of this study is the horizontal tailplane-tip mounted tractor propeller concept.

The aim of this thesis is an experimental investigation into the aeroacoustic and aerodynamic interaction effects of the tailplane-tip mounted tractor propeller configuration, including the effects of elevator deflections. The experimental study was conducted at TU Delft’s Low Speed Laboratory in both the Vertical Tunnel and the Low Turbulence Tunnel with two different models. The first model was a variable propeller-pylon model, the VARPW, which was used exclusively to test the aeroacoustic interaction effects for the tractor propeller-pylon configuration. The second model, the PROWIM 2.0, was used to study the propeller-pylon interaction effects with respect to performance and pylon loads. Additionally, particle Image Velocimetry was undertaken to study the propeller slipstream flow field.

From the aeroacoustic study using the VARPW model, it was concluded that the installation of the pylon behind the propeller affects both the directivity and the tonal levels of the propeller noise field, with the broadband acoustic levels remaining unchanged. It was determined that the overall sound pressure level (SPL) across the range of directivity angles considered is inversely proportional to the propeller-pylon spacing. The installation of the trailing pylon with zero elevator deflection at a distance of 10% of the propeller diameter from the propeller, resulted in a 4 dB noise penalty compared to the isolated propeller. For a spacing of 50% of the propeller diameter, the overall SPL was comparable to the case of the isolated propeller. A unique characteristic of installation of the pylon was the development of a trough in the directivity for an observer position in the pylon plane. This was caused by the cancelling of the steady noise field by the noise due to unsteady blade loading which arises due to inflow distortion caused by potential effects
caused by the pylon. This unsteady blade loading is a function of the propeller-pylon spacing, and hence the levels in the trough decrease with decreasing propeller-pylon spacing. The effect of the elevator on the acoustic levels depended on the location of the pylon with respect to the propeller. For directivity angels in the pylon plane, for a spacing below 30% of the propeller diameter, the unsteady loading is further influenced by the elevator deflection and was the main mechanism of the interaction noise. For propeller-pylon spacings above 30% of the propeller diameter, the interaction of the slipstream (either the propeller noise field, or the slipstream impingement) with the elevator was determined to be the main interaction mechanism. However, acoustic source localisation experiments are recommended to investigate this interaction mechanism in detail.

From the PIV and performance evaluations using the Prowim 2.0 model, it was concluded that for the given propeller-pylon spacing (43% and 85% propeller diameter), there was indeed negligible upstream interaction effect due to the trailing pylon, including the case of the deflected elevator. From the velocity profiles obtained from PIV, it was further confirmed that potential effects of the elevator are dominated by the propeller induced velocities in the slipstream region. Pylon loads obtained from an external balance showed that for symmetric inflow conditions, operation of the thrusting propeller increased elevator effectiveness by 20% compared to the case with no propeller present.

A numerical simulation using XROTOR was used for the validation of the test data and had a relative error of 3% with the experimentally evaluated propeller thrust for the lowest advance ratio. A slipstream propagation model based on the computed propeller induced velocities showed acceptable trends when compared to the experimentally determined induced propeller velocity profiles. However, the numerical model overpredicts the velocity profile in the tip region, owing to the tool’s limitation in predicting stall at the blade tip. A VLM based numerical analysis which included the effects of the propeller slipstream, was able to predict the pylon lift to within 3% of the lift computed from the surface pressure measurements, but failed in the prediction of the drag of the model.

This thesis was a preliminary investigation into the tailplane-tip mounted propeller concept. A priority for future work is to determine the blade loads directly either by means of a rotating shaft balance, or with PIV to directly capture the blade surface pressures, allowing for the determination of the contribution of unsteady blade loading noise. The determination of interaction mechanism at spacings above 30% of the propeller diameter is another aspect that needs further investigation. Additionally, an evaluation of the configuration from a structural and stability perspective is also necessary.
Acknowledgements

Considering all my studies as an Aerospace Engineering student, I never really understood how time could ‘fly’, but gosh it does! This Masters Thesis is the culmination of my two years at Delft. During this time, I have experienced a lot, from multidisciplinary optimisation, to attempting to pilot a helicopter on the SIMONA. First off I’d like to thank all the people who have influenced my time in Delft. I have learnt a lot of things that I never expected to have studied during a masters degree! From the need for a special pair of shoes for fraternity parties, to opening beer bottles with the first random object within reach!

With special reference to my thesis, the journey would not be complete without the help of a number of people.

I would like to thank Leo for initially matching my interest in propellers and experimental studies, and pointing me in the direction of this thesis. It has been a great privilege to get your guidance right through my study.

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Last but not the least, I would like to thank my family, I am extremely grateful to my parents and my sister, and am here sitting writing this all thanks to you!

Ashwin A. Candade
September 18, 2015
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Latin Symbols

\begin{tabular}{ll}
\textit{BPF} & Blade passage frequency [Hz] \\
\textit{c} & Chord [m] \\
\textit{c_e} & Elevator chord [m] \\
\textit{c_d} & 2 Dimensional sectional drag coefficient [-] \\
\textit{c_l} & 2 Dimensional sectional lift coefficient [-] \\
\textit{C_p} & Coefficient of pressure [-] \\
\textit{C_Q}, \textit{C_Q}' & Torque coefficient [-] \\
\textit{C_T}, \textit{C_T}' & Thrust coefficient [-] \\
\textit{D} & Propeller diameter [m] \\
\textit{D_{ary}} & Beamforming array diameter [m] \\
\textit{D_\infty} & Drag of the model at a given operation condition [-] \\
\textit{D_{int}} & Drag due to increased dynamic pressure in the slipstream [-] \\
\textit{F_{net}} & Net force [N] \\
\textit{J} & Propeller advance ratio [-] \\
\textit{m} & Mass flow rate [kg/s] \\
\textit{N} & Number of propeller blades [-] \\
\textit{n} & Revolutions per unit time(min or sec) [rpm],[rps] \\
\textit{Pt} & xflrp mesh grid control point [-] \\
\textit{Pt_0} & xflrp propeller center point [-] \\
\textit{R} & Blade radius [m] \\
\textit{b} & Span [m] \\
\end{tabular}

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### Nomenclature

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<th>Unit</th>
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<tr>
<td>$s$</td>
<td>Distance of slipstream section from propeller plane</td>
<td>[m]</td>
</tr>
<tr>
<td>$S_P$</td>
<td>Propeller disk area</td>
<td>[m$^2$]</td>
</tr>
<tr>
<td>$S_{tun}$</td>
<td>Tunnel jet cross-sectional area</td>
<td>[m$^2$]</td>
</tr>
<tr>
<td>$u$</td>
<td>Propeller induced axial velocity</td>
<td>[m/s]</td>
</tr>
<tr>
<td>$U_{in}$</td>
<td>Propeller inflow velocity</td>
<td>[m/s]</td>
</tr>
<tr>
<td>$V_E$</td>
<td>Effective velocity at blade section</td>
<td>[m/s]</td>
</tr>
<tr>
<td>$V_{jet}$</td>
<td>Jet velocity</td>
<td>[m/s]</td>
</tr>
<tr>
<td>$V_R$</td>
<td>Relative velocity at blade section</td>
<td>[m/s]</td>
</tr>
<tr>
<td>$V_\infty$</td>
<td>Freestream velocity</td>
<td>[m/s]</td>
</tr>
<tr>
<td>$w$</td>
<td>Propeller induced tangential velocity</td>
<td>[m/s]</td>
</tr>
<tr>
<td>$Y$</td>
<td>Distance between beamforming array and source</td>
<td>[m]</td>
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### Greek Symbols

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
<th>Unit</th>
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<tbody>
<tr>
<td>$\alpha$</td>
<td>Effective blade angle of attack</td>
<td>[°]</td>
</tr>
<tr>
<td>$\alpha_i$</td>
<td>Induced angle of attack</td>
<td>[°]</td>
</tr>
<tr>
<td>$\beta$</td>
<td>Blade pitch angle</td>
<td>[°]</td>
</tr>
<tr>
<td>$\beta_{3/4}$</td>
<td>Blade pitch angle at 75% blade radius</td>
<td>[°]</td>
</tr>
<tr>
<td>$\Delta f$</td>
<td>Frequency resolution of the PSD</td>
<td>[Hz]</td>
</tr>
<tr>
<td>$\Lambda$</td>
<td>Sweep angle</td>
<td>[-]</td>
</tr>
<tr>
<td>$\lambda$</td>
<td>Taper ratio</td>
<td>[-]</td>
</tr>
<tr>
<td>$\lambda_{XROTOR}$</td>
<td>$XROTOR$ fit optimisation objective weight factor</td>
<td>[-]</td>
</tr>
<tr>
<td>$\eta_{propeller}$</td>
<td>Propeller efficiency</td>
<td>[-]</td>
</tr>
<tr>
<td>$\eta_{prop}$</td>
<td>Propulsive efficiency</td>
<td>[-]</td>
</tr>
<tr>
<td>$\omega$</td>
<td>Angular velocity</td>
<td>[rad/s]</td>
</tr>
<tr>
<td>$\sigma$</td>
<td>$XROTOR$ fit optimisation objective penalty function</td>
<td>[-]</td>
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### Abbreviations

<table>
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<tr>
<th>Abbreviation</th>
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<tbody>
<tr>
<td>BPF</td>
<td>Blade Passage Frequency</td>
</tr>
<tr>
<td>CROR</td>
<td>Contra-Rotating Open Rotor</td>
</tr>
<tr>
<td>FPGA</td>
<td>Field-Programmable Gate Array</td>
</tr>
<tr>
<td>LLT</td>
<td>Lifting Line Theory</td>
</tr>
<tr>
<td>LSL</td>
<td>Low Speed Laboratory</td>
</tr>
<tr>
<td>MCU</td>
<td>Motor Control Unit</td>
</tr>
<tr>
<td>PSD</td>
<td>Power Spectral Density</td>
</tr>
<tr>
<td>SPL</td>
<td>Sound Pressure Level</td>
</tr>
<tr>
<td>VLM</td>
<td>Vortex Lattice Method</td>
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Part I

Introduction
Introduction

1.1 History of the Open Rotor

The late 1950s saw the replacement of piston engine powered propeller planes by intercontinental turbo jet powered transport aircraft. These aircraft set new standards in the transport sector with unrivalled levels of speed and economy. But after a few short years of service, the turbojets were replaced by the first generation of turbofans, which were more efficient. The 1960s ushered in the era of the turbofan and the focus shifted away from propeller technology for transport aircraft. The turbofan has been the workhorse of the commercial airline transport sector ever since. The early 1970s witnessed the Six-Day War, and the retaliation of the Arabs in 1973. The resulting oil embargo sent crude oil prices to new heights. This scarcity led to a collaboration between NASA and Hamilton Standard and ultimately resulted in the Advanced Turboprop Project. The Gulf War in the 1980s caused further increase in oil prices, and this persuaded General Electric to develop a proof-of-concept of an open rotor engine which was called the UDF® demonstrator engine. To minimise swirl losses from the propeller, the engine consisted of two propellers co-axially rotating in opposite directions. This engine was flight-tested on a McDonnell Douglas MD-80 and later on a Boeing 727-100 [1]. Soon after the UDF concept, a slew of open rotor engine concepts were developed by NASA, Pratt & Whitney and others like the Allison 578DX, GE36, and ZMKB SV 27. [2]. However, with no mainstream integration of these engines by aircraft manufacturers such as Boeing and McDonnell Douglas, along with the relaxation of crude oil prices, further development of the open rotor came to a standstill and the Advanced Turboprop Project was officially closed in 1987. [3, 4].

From the beginning of the 21st century, there has been an increasing scarcity of fossil fuels and emphasis on more sustainable operations. The growth of the commercial air transport sector coupled with increasing crude oil prices have prompted the regulatory bodies to impose stricter emission and operational norms for future aircraft. The Advisory Council for Aeronautics Research in Europe (ACARE) is targeting a reduction of 50% for CO₂ emissions and fuel burn, a 80% reduction of NOₓ and a reduction of external noise by 50% for aircraft in the year 2020 [5]. Current trends as visualised in Figure 1.1 propose that
the propeller is making a comeback, albeit in the form of open rotors and contra-rotating propellers, targeting a 22-28% reduction in fuel burn and CO$_2$ emissions from current operational levels [6–8].

![Figure 1.1: Advanced propulsion concepts timeline; reproduced from [7]](image)

### 1.2 Advanced Propeller Propulsion

The threat of an impeding and acute oil scarcity in the 1970s was one of the key catalysts which led to the genesis of open rotors. The need for a more fuel efficient propulsion system led to the re-look at propeller technology. The principle behind the higher efficiency of these systems can be understood from the equations of net thrust and propulsive efficiency which are given by:

\[
F_{\text{net}} = \dot{m}(V_{\text{jet}} - V_{\infty}) \tag{1.1}
\]

\[
\eta_{\text{prop}} = \frac{2}{1 + \frac{V_{\text{jet}}}{V_{\infty}}} \tag{1.2}
\]

where

\( \dot{m} \) = Mass flow rate  
\( V_{\text{jet}} \) = Jet velocity  
\( V_{\infty} \) = Freestream velocity

The open rotor extends on the benefits of the propeller. Thus, for a given thrust, open rotors have a larger mass flow at reduced velocity as compared to turbofans which will have a lower mass flow at higher velocity. Hence, open rotors are able to operate with a higher propulsive efficiency, given the reduced difference in velocity between the aircraft and the jet. One way to achieve this higher mass flow at lower velocities is to increase the engine bypass ratio. Conventional turbofans are limited in the maximum bypass ratio
they can achieve by the weight and drag of the nacelle. These issues are overcome in open rotors by utilising a propeller, thus intrinsically increasing the bypass ratio, while removing the nacelle to minimise drag, hence giving it the name of ‘open rotors’. Further improvement in the efficiency of the open rotor system is obtained by placing a secondary propeller behind the main propeller to minimise the swirl losses. This configuration is referred to as the Contra-Rotating Open Rotor (CROR).

1.3 Challenges Faced with Open Rotors

Despite of the propulsive benefits of open rotor systems, they are yet to be adopted in any commercial transport aircraft. The open rotor propulsion system is most suitable for short range, 100-150 passenger commercial aircraft with a cruise Mach number of 0.8 [8–10]. There are however two main challenges that need to be overcome prior to the adoption of these systems in commercial aircraft. The first challenge for open rotors is the engine airframe integration. The large engine diameter, makes the under-wing engine configuration impractical for the typical mid and low wing configuration. Another concern is the noise generated by open rotors. The lack of a nacelle leads to higher noise levels as the shielding effect and acoustic liners are no longer feasible [9].

1.3.1 Open Rotor Airframe Integration

To overcome the constraints imposed by the large blade span, the aft pylon mounted configuration is the most commonly proposed open rotor integration configuration [8, 9]. The obvious benefit of the aft mounted configuration would be that the wing is in the so called “clean” configuration, lowering wing drag. The main benefit would be increased passenger comfort, as the engine is mounted further away from the cabin. Additional benefits would be lower yawing moment in case of a single engine operating condition. Another advantage is the reduction of the risk of foreign object damage, with the engine mounted further higher up on the aircraft, risk of ground debris ingestion is reduced. For the aft mounted configuration, several tail configurations are possible: the T-tail, the U-tail, the cruciform tail, and the conventional tail which can further influence the engine integration.

The aft engine configuration has some disadvantages: in case of uncontained engine failure, vital control lines are vulnerable. Another disadvantage is that the structural weight would increase for a tail mounted engine. To withstand the weight of the engine the tail box will require more material, as tail box area is smaller compared to a wing box, and hence needs to be denser in order to maintain the required stiffness [11]. There are additional challenges of acoustic scattering as well, which increases cabin noise [8, 12, 13].

1.3.1.1 Horizontal Tail-Mounted Configuration

When considering the aft-mounted open rotor engine concept, the idea of utilising a horizontal tailplane tip-mounted propeller comes to mind. This configuration would do away with the need for an engine pylon, reducing the associated skin friction drag. This
6 Introduction

The concept was looked into by McDonnell Douglas in 1981 in a study comparing different engine mounting options such as conventional under wing, aft fuselage pylon mounted, and horizontal tail mounted [14]. The horizontal tail mounted configuration proposed in the research is illustrated in Figure 1.2. The study concluded that the horizontal tail mount was the more beneficial configuration, based on structural, acoustic and performance criteria [10].

![Figure 1.2: DC-9 Super 80 Propfan concept; reproduced from [10]](image)

More recently, Boeing in their quest for more environmentally friendly aircraft came up with the concept of the ‘Fozzie’ with an advanced propeller propulsion concept mounted on the horizontal tailplane as seen in Figure 1.3. Boeing envisions 30% fuel savings [15].

![Figure 1.3: Boeing Fozzie concept short range aircraft with rear empennage mounted advanced propeller propulsion system; reproduced from [15]](image)

The installation of open rotor engine concepts however gives rise to a multitude of interaction effects. A survey of existing literature on the possible interaction effects is presented in the subsequent chapter.
Propeller Slipstream Interaction Effects

Propeller slipstream interactions remains a field of active and challenging research. The increased dynamic pressure along with the swirl in the propeller slipstream can potentially cause large installation penalties. While a complete discussion of the interaction effects is beyond the scope of this work, a brief overview of the terms and concepts employed in propeller aerodynamics is discussed initially in Section 2.1. A summary of past interaction studies is then presented in Section 2.2. An overview of propeller acoustics is covered in Section 2.3. This chapter attempts to facilitate the discussion of the experimentally observed phenomena of this thesis work with insight from prior studies.

2.1 Propeller Aerodynamics - Blade Element Theory

A typical fixed pitch propeller can be described by the number of blades \( N \), where each blade has a radius \( R \) and rotating with an angular velocity \( \omega \). A single blade can then be divided into sections or blade elements each having a height of \( dr \), positioned at distance \( r \) from the hub as illustrated by Figure 2.1a.

In the frame of reference of the blade section, the propeller blade angle or pitch angle \( \beta \) is defined as the angle between the zero-lift line or the chord line of the element and the rotation plane as illustrated in Figure 2.1b. In the case of a fixed pitch propeller, the blade angle is usually defined at 75% of the blade radius (i.e. \( \frac{3}{4}R \)) and is designated as \( \beta_{3/4} \).

If the blade is advancing through the air with a velocity \( V_\infty \), the relative velocity experienced by the blade section \( V_R \) is given by:

\[
V_R = \sqrt{V_\infty^2 + (\omega r)^2}
\]  \hspace{1cm} (2.1)

As the blade section in theory is an airfoil under an angle of attack, it produces lift \( dL \). Thus in order to produce this lift, a circulation is present around each section. This
results in an induced velocity which can be decomposed into three components, the axial component $v_a$, the tangential component $v_t$, and the radial component $v_r$. The radial component is normal to the blade section and is usually neglected in calculations. The induced velocity imparts an additional angle called the induced angle of attack ($\alpha_i$). The effective velocity ($V_E$) at the section can be obtained taking into account the induced effects and is given by:

$$V_E = \sqrt{(V + v_a)^2 + (\omega r - v_t)^2}$$ (2.2)

The effective velocity vector forms an angle with the zero-lift line, which is called the effective angle of attack $\alpha$ which is defined as

$$\alpha = \beta - (\phi + \alpha_i)$$ (2.3)

The thrust and torque of the entire propeller can then be determined from the lift and drag of each blade section. In order to quantify the propeller performance, the following typical non dimensional propeller performance parameters are employed:

$$C_T = \frac{T}{\rho n^2 D^4}$$ (2.4)

$$C'_T = \frac{T}{\rho V_{\infty}^2 D^2}$$ (2.5)

$$C_Q = \frac{Q}{\rho n^2 D^5}$$ (2.6)
The dimensionless coefficients can be defined in terms of the propeller diameter $D$ and either the revolutions per unit time $n$, or the freestream velocity as seen in Equations 2.5 and 2.7. In order to describe the operation condition of the propeller, the advance ratio $J$ relates the freestream velocity, propeller diameter and rotational speed. It is defined as:

\[ J = \frac{V}{nD} \]  

The efficiency of the propeller is defined by the ratio of the propulsive power to the shaft power required. Using the non-dimensional coefficients previously stated, the propeller efficiency is given by:

\[ \eta_{\text{propeller}} = J \frac{C_T}{C_P} = \frac{J}{2\pi} \frac{C_T}{C_Q} \]  

With a brief summary of propeller aerodynamic theory and performance characteristics completed, the subsequent section outlines the basics of propeller slipstream interactions.

### 2.2 Aerodynamic Slipstream Interaction Effects

The propeller slipstream interaction effects can be segregated into two main components: the downstream effect of the propeller on the wing, and the upstream effect of the wing on the propeller.

#### 2.2.1 Downstream Effect of Propeller on Trailing Wing

In the tractor propeller configuration, the trailing wing is subjected to the wake of the propeller. The propeller induces both the axial and the swirl velocities in the slipstream. The axial velocities induce an increased dynamic pressure in the region of the slipstream. This affects the wing in a symmetrical way, i.e. the direction of rotation of the propeller does not influence the axial velocities. However, the swirl velocities induce either an increase or decrease of the local angle of attack depending on the direction of the blade rotation with respect to the wing. Due to these slipstream velocity components, there is a change in the lift distribution of the wing as visualised in Figure 2.2. For the inboard up configuration (i.e. the propeller blade going up relative to the wing), there is a positive interaction effect that leads to increased lift in the slipstream region. There is a shift in the local force vector as well, creating a negative drag or thrust component. At the
wingtip, the spanwise loading gradient is the steepest, and thus by mounting the propeller at the tip, the inboard up effects are most beneficial. Analytical models and windtunnel experiments performed in the 1980s confirm these effects of the propeller slipstream with observed increase in lift coefficient and reduction in drag with a wingtip mounted propeller [17–21].

![Figure 2.2: Influence of propeller slipstream on spanwise loading of trailing wing; reproduced from [22]](image)

Experiments conducted in TU Delft’s Low Turbulence Tunnel (LTT) by Dimchev [23] using a tip mounted tractor propeller model also confirm the effects of the propeller slipstream on the lift of the trailing wing as seen in Figure 2.3.

![Figure 2.3: Effect of thrusting wing-tip mounted tractor propeller on the lift coefficient of a wing; reproduced from [23]](image)

There is indeed an increase in lift as anticipated. The large difference in the drag coefficient between the thrusting propeller and the unmounted propeller measurements seen in Figure
2.2 Aerodynamic Slipstream Interaction Effects

2.3 is due to the thrust of the propeller being included in the drag measured by the balance for the propeller running case.

Dimchev in his studies, also varied the spacing between the wing and propeller. The study found there were negligible effects on the lift of the wing when the distance between the propeller and wing was reduced from 85% to 43% of the propeller diameter (0.42 m) as seen in Figure 2.4. This was attributed to negligible changes in the induced velocities between the two spacings.

![Figure 2.4: Effect of propeller-pylon spacing on the wing lift coefficient for an advance ratio \( J = 0.80 \); reproduced from [23]](image)

The interaction effects observed thus far focused mainly on the time averaged steady interaction effects of the propeller slipstream. In contrast, the blade tip vortex from the propeller causes unsteady effects on the wing as well. There is a deformation of this tip vortex at the leading edge of the wing at the location of impingement, splitting the vortex along the upper and lower surface [24].

Research on vortex impingement also show that there is then a spanwise motion of these vortices on the upper and lower surfaces due to the difference in pressures. This deformation and spanwise motion can be visualised from the radial velocity plot seen in Figure 2.5. The same study also found that the strength of these vortices reduce as they travel along the chord [25]. Research on natural laminar flow by Catalano and Howard also show how the propeller inflow in pusher configuration can be used to prevent separation. However in the tractor configuration, the observe that the wake of the propeller and tip vortices promote transition [26, 27]. These interaction effects are further explored when discussing aeroacoustic propeller interaction effects of the trailing wing.

2.2.2 Upstream Effect of Trailing Wing on Propeller

A lifting wing induces an upwash ahead of it. The presence of a lifting wing behind a propeller will thus induce an angle of attack to the propeller inflow due to this upwash [20].
When an isolated propeller is subjected to an angle of attack there is a difference between the effective angle of attack of the blade depending on its circumferential position thus leading to a change in the thrust. This effect is illustrated in Figure 2.6. Veldhuis confirms the effect of the angle of attack with his finding of increased thrust at the same rotation speed for a positive angle of attack [28].

The effect of the change in the propeller inflow due to a trailing wing was more recently experimentally studied at TU Delft, as seen in Figure 2.7. The results show the increase in the propeller thrust coefficient due to the upwash caused by the increase in the lift coefficient which is increased by the angle of attack of the wing. This upwash affects the local angle of attack of the propeller blade and hence for the same advance ratio, there is
more thrust when the wing is closer to the propeller due to the wing thickness effect and stronger upwash effects [29].

![Figure 2.7: Effect of upstream interactions on propeller performance. Relative difference in thrust coefficient (ΔC_T) between two propeller-pylon spacings; reproduced from [29]](image)

### 2.2.3 Effect of the Propeller Slipstream on Control Surface Deflections

A control surface subjected to the propeller slipstream will have higher lift when compared to the freestream due to the propeller induced velocities. This effect is seen in Figure 2.8, where computations based on Euler equations predicts the propeller produces a 52% increase in the local lift coefficient [30]. The effect of the blade rotation direction can also be noticed in the figure. Experimental studies performed by Veldhuis and Müller et al.also confirm an increase of the lift contribution of a flap in the propeller slipstream[28, 31].

Computations using the DLR TAU code (Reynolds Averaged Navier Stokes solver for an unstructured grid) were performed to predict the effect of a trailing high lift device on the inflow of an open rotor configuration. The propeller in these simulations was modelled as a reactive actuator disk to take into account the non uniform inflow conditions while capturing quasi static effects of the flow. The results from these tests indicate the inflow field for the normal baseline isolated propeller in Figure 2.9 (a). The concentric load distributions depict the ideal design case for the propeller, which is a uniform angle of attack inflow. Thus the propeller produces constant thrust at each radial station through each revolution. Figure 2.9 (b) illustrates the tractor configuration and the effect of a trailing high lift device. Due to the upwash of the trailing wing, there is an increase in the angle of attack and thus the blade loads. There is also a horizontal (Y-axis) shift in the loading due to the induced local angle of attack ahead of the wing [31].
Figure 2.8: Simulations of propeller slipstream effect based on Euler equations at a freestream Mach number of 0.25; reproduced from [30]

(a) Isolated propeller, $\beta_{75} = 29^\circ$, $C_T = 0.385$

(b) Tractor, $\beta_{75} = 29^\circ$, $C_T = 0.388$

Figure 2.9: Influence of trailing wing with high lift devices on propeller loading. Numbers represent the distribution of the effective blade section angle of attack $\alpha_e$ and contours represent local thrust $t/t_{max}$; reproduced from [31]
2.3 Propeller Noise Emissions

The noise field from a propeller differs when in the isolated case and when the propeller is installed on the aircraft. The installation of the propeller leads to either blade loading changes, or installation effects such as shielding or reflections that change the noise field.

2.3.1 Isolated Propeller Noise

The noise emitted from a propeller can be classified into three categories: harmonic noise, broadband noise and narrow-band random noise [32]. The harmonic or tonal noise is the main source of noise for propellers [33]. The contribution of broadband noise is considered relatively unimportant with majority of literature focusing on tonal noise [34, 35]. However, with methods to reduce the tonal noise for the installed pusher configuration (such as pylon blowing [13, 36–38]), there is an increasing interest in the reduction of broadband noise especially at mid and higher frequencies where it can no longer be ignored [39–41]. From the surveyed literature, it is concluded to keep the focus of the current thesis study on the tonal noise aspect of the propeller only.

The tonal noise of an isolated propeller can be divided into steady and unsteady sources as illustrated in 2.10. Linear thickness, linear loading and non-linear quadrupole are steady sources, while effects of angle of attack, external disturbances such as the upstream pylon wake for a pusher propeller, and interactions between wake and tip vortices in contra-rotating propellers are unsteady sources [32, 42, 43].

\[ BPF = \frac{N\omega}{2\pi} \]  

(2.12)

2.3.2 Installed Propeller Noise

The typical installation of the rotor on the airframe causes a static inflow distortion due to its vicinity to the fuselage. In the case of the tractor configuration, the upstream
circulation interactions cause a further change in the propeller inflow, while in the pusher configuration, the inflow is distorted by the upstream pylon wake. The tonal nature of propeller noise is seen from the propeller noise spectrum for a pusher contra-rotating configuration is seen in Figure 2.11. As the number of blades are different in the rear and front row, the blade passage frequency of each row is different as seen.

![Figure 2.11: Effect of pylon installation on the sound spectrum if a counter-rotating open rotor; reproduced from [13]](image)

While open rotor noise is an actively researched topic, there is limited information on tractor propeller installation effects. In the tractor configuration the main tone from the propeller is complemented by interaction noise from the trailing pylon as determined by Block in her series of experiments [44, 45]. In this study a pusher configuration was compared to the tractor configuration as well as an isolated propeller case, as seen in Figure 2.12. The effects of the interaction of the pylon can be noticed with higher measured noise downstream of the propeller in the tractor configuration for the first and second BPF in Figure 2.12b. In the pusher configuration, the majority of the noise is seen in the propeller plane, with the significant higher harmonics as seen from Figure 2.12c. This is attributed to the unsteady loading caused by the impingement of the upstream pylon wake on the propeller [13, 40, 45–48]. In the tractor configuration, the pressure fluctuations on the pylon due to the periodic impingement of the blade tip vortices form a source of interaction noise [49, 50]. More recently Thom utilised the Ffowcs Williams and Hawking model on CFD results of the pressure field generated due to the impingement of the blade tip vortex. The study concluded that the vortex noise was lower than the main tone from the propeller [51].
2.3 Propeller Noise Emissions

Figure 2.12: Propeller directivity trends; reproduced from [44]
Thesis Scope and Objective

An integral assessment of the aerodynamic and aeroacoustic interaction effects occurring for a tailplane mounted tractor propeller is yet not available in open literature. The majority of current studies focus on the aft-fuselage pylon-mounted pusher configuration. The aim of the present study is to quantify the tractor rotor-pylon interactions, focusing on the tailplane and propeller performance as well as the noise emissions. This leads to the formulation of the research aim:

To experimentally investigate the aerodynamic and aeroacoustic installation effects for a tailplane tip mounted tractor propeller, including the effects of elevator deflections.

3.1 Thesis Objectives

From the baseline parameters of the primary aim, a more fundamental secondary objective is realised, that is defined is:

To investigate tractor rotor-pylon interaction noise

In order to achieve the aim of this thesis, a number of research objectives are defined that will be accomplished by:

- Performing a series of wind tunnel studies with quantitative measurements for multiple propeller-pylon spacings and elevator deflections in order to:
  - Assess the upstream and downstream interaction effects on propeller noise and performance
  - Determine the flow field in the propeller slipstream to quantify the impact of the interaction effects.
- Validate the windtunnel data with a numerical analysis of the propeller performance and the downstream aerodynamic interaction effects.
3.2 Thesis Outline

This thesis report is structured in four parts. The first part that was just covered was comprised of the introduction and the formulation of the thesis aim.

The second part of the thesis focusses on the results from the experimental studies conducted. The details of the two experimental models and their setup is discussed in Chapter 4. As two models were employed in the research, the results from each model are presented separately in Chapter 5 and 6, respectively.

In third part of the report deals with the numerical analysis aspect of the study. The setup of the numerical model is first described in Chapter 7, followed by the results of the comparison between the numerical and experimental results in Chapter 8.

The final part of the report contains the conclusions drawn from the study, following which recommendations for further research are given.
Part II

Experimental Analysis
Experimental Methods

“An experiment is a question which science poses to Nature, and a measurement is the recording of Nature’s answer.”

– Max Planck

This chapter discusses the setup of the wind tunnel experiments undertaken to quantify and gain insight into the interaction effects between a tractor propeller and pylon. A brief overview of the wind tunnel facilities utilised is presented in Section 4.1, following which a detailed description of the models used in the test is covered in Section 4.2. The measurement techniques used in the test campaigns, and the post processing techniques is discussed in Sections 4.3 and 4.4.

4.1 Wind Tunnel Facility

All experiments were carried out at the Delft University of Technology Low Speed Laboratory (LSL). For the study, two tunnels were employed, the Low Turbulence Tunnel (LTT) and the Vertical Tunnel (V-Tunnel).

4.1.1 Low Turbulence Tunnel (LTT)

The LTT is an atmospheric closed circuit tunnel known for its low turbulence levels ranging from 0.015% at 20 m/s up to 0.085% at 100 m/s. The tunnel is powered by a 525kW DC motor allowing for a maximum velocity of 120 m/s, but is limited to 100 m/s in practice. The tunnel has a closed octagonal test section which is 1.8 m wide and 1.25 m tall with a length of 2.60 m. A zero static pressure gradient is achieved for the empty test section due to the slightly diverging walls. The tunnel’s low turbulence levels are attributed to the tunnels large contraction ratio of 17.8. A schematic representation of the LTT tunnel facility is given in Figure 4.1.
4.1.2 Vertical Tunnel (V-Tunnel)

The Vertical Tunnel, or V-Tunnel is an open-jet tunnel with a circular outlet of 0.6 m diameter, with a maximum freestream velocity of 45 m/s. An advantage of this tunnel with respect to acoustic measurement is the lack of test section walls, which negates the issues of tunnel wall reflections and scattering inherent in non acoustically treated closed tunnel sections. However, the tunnel outlet is not in an anechoic test hall. A schematic of the V-Tunnel is given in Figure 4.2.

4.2 Experimental Model

In the experimental work discussed in this chapter, two models were utilised. One model was used to study the propeller noise directivity patterns as a function of propeller-pylon spacing and elevator deflections. The second model was used to determine the downstream slipstream interaction effects on the pylon. The two models used in the study were identified as:

1. The VARiable Propeller-Wing model, the VarPW
2. The modified PROpeller Wing Interference Model, the Prowim 2.0

The VarPW model gets its name as the modular model allows for different pylon models. The model is described in more detail in Subsection 4.2.1. The Prowim 2.0 model was analysed in two test campaigns spanning over two tunnels, the details of which are outlined in Subsection 4.2.2.
4.2 Experimental Model

4.2.1 VarPW Model

This model was a modular setup, consisting of two components. The propeller unit contained the drive mechanism and supporting structure for the propeller. This unit is connected to the propeller via a variable length shaft. The second component was the pylon model. A key feature of this model was the ability to change the spacing between the propeller and the pylon while maintaining all other operating conditions constant, thus reducing variability between measurements. This was achieved by mounting the pylon model on a linear traverse.

4.2.1.1 Propeller Unit

The propeller unit was adapted from a pre-existing model used for prior studies. The model consisted of a motor unit coupled to force and torque sensors housed in a streamlined 3D printed fairing. A 150 W Maxon RE 40 DC motor powered a two bladed APC 9x6 propeller of diameter 0.228 m. The motor unit was connected to the propeller via an interchangeable carbon fibre tube and shaft system with an outer diameter of 0.02 m. Two shafts were used for this test, one of length 0.22 m referred to as the ‘short’ shaft configuration and the second shaft of length 0.40 m referred to as the ‘long shaft’ configuration. A schematic design of the model is shown in Figure 4.3. This model was originally designed to be used in a conventional horizontal tunnel, and hence the weight of the shaft is balanced with a calibrated counter weight. When mounted vertically in the V-tunnel this counter weight was replaced by a pulley system with a counter weight to balance the weight of the model.
4.2.1.2 Pylon Model

The pylon model had an untapered, unswept planform with a symmetrical NACA 0012 profile. The model had a chord of 150 mm with a span of 450 mm. A cut-out for the propeller model shaft was provided to mimic the radially located nacelle pylon junction. The pylon was equipped with an elevator with its hinge point at 75% of the chord. The pylon dimensions were chosen based on previous propeller interaction studies\(^1\), while also facilitating a wide range of propeller-pylon spacings. The propeller-pylon spacing could be varied between 10% to 110% of the propeller diameter for the long shaft configuration and 10% to 50% for the short shaft configuration. The pylon model was 3D printed out of laser sintered polyamide and sanded to obtain a smooth surface. A technical drawing of the pylon model is provided in Figure 4.4, while the overview of the test setup of the model in the V-Tunnel is depicted in Figure 4.5.

<table>
<thead>
<tr>
<th>Chord</th>
<th>Span</th>
<th>Taper Ratio</th>
<th>Sweep Angle</th>
<th>Airfoil</th>
<th>Elevator chord</th>
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<td>(b)</td>
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</tr>
</tbody>
</table>

\(^1\)For example see references [13, 29, 31, 38, 45]
4.2 Experimental Model

4.2.2 The Prowim 2.0 Model

The Prowim 2.0 model is derived from the half span model used in prior propeller-wing interaction studies. The old model, referred to as the ‘PROWIM’ (PROpeller Wing Interaction Model) consisted of a conventional mid span mounting of the propeller [28]. This model was later modified to facilitate a tip mounted propeller model by designing a new nacelle and a modified mounting mechanism low aspect ratio configurations for UAVs [23].

The motor unit is a compact 5.5kW three-phase induction motor that is housed in a
70 mm diameter nacelle. An optical encoder with 200 pulses per revolution is mounted on
the motor. The rotational speed of the motor is regulated by an external motor control
system that also monitors vital temperature and voltage parameters of the system.

A four bladed propeller is used with a diameter of 0.236 m. The propeller blade is based
on a 1:11 scaled version of the Hamilton Standard 2D30 237 propeller used on the De
Havilland Canada DHC-2 Beaver aircraft [53]. During the tests, the blade angle at 75%
of the blade radius ($\beta_{3/4}$) was set to 23°. As mentioned earlier, the PROWIM 2.0 model
was utilised in the LTT as well as the V-tunnel. An image of the model mounted in the
LTT is given in Figure 4.6 and the mounting details of the model in the V-Tunnel is given
in Figure 4.11.

![Balance Reflection Plane](image)

**Figure 4.6:** The PROWIM 2.0 model mounted in the LTT.

The pylon consisted of an unswept, rectangular planform with no twist. The airfoil section
of the pylon was the symmetrical NACA 642 - A015. The pylon had a cord of 0.240 m,
which was in the order of one propeller diameter. The semi span of the wing was 0.325 m,
which resulted in an aspect ratio of 2.7. The model is equipped with an elevator with its
hinge point at 75% of the chord. The spacing between the propeller and the pylon could
be varied by adding an additional element on the nacelle. The closest propeller-pylon
spacing possible was termed the ‘short’ configuration, which had a spacing of 43% of the
propeller diameter. The largest propeller-pylon spacing was called the ‘long’ configuration
and had a spacing of 85%.

The PROWIM 2.0 model is equipped with 408 orifices for surface pressure measurements.
The orifices are split into eight rows of 51 orifices each. The chordwise distribution of
the orifices is provided in Appendix A.3. The rows of orifices are distributed over the
span of the model, with a denser distribution in the region washed by the propeller
slipstream. The dimensions of the PROWIM 2.0 pylon model are illustrated in Figure 4.7
and summarised in Table 4.2.
The Prowim 2.0 model was used both in the V-Tunnel mounted vertically, as well as in the LTT where it was mounted horizontally. A more detailed explanation of the mounting of the model for the different tests is presented in the forthcoming Section 4.3

Table 4.2: Dimensions of the Prowim 2.0 pylon model

<table>
<thead>
<tr>
<th>Chord $c$</th>
<th>Span $b$</th>
<th>Taper Ratio $\lambda$</th>
<th>Sweep Angle $\Lambda$</th>
<th>Airfoil</th>
<th>Elevator chord $c_e$</th>
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<td>1.0</td>
<td>0</td>
<td>NACA 642 – A015</td>
<td>0.25c</td>
</tr>
</tbody>
</table>

4.3 Measurement Techniques

In order to collect quantitative data from the tests in the campaign, multiple measurement techniques were employed. This section briefly describes the measurement systems utilised in the campaigns, followed by a treatment of the post-processing and data reduction techniques in Section 4.4.

4.3.1 Coordinate System

The purpose of the experimental campaigns was to investigate the interaction effects associated with a horizontal tailplane tip mounted propeller propulsion concept. With this in mind, a suitable model coordinate system was chosen. For these series of tests, the conventional right-handed Cartesian coordinate system having its origin in the center of the propeller plane was utilised. For the propeller noise emission directivity, an azimuthal or circumferential angle $\phi$ was defined, following the convention of the right-hand rule.
The flyover or axial directivity \( \theta \) was defined with respect to the freestream and the \( X \)-axis of the model coordinate system. An illustration of the model coordinate system is depicted in Figure 4.8.

![Diagram of model coordinate system]

(a) Azimuthal circumferential directivity
(b) Flyover axial directivity

Figure 4.8: Model based coordinate system.

4.3.2 Propeller Performance and Pylon Loads Measurements

To determine the installation effects of the pylon on the propeller performance, a measure of the thrust generated by the propeller is required. In order to obtain propeller performance data and pylon loads\(^1\), multiple techniques were employed depending on the tunnel and the model being tested.

The VarPW model was equipped with an integrated load cell and a torque sensor. However, as mentioned earlier in Section 4.2.1, the model was originally designed for horizontal operation. In the V-tunnel, as the model was mounted in a vertical configuration, the weight of the model was beyond the rated limit of the load cell. Hence, a counter balance approach was employed using a pulley system. During initial calibration tests, it was discovered that the load cell was able to determine the loading force, but did not provide repeatable results during unloading due to the friction of the cables and pulley system. Based on the conclusion that there was no repeatability of load measurements, it was decided not to consider the performance (thrust and torque) data in this thesis. The main focus of the VarPW model was fundamentally noise measurements and not performance, therefore the performance (thrust and torque) data of that particular campaign is not treated in this thesis.

As the tests with the Prowim 2.0 model spanned across both the LTT and the V-tunnel, separate force measurement systems were employed in each tunnel to obtain the propeller performance. In order to obtain the downstream pylon-slipstream interaction effects, the pylon loads were measured in the test campaign in the LTT with the Prowim 2.0 model as described in the next subsection.

\(^1\)For the Prowim 2.0 model tests in the LTT
4.3 Measurement Techniques

4.3.2.1 LTT

A six component external balance is integrated into the LTT facility as seen in Figure 4.1. The model is connected via a turntable mount that serves as the reflection plane. The balance has the ability to rotate in the horizontal plane, resulting in a change in angle of attack for half wing models such as the Prowim 2.0. A schematic of the model mounted in the test section #7 can be found in Figure 4.9a. A more detailed description of the balance and model interfacing system is presented in Appendix A.1.

![Schematic drawing](image1.png)

(a) Schematic drawing

![Actual experimental setup](image2.png)

(b) Actual experimental setup

**Figure 4.9:** The Prowim 2.0 installed in LTT test section #7.

4.3.2.2 V-Tunnel

For the vertical tunnel, a custom single-component force measurement system was developed. The entire model was mounted on two flexures which arrested its motion in the direction of the other two axes. The whole model was then clamped via a tension load cell to the supporting strut structure. A schematic representation of this measurement system is provided in Figure 4.10, along with an image of the model mounted in the V-Tunnel in Figure 4.11. The load cell chosen was the Scaime ZFA25 with a maximum load rating of 245 N and a maximum combined error of $\pm 0.03\%$ the rated capacity. This translated into a maximum error of $\pm 0.073$ N. The load cell was found to show repeatable loading and unloading results during static testing prior to the experiment.

4.3.3 Pylon Surface Pressures

To investigate the slipstream interactions at the pylon, surface pressure measurements were performed in the LTT test campaign. The Prowim 2.0 model, as mentioned earlier in Subsection 4.2.2, was equipped with eight rows of surface pressure orifices. A DTC Initium pressure scanning system from Esterline Pressure Systems was employed to record the pressure data from orifices. The 32HD scanner with a 1 psi range was selected. All eight of the spanwise rows were merged into one tube per chordwise position coming out of the model. This meant, to acquire an individual row’s pressure distribution, the rest of the seven pressure rows were sealed for the run. For each pressure row measurement, readings were taken for 30 seconds at a sampling rate of 5 Hz.
4.3.4 Propeller Acoustic Measurements

In order to determine the acoustic interaction noise of the propeller, two type of microphones measurements were employed. Individual discrete microphones measurements, and a beamforming array.

4.3.4.1 Discrete Microphone

Noise measurements were carried out using five LinearX M51 microphones, positioned at different directivity locations with respect to the propeller center. The microphones are operated at 9 V DC and are of the electret condenser design. The M51 has an acoustic sensitivity of $-36$ dB m/Pa and a maximum rated Sound Pressure Level (SPL) of 150 dB in an operating range of 10 Hz to 40 kHz. The M51 has a flat response in the 50 to 2,000 Hz range, while error response corrections are provided to correct for the response outside this range. The microphone has a completely omnidirectional directivity for frequencies up 5,000 Hz. To map the voltage response of the microphone into a pressure response, a piston phone (model G.R.A.S 42AA) was employed to calibrate the microphones on
4.3 Measurement Techniques

Figure 4.11: The PROWIM 2.0 mounted in the V-tunnel.

each measurement day. The analogue data from the microphones was sampled at 50 kHz, resulting in a Nyquist frequency well above the frequency range of interest.

4.3.5 Acoustic Array

In addition to discrete microphones, an array microphones was also employed in the test campaigns in the V-Tunnel. The array consists of PUI Audio POM-2735P-R microphones which have an acoustic sensitivity of $-35\pm2 \text{ dB m/Pa}$. Data acquisition for the array was handled with a custom built DAQ system consisting of an analogue to digital converter fed via a signal amplifier. For these tests, the low amplification option was chosen, resulting in a maximum measurable SPL of 122 dB. The signal is sampled at a rate of 50 kHz and fed through a low pass filter set at 40 kHz. The filtered digital signals are then processed by a Digital Signal Processor consisting of a Field-Programmable Gate Array and a real time controller. The FPGA digitises the analogue signal into 16 bit signed integers which are then transferred to the recording computer via the RT controller and a dedicated LabVIEW application on the host computer. Given the tonal nature of propeller noise, the interaction effects occur mainly at the blade passage frequency and its higher harmonics. For the VARPW model, the blade passage frequency at the lowest
considered advance ratio of $J = 0.85$ and a freestream velocity $U_\infty$ of 20 m/s is given by:

\[ n_{BPF} = n \frac{N \omega}{2\pi} = n \frac{N U_\infty}{D J} \]  
\[ n_{BPF} = n \frac{2 \times 20}{0.228 \times 0.85} = n \times 206 \]  

Given that the fundamental tone ($n = 1$) is at approximately 207 Hz for the VarPW model, the application of beamforming at that frequency range and model dimensions results in very poor spatial resolution [54, 55]. The resolution for conventional beamforming can be estimated using:

\[ Y_{res} = \frac{425 Y}{D_{ary} BPF} \]  

Where $Y$ is the distance between the array and source and $D_{ary}$ is the equivalent diameter of the beamforming array. For a conventional logarithmic spiral array pattern, the resolution was estimated as:

\[ Y_{res} = \frac{425 \times 0.7}{0.49 \times 206} = 2.9 \text{ m} \]  

Given the resolution of almost 3 m, instead of the beamforming array, each of the 64 array microphones were utilised as individual microphones to cover a wider directivity range leading to eight different circumferential directivity ($\phi_{mic}$) angles for eight different axial directivity angles ($\theta_{mic}$).

Similarly, for the Prowim 2.0 model at the lowest advance ratio of $J = 0.65$ and four blades, the blade passage frequency is given by

\[ n_{BPF} = n \frac{4 \times 20}{0.236 \times 0.65} = n \times 521 \]  

For the Prowim 2.0 model, although the resolution was determined to be poor for the fundamental frequency, the resolution for the 1\textsuperscript{st} harmonic seemed promising. However, it was difficult to predict the capabilities of the beamforming array prior to testing with the model.

Nevertheless, the array was employed using a conventional logarithmic spiral pattern. The results from the array, given the current delay sum processing technique, however did not result in source location with viable resolution and are hence not presented in this thesis work.

### 4.3.6 Propeller Slipstream Flowfields

One of the challenges faced in propeller-pylon interaction studies is gaining information on the flowfield in the propeller slipstream. The majority of the previous experimental studies\(^1\) utilised intrusive measurement techniques such as traversing a pitot tube in the

\(^1\)For example see references [23, 38, 56, 57]
slipstream. With PIV, the velocity components of the flow field can be captured non-intrusively. The fluid flow can be tracked by introducing tracer particles, and recording their motion.

For the test campaign with the Prowim 2.0 model in the V-Tunnel, a stereoscopic setup was utilized comprising of two LaVision Imager Pro LX 16 Mp cameras. These are 16 Mpix (4872x3248) 12 bit cameras recording at a maximum frequency of 0.8 Hz. The light source was a Quantel Twins Nd:Yag laser, which provides a 200mJ@10Hz laser beam of 35 mm diameter. Optics were used to focus the laser beam into a laser sheet of 3mm thickness. The seeding was provided by SAFEX-Inside-Nebelfluid which is a mixture of dyethylene-glycol and water, that generates tracer particles with a diameter of about one micron. The seed particles were introduced into the flow via the SAFEX Twin Fog machine. The cameras were connected with Nikon lenses of 105 mm focal length with numerical apertures of f#11 and f#8. Adapters to tilt the lens to change the focal plane of the cameras to achieve the Scheimpflug condition\(^1\) were utilised. A schematic of the arrangement of camera setup is illustrated in Figure 4.12a and along with an illustration of the camera in Figure 4.12b.

![Figure 4.12: Details of the PIV setup for the Prowim 2.0 model in the V-Tunnel.](image)

The cameras were calibrated using a 300 × 300 mm calibration plate and the final calibrated field of view was in the order of 335 × 287 mm.

Two PIV planes were utilised for both the ‘short’ and the ‘long’ configurations of the Prowim 2.0 model. An inflow plane was positioned ahead of the propeller to capture upstream interference effects, while a downstream plane in the propeller slipstream was used to obtain the slipstream flowfield. For each configuration, the inflow plane was situated 8 mm ahead of the model spinner, and the slipstream plane was positioned 25 mm

\(^{1}\)See references [58, 59] for further information
in front of the leading edge of the pylon model. These were the minimum offset distances from the model to avoid flares in the captured images from the reflections of the laser sheet from the model. An illustration of the position of these PIV planes is provided in Figure 4.13.

![PIV planes illustration](image)

**Figure 4.13:** Location of PIV planes for the PROWIM 2.0 model.

### 4.4 Measurement Processing, Corrections and Data Reduction

The data acquired during the campaigns require post processing before analysis of the results is possible. This section provides a description of the methods utilised for each measurement device.

#### 4.4.1 Performance Data

In order to determine the thrust of the propeller from the measured axial force, a bookkeeping method was employed. For a given operating point, two measurements are taken: one measurement without the propeller mounted, and the second with the thrusting propeller present. Thus the two measured forces are given by:

\[
F_{\text{prop off}} = D_\infty \\
F_{\text{prop on}} = D_\infty + D_{\text{intf}} - T
\]

The measured force of the model without the propeller mounted determines the drag of the entire model, \(D_\infty\), for a given operating condition. The force measured with the thrusting propeller includes an additional slipstream interaction drag \(D_{\text{int}}\), which is the incremental drag due to the additional dynamic pressure and the local change of the angle of attack in the propeller slipstream. Given the relatively small area wetted by the slipstream, the interaction drag contribution is considered negligible. Hence, the thrust is determined by:

\[
T = F_{\text{prop off}} - F_{\text{prop on}} + D_{\text{int}}
\]
\[ T \approx F_{\text{prop off}} - F_{\text{prop on}} \] (4.10)

For the Prowim 2.0 model, given the relative size of the propeller diameter to the tunnel outlet, the thrusting propeller changes the inflow velocity from the tunnel provided freestream velocity. The propeller due to upstream inflow contraction, entrains ambient air from the tunnel plenum [60]. The contraction of the propeller inflow stream tube can be modelled as a function of the propeller area \( S_p \) and the thrust setting \( T_C \). For an open jet tunnel such as the V-Tunnel, the inflow stream tube is the tunnel jet, therefore, the cross-sectional area of the jet, \( S_{\text{tun}} \) is utilised to obtain the contraction of the inflow by:

\[ S_{\text{tun}} - S_p = S_p \left( \sqrt{1 + \frac{8}{\pi} T_C} - 1 \right) \] (4.11)

The strength of the sink is then determined from the contraction as:

\[ Q = U_\infty S_p \left( \sqrt{1 + \frac{8}{\pi} T_C} - 1 \right) \] (4.12)

The actual inflow for the propeller can be then approximated as a weighted average of the air that exits the wind tunnel, and the entrainment of the ambient air and is given by:

\[ U_{\text{in}} = U_\infty \frac{S_{\text{tun}}}{S_{\text{tun}} + S_p \left( \sqrt{1 + \frac{8}{\pi} T_C} - 1 \right)} \] (4.13)

Where \( U_{\text{in}} \) is the propeller inflow velocity and \( S_{\text{tun}} \) and \( S_p \) are the areas of the tunnel jet and propeller disk respectively. Once the propeller inflow is known, the propeller thrust can be evaluated using the actuator disk theory from the inflow and slipstream axial velocity as:

\[ T = \dot{m}(U_{ss} - U_{\text{in}}) \] (4.14)

Equation 4.14 is also utilised to determine the propeller thrust using the slipstream velocities determined by the PIV analysis.

### 4.4.2 Pressure Data

The raw pressure data obtained from the Prowim 2.0 model was converted into pressure coefficients \( C_p \) by normalising with the corrected free stream dynamic pressure. The Prowim 2.0 model has no pressure port at the trailing edge, hence the most extreme pressure port data from the upper and lower surface is extrapolated and the mean of this value is utilised to determine the \( C_p \) at the trailing edge. The spanwise lift and drag distribution was obtained by using the chordwise pressure distribution to approximate the section lift and drag coefficient using the relation:

\[ c_l \approx \cos \alpha \int_0^1 (C_{p\text{up}} - C_{p\text{lw}}) d\left( \frac{x}{c} \right) - \sin \alpha \int_0^1 (C_{p\text{up}} - C_{p\text{lw}}) d\left( \frac{y}{c} \right) \] (4.15)
\[ c_d \approx \sin \alpha \int_0^1 (C_{pu} - C_{pl}) d \left( \frac{x}{c} \right) + \cos \alpha \int_0^1 (C_{pu} - C_{pl}) d \left( \frac{y}{c} \right) \]  \hspace{1cm} (4.16)

Numerical integration using the trapezoidal rule was implemented in order to perform the above integration from the chordwise pressure coefficient distribution experimentally obtained. A wake rake was not utilised during the surface pressure measurements, and hence the corrections for the following effects are not accounted for:

- Model wake blockage
- Lift interference
- Model wake blockage
- Tunnel wall effects

4.4.3 Microphone Data

The raw voltage levels from the microphone were calibrated on each measurement day using a piston phone. The known reference signal of the piston phone was first converted to a RMS pressure level, and then compared to the RMS value of the measured voltage levels of the microphone. This ratio determines the calibration factor as:

\[ F_{mic} = \frac{P_0 \cdot 10^{\frac{SPL_{cal}}{20}}}{RMS(x - \bar{x})} = \frac{P_{cal}^{rms}}{RMS(x - \bar{x})} \]  \hspace{1cm} (4.17)

The DC offset was subtracted from the raw voltage signal \( x \) by subtracting with the mean \( \bar{x} \). The voltages of the each measured run where thereafter converted to calibrated pressure levels using:

\[ x_{cal} = x F_{mic} \]  \hspace{1cm} (4.18)

The pressure levels were then transformed from time domain signals to frequency domain signals using Welch’s method [61]. This results in the Power Spectral Density (PSD) of the pressure levels as a function of the frequency. This method divides the input time domain signal into windows (with or without overlap), Fourier transforms each window and the peridogram for each window, and finally averaging the results over number of windows. For the measurements, a rectangular window with a length such as to obtain a frequency resolution \( \Delta f \) of 1.5 Hz was utilised with no overlap. The resulting sound pressure levels are determined from the PSD by:

\[ SPL(x_{cal}) = 10 \log_{10} \left( \frac{P(x)\Delta f}{P_0^2} \right) \]  \hspace{1cm} (4.19)

Since the measurements were conducted in an open-jet facility, there is a change in sound pressure level and the propagation angle as the sound waves get refracted through the tunnels shear layer. In order to account for this effect, Goldstein’s [62] corrections were applied to correct the axial directivity angles, and the change in SPL. The sound pressure levels were further scaled to a reference distance of 1 m.
4.4.4 Particle Image Velocimetry Data

For each PIV plane considered, prior to measurements, the PIV system was calibrated. This involved using a known calibration object to obtain a mapping function which determines the correspondence between the captured image and physical object. The multi level target model [63] that assumes the view is obtained through a pinhole was utilised for the calibration. Once the mapping function is obtained, the physical calibration is complete. In practice, the laser sheet is not perfectly aligned with the calibrated object plane. To account for any misalignment of the laser sheet with the object plane, a disparity correction is applied. This procedure utilises the original physical calibration on images of seeded flow. From these images, a cross-correlation is applied and a disparity vector map is generated. From the disparity map, a correction function is obtained and applied to the original physical calibration. Thus, the field of view of each camera is de-warped (physical calibration) and corrected (self-calibration) to obtain the positions of the particles in the laser sheet. Given the close proximity of the PIV plane to the model, there were strong flares present in the captured images stemming from reflections of the laser from the pylon. In order to reduce the effects of these flares, the captured images were pre-processed using techniques to increase the contrast for the easier identification of the seeding particles [64, 65]. A simplified representation of this procedure is illustrated in Figure 4.14 and the steps followed are briefly outlined:

1. Extraction of the peaks: the minimum intensity value over all snapshots recorded was subtracted from each individual instantaneous image to obtain only the peaks.

Figure 4.14: Pre-processing raw images for contrast enhancement.
2. Intensity normalisation: the individual instantaneous images are normalised by the average of all snapshots and then multiplied by a constant factor of 100.

3. Sliding minimum subtraction: the minimum of a sliding window of $22 \times 22$ pixels was subtracted to obtain the final processed image for cross-correlation.

Once the captured images are processed, cross-correlation with a $16 \times 16$ pixel window with 50% overlap was performed to obtain the velocity components of the flow.
Experimental Results: VarPW Model

"Before the result of a measurement can be used, it must be interpreted—nature’s answer must be understood properly.”

– Max Planck

The VarPW campaign focused entirely on propeller-pylon interaction noise. The aim of the experimental campaign was to parametrically study the propeller noise emissions as a function of propeller-pylon spacing and elevator deflections. Thus, for each propeller-pylon spacing, multiple runs for each elevator deflection and advance ratio were carried out. A synopsis of all the operating points of the campaign is initially presented in Section 5.1. The repeatability of the microphone measurements is assessed in Section 5.2 following which the results are discussed. The data from the VarPW model study are grouped into results for the isolated propeller in Section 5.3, followed by the effects of propeller-pylon installation in Section 5.4.

5.1 VarPW Test Matrix

A summary of the operating conditions considered for the experimental measurements is presented in Table 5.1. During this experimental campaign, the angle of attack and sideslip were set to zero degrees. The tests were performed at multiple advance ratios by maintaining the freestream velocity constant while varying the rotational speed of the propeller.

All the tonal level results were acquired with a measurement time of 30 s. The reader is reminded of the microphone locations which are defined as given in Figure 5.1. For the circumferential directivity, the microphones that are located in the directivity towards the positive deflection of the elevator are indicated in red, and the negative deflection of the elevator in green. The directivity in the propeller plane ($\phi = 180^\circ$) is indicated in blue. For the axial directivity, the upstream positions are further demarcated in orange, and downstream locations in cyan.
### Table 5.1: Test matrix for the V-tunnel campaign with the VarPW model.

<table>
<thead>
<tr>
<th>Configuration</th>
<th>Velocity $U_\infty$ [m/s]</th>
<th>Advance Ratio $J$ [-]</th>
<th>Pylon Spacing $\Delta X$ [-]</th>
<th>Elevator Deflection $\delta_e$ [deg]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Short shaft</td>
<td>20</td>
<td>0.93</td>
<td>NaN, 0.1$D$ to 0.5$D$</td>
<td>0, ±10, ±20, ±30</td>
</tr>
<tr>
<td></td>
<td></td>
<td>0.85</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Long shaft</td>
<td>20</td>
<td>0.93</td>
<td>NaN, 0.1$D$ to 1.1$D$</td>
<td>0, ±10, ±20, ±30</td>
</tr>
<tr>
<td></td>
<td></td>
<td>0.85</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

(a) Microphone circumferential distribution

(b) Microphone axial distribution

Figure 5.1: Schematic of microphone positions.

### 5.2 Repeatability and Reproducibility of Acoustic Measurements

The acoustic data presented in this section were first assessed for the quality of the signal acquired by the microphones.

To gauge the repeatability of the microphone signals, data were recorded at constant operation conditions. From the resulting spectrum data, the sound pressure levels of the first five integer multiples of the blade passage frequency were extracted for all five microphones. As a measure of repeatability, the difference between the mean sound pressure

---

$\delta_e$ = 30°

---

$\phi$ = 270°

---

$\phi$ = 180°

---

$\phi$ = 90°

---

$\theta$ = 90°, $\phi$ = 270°

---

$\theta$ = 90°, $\phi$ = 90°

---

$\theta$ = 47°, $\phi$ = 90°

---

$\theta$ = 135°, $\phi$ = 90°

---

$\theta$ = 90°, $\phi$ = 90°

---

1Ratio of the propeller diameter $D$, in steps of 0.1 with NaN indicating no pylon.
level and each sound pressure level at the first four harmonics for all microphones considered are presented in Figure 5.2. The relatively larger variation for the tonal levels at the 4th and 5th blade passage frequency is due to the fact that given the operating conditions of the VARPW model, the amplitudes of the higher harmonics were comparable to the amplitude of the background noise in the tunnel. Hence, identification of these harmonics does not result in repeatable results. Nevertheless, the fundamental tone (1BPF) shows a good repeatability of within ±0.5 dB for all five microphones.

![Figure 5.2: Repeatability of successive propeller noise measurements with discrete microphones. Sample readings for \( J = 0.85, \Delta X = 0.1D, \delta_c = -30^\circ \).](image)

For the 64 element grid microphones, the repeatability was assessed in a similar fashion during the same runs. Only the results of the fundamental tone are illustrated in Figure 5.3. The grid microphone at \( \theta_{\text{mic}} = 90^\circ, \phi_{\text{mic}} = 270^\circ \) was found to be malfunctioning, hence the data recorded using this microphone are omitted from the subsequent results.

### 5.3 Isolated Propeller Configuration

This section discusses the noise emissions of the isolated configuration of the propeller, hence without the pylon. The isolated propeller produces noise due to linear thickness and loading as summarised in Section 2.3. The measurements from this configuration serve as the baseline for further comparisons between the different installed pylon configurations.

#### 5.3.1 Sound Frequency Spectrum

The isolated propeller sound spectrum for one sample case at an advance ratio of \( J = 0.85 \) for the short configuration of the VARPW model is presented in Figure 5.4. The figure depicts the output for the microphone located at an axial flyover directivity \( \theta_{\text{mic}} = 90^\circ \) and a circumferential directivity of \( \phi_{\text{mic}} = 180^\circ \). The blue markers represent the peak sound pressure levels at blade passage frequency and its harmonics, while the red vertical dashed lines indicate the frequency of these tones. The tones generated by the tunnel fan
Experimental Results: VarPW Model

Figure 5.3: Repeatability of successive propeller noise measurements for the 1st BPF with grid microphones. Sample reading for $J = 0.85$, $\Delta X = 0.1D$, $\delta_e = -30^\circ$.

For the given tunnel freestream velocity $V_\infty$ of 20 m/s are indicated by the black vertical horizontal lines at 300 Hz and 718 Hz. The green marker indicates the rotational frequency of the model motor. Since the VarPW model utilises a two bladed propeller, the rotational frequency of the model motor coincides with half the blade passage frequency. The spectrum is not corrected for shear layer effects or normalised to the reference radius.

Figure 5.4: Isolated propeller sound spectrum. Reading for $J = 0.85$.

From Figure 5.4, it becomes apparent that the fundamental tone (at the BPF) is dominant with an SPL of about 80 dB. The first two harmonics (2nd and 3rd BPF) are significantly lower in amplitude at around 58 dB and 48 dB. While the level of the 2nd BPF is about
10 dB higher than the background noise, the level 3rd BPF is not significantly higher than the background noise. Due to the largely tonal nature of the sound spectrum, the analysis shifts to the investigation of the individual tonal levels which are present at frequencies equal to multiples of the blade passage frequency. The tone caused by the model motor and its associated vibrations is identified with a level of about 70 dB.

5.3.2 Tonal Noise Levels

The tonal levels at each multiple of the blade passage frequency were extracted from the frequency spectra, following which the data was corrected for the tunnel jet shear layer and normalised to a microphone radius of one meter \( R = 1 \text{ m} \). This procedure was followed for all five microphones.

The data from three propeller loading cases were initially analysed: a no load case \( J = 1.07 \), a lightly loaded case \( J = 0.93 \) and a highly loaded case \( J = 0.85 \). The measured levels of the propeller noise as a function of these advance ratios can be seen in Figure 5.5.

The plot illustrates that the propeller noise increases with decreasing advance ratio. An increase of the tonal noise levels for all directivity angles between the loaded propeller \( J = 0.93, 0.85 \) compared to the unloaded propeller \( J = 1 \) is apparent from these results. This is due to the presence of loading noise, which results from the movement of the pressure field around the blade as it rotates. Hence, for the higher thrust cases, there is more lift at the blade sections, causing larger pressure fluctuations, thus higher sound levels. The small difference between the lightly loaded and the highly loaded case is reasoned to be due to the vibrations of the propeller shaft. The axial directivity pattern resembles that
of an isolated propeller: the peak levels are found in the propeller plane, with declining levels in either axial directions. However, for the furthest downstream directivity angle ($\theta_{\text{mic}} = 135^\circ$) there are deviations at the higher advance ratios considered. This is due to the higher tunnel background noise at this microphone position, leading to a low signal-to-noise ratio. This effect is more pronounced for higher advance ratios, as the tonal levels reduce.

The dominance of the fundamental tone at the blade passage frequency was evident from the sound spectrum measured in the propeller plane ($\theta_{\text{mic}} = 90^\circ$) as shown in Figure 5.4. In order to assess the contribution of the higher harmonics to the total sound pressure level, the levels of the first three harmonics are plotted for five axial directivity angles in Figure 5.6.

![Figure 5.6: Propeller tonal noise for the isolated propeller. Reading for $J = 0.85$, short configuration.](image)

For the same advance ratio, the dominance of the fundamental tone is seen clearly across all directivity angles. The increase in the levels at $\theta_{\text{mic}} = 135^\circ$ at the lower BPFs is again noticed due to a low signal-to-noise ratio. The difference in levels between the fundamental tone and the harmonics are in the order of around 25 dB. The large difference makes the contribution of the harmonics to the total sound pressure level negligible. Given this large difference was witnessed across all the advance ratios tested for both the long and the short configuration of the model, it was concluded that the contribution of the harmonics to the total SPL of the isolated propeller is negligible. Therefore, it was decided to only consider the fundamental tone (1BPF) in further analysis for the isolated propeller case.

The results discussed thus far investigated different axial directivity angles, for a circumferential angle of $\phi_{\text{mic}} = 180^\circ$. For other azimuthal directivity angles, a grid of microphone was employed as discussed in Section 4.3. Tunnel jet shear layer corrections and normalisation are applied for these measurements. The tonal levels of the isolated
propeller measured by the grid microphones are presented in Figure 5.7. The X-axis of the figure represents the axial directivity angles $\theta_{\text{mic}}$, while the circumferential directivity angles $\phi_{\text{mic}}$ are considered along the Y-axis. The contour plot can be visualised as an unwrapped cylinder, with the two extremes of the Y-axis the parting line along which the cylinder is unwrapped.

![Contour plot](image)

**Figure 5.7:** Isolated propeller tonal noise levels of the blade passage frequency measured by grid microphones. Reading for $J = 0.85$, short configuration.

From Figure 5.7, it is clear that the isolated propeller noise is most prominent in the propeller plane and upstream of the propeller plane. The measured noise downstream ($\theta_{\text{mic}} > 90^\circ$) is approximately 4 dB lower than the measured noise upstream ($\theta_{\text{mic}} < 90^\circ$). The integrated sound pressure level for these directivity angles was found to be 84 dB for the isolated propeller case. The lower tonal levels for the $\phi_{\text{mic}} = 45^\circ$ directivity are attributed to the calibration/data acquisition system. It was found that one channel of the data acquisition system was clipping the amplitude of the measured signal. The measured data levels were below this clipping limit, but the piston phone calibration level was clipped, thus resulting in lower calibration levels which in turn effects the measured data levels. However, as all subsequent discussions compare different operating points measured by the same system, the relative trend between the different operating points is not significantly affected. Compared to typical isolated propeller, the levels across circumferential directivity show non constant levels. This could be associated with the oscillatory vibrations occurring from the unsupported propeller shaft. This would lead to a change in the lift generated by each blade, hence an unsymmetrical loading on the propeller, leading to lobes in the circumferential directivity angles.
5.4 Installed Propeller Configuration

The installation of the pylon behind the propeller significantly influences the propeller noise levels along with the directivity. These changes are caused due to the following interaction mechanisms:

- The downstream interaction of the pylon.
  - Reflection/diffraction/scattering of the noise field by the pylon.
  - Blade tip vortex impingement noise.
- The upstream interaction of the pylon, which changes the propeller loading noise.

A comparison of the spectra for the installed and isolated propeller is presented in Subsection 5.4.1. This is followed by a more detailed analysis of the tonal levels as a function of propeller pylon spacing and elevator deflections in Subsection 5.4.2 and 5.4.3 respectively.

5.4.1 Sound Frequency Spectra

To investigate the effects of the installation of the pylon, the installed propeller sound spectra for the axial directivity in the propeller plane ($\theta_{mic} = 90^\circ$) is compared to spectrum of the isolated propeller case in Figure 5.8. The spectra are plotted for three cases: the isolated propeller, along with the installed pylon at two spacings of $\Delta X = 0.1D$ and $\Delta X = 0.5D$. For the short shaft configuration of the VarPW model these spacings represent the closest and furthest viable propeller-pylon spacings. The isolated propeller spectrum and the notations of the markings are the same as used previously in Figure 5.4.

![Figure 5.8](image)

**Figure 5.8:** Comparison of sound spectra for the isolated and installed configuration ($\Delta X = 0.1D, \Delta X = 0.5D$). Reading for $J = 0.85, \delta_e = 0^\circ$, short configuration.

Figure 5.8 reveals that for both the installed and isolated propeller case, the fundamental tone dominates the propeller noise emissions, with no significant change in the broadband
noise levels due to pylon installation. The effects of the installation of the pylon are witnessed, with increased levels of the harmonics when compared to the isolated propeller case. The closest pylon spacing \((\Delta X = 0.1D)\) displays significantly higher harmonic levels in comparison to the furthest pylon spacing \((\Delta X = 0.5D)\). The higher levels of the harmonics are attributed to the distortion of the inflow caused by the potential effects of the installation of the pylon for the close propeller-pylon spacing configuration. The distortion results in unsteady blade loading, which results in additional noise at multiples of the BPF \[32\]. Since the inflow distortion is asymmetric, the unsteady loading component is witnessed in the even and odd harmonics. For the larger spacing \((\Delta X = 0.5D)\), the levels of the harmonics at the 3rd and higher BPF are comparable to levels of the isolated propeller. This is expected, as the strength of the distortion due to the upstream potential effects of the pylon reduces for increasing propeller-pylon spacing. The effects of the inflow non-uniformity are discussed in more detail in Subsection 5.4.2. From the sound spectra, it is further assessed that the contribution of the higher harmonics to the total SPL is not significant and is hence neglected in the discussion of the total SPL of the tonal levels.

The presence of sub harmonics, i.e. non integer, half multiples of the blade passage frequency is witnessed for the two installed cases, while not present in the isolated propeller case. In the installed case, the pylon supports the propeller shaft, and hence the level of the fundamental tone at the motor rotational frequency reduces when compared to the isolated case. However, there is an increase in the harmonics of this tone (which occurs at the sub harmonics of the BPF) for the installed propeller configuration and are reasoned to originate from the pylon-shaft mounting vibrations. This however can not be confirmed from the present measurement data.

### 5.4.2 Tonal Noise Levels: Propeller-Pylon Spacing Effects

In order to investigate the effects of the trailing pylon on the propeller noise emissions, measurements were taken for multiple propeller-pylon spacings while keeping the tunnel and motor parameters constant. To further reduce the variability of the data, multiple measurements were taken for each pylon spacing, which were averaged over and presented here.

The tonal levels were extracted from the spectrum data and corrected for the tunnel shear layer and normalised to a constant observer position as discussed previously. The discussions of the results focus on two main regions of the circumferential directivity angle: inclined to the pylon \((40^\circ \leq \phi_{mic} \geq 150^\circ \text{ and } 215^\circ \leq \phi_{mic} \geq 315^\circ)\) as measured by the grid microphones, and in the pylon plane \((\phi_{mic} = 180^\circ)\) as measured by the discrete microphones.

The levels of the fundamental tone measured at circumferential directivity angles inclined to the pylon are depicted in Figure 5.9 for the closest and largest propeller-pylon spacings of \(\Delta X = 0.1D\) and \(\Delta X = 0.5D\).

For the closest propeller-pylon spacing \((\Delta X = 0.1D)\) in Figure 5.9a, comparing the circumferential directivity angles, the peak tonal levels are obtained for an azimuthal angle of \(\phi_{mic} = 215^\circ\) and had a mean level of about 3 dB higher than the isolated propeller (Figure 5.7). When considering the axial directivity angles, the peak level is observed
Experimental Results: VarPW Model

(a) $\Delta X = 0.1D$

(b) $\Delta X = 0.5D$

Figure 5.9: Propeller tonal noise levels measured by grid microphones. Reading for $J = 0.85$, $\delta_e = 0^\circ$, short configuration.

in the propeller plane ($\theta_{\text{mic}} = 90^\circ$) with an approximate 3 dB increase over the isolated propeller case. Additionally when compared to the tonal levels of the isolated propeller, higher levels are noticed downstream of the propeller as well. Thus, along with the increased levels due the installation of the propeller, there is also increased noise radiations towards the propeller axis downstream. In comparison, when observing the tonal levels of the largest spacing ($\Delta X = 0.5D$) plotted in Figure 5.9b, the levels observed for the downstream axial directivity angles are found to be comparable to the isolated propeller case. As a measure of the total sound pressure level for these circumferential directivity angles inclined to the pylon, the integrated SPL was evaluated and plotted against the propeller-pylon spacing in Figure 5.10.

Figure 5.10: Effect of propeller pylon spacing on the total SPL for circumferential directivity angles inclined to the pylon. Reading for $J = 0.85$, $\delta_e = 0^\circ$, short configuration.
The SPL corresponding to the closest pylon spacing ($\Delta X = 0.1D$) was determined to be about 4 dB higher than for the isolated propeller configuration. On the other hand, the integrated SPL level for the largest propeller-pylon spacing ($\Delta X = 0.5D$) had a level approximately equal to that of the isolated propeller configuration. The results indicate that as the propeller-pylon spacing is reduced, the interaction noise for circumferential directivity angles inclined to the pylon increases.

In order to determine the noise penalty due to the installation of the pylon, the isolated propeller tonal levels are subtracted from the closest and furthest spacing. The resulting tonal levels are plotted in Figure 5.11. For the closer propeller-pylon spacing ($\Delta X = 0.1D$) seen in Figure 5.11a, there are significantly higher levels both upstream and downstream of the propeller especially for circumferential directivity perpendicular to the pylon ($\phi_{mic} = 90, 270^\circ$). Downstream of the pylon, the higher levels are noticed across all the circumferential directivity. In comparison, for the larger spacing ($\Delta X = 0.5D$) seen in Figure 5.11b, there is no significant penalty due to installation of the pylon behind the propeller.

From the tonal levels discussed thus far, an initial observation is made: the installation of the pylon increases the tonal levels and the total SPL as a function of the propeller-pylon spacing, across the directivity measured. For the largest propeller-pylon spacing, the installation penalty is negligible, with levels comparable to the isolated propeller case. Block [44, 45] in her research on tractor-pylon interaction noise, observed similar higher levels downstream of the propeller, which were attributed to reflections off the pylon. In the case of the present model, the wavelength of the fundamental tone at 208 Hz is 1.65 m which is much larger than the chord of the model. Hence, pure reflection is ruled out as an interaction mechanism for the VARPW model. It is important to note that the reduced level for all axial directivity for the microphones with circumferential directivity angle of $\phi_{mic} = 150^\circ$ in comparison to other circumferential directivity could be due to a measurement or data acquisition error. However, in tests conducted on CROR pusher installation effects [13], it was discovered that in the pylon plane ($\phi = 180^\circ$), a lobe was
generated in the circumferential directivity which, depending on the rotation direction of the blade had either a minimum or maximum level. In the case of the propeller moving away from the observer as it passes through an inflow distortion, a reduction in levels were observed. While the minimum level of this trough was at $\phi = 180^\circ$, the reduction begins from a directivity of $\phi = 130^\circ$ onwards. The closest propeller-pylon spacing in the present configuration, gives rise to inflow distortions due to the potential effects of the pylon (discussed in more detail in subsequent paragraphs) causing unsteady blade loading similar to the case of pylon wake impingement in pusher rotor configuration. As the current test were conducted for only a single propeller rotation direction, the applicability of this effect even for tractor propeller installation can not be confirmed. From the evaluation of the installation penalty for the largest propeller-pylon spacing ($\Delta X = 0.5D$) the blade tip vortex impingement noise mechanism can be ruled out, as the levels are comparable with the isolated case.

Further insight on the mechanism interaction effects is drawn by examining the tonal levels for the circumferential directivity in the pylon plane (i.e. $\phi_{\text{mic}} = 180^\circ$). The SPL of the fundamental tone is plotted as a function of the propeller-pylon spacing against the axial directivity in Figure 5.12. The results reveal that there is an increase in the measured noise with increasing propeller-pylon spacing for the observation position in the pylon plane ($\phi_{\text{mic}} = 180^\circ$). In the propeller plane ($\theta_{\text{mic}} = 90^\circ$) there is a difference of approximately 2 dB between the closest and largest spacing of $\Delta X = 0.1D$ and $\Delta X = 0.5D$. Further downstream, a more significant difference is noticed with the largest spacing having a tonal level of about 6 dB higher.

![Figure 5.12: Installed propeller tonal noise levels for different directivity and $\Delta X$. Reading for $J = 0.85$, $\Delta X = [10\% - 50\%]$, $\delta_e = 0^\circ$, short configuration.](image)

In order to determine the effects of spacing on the levels in the propeller plane, the tonal
levels in the propeller plane ($\theta_{\text{mic}} = 90^\circ$) are examined for the various spacings in Figure 5.13. Error bars of $\pm 0.5$ dB, derived from the repeatability tests for consecutive readings are also depicted. The level of the isolated propeller tone is represented by the dotted black horizontal line.

![Figure 5.13: Installed propeller tonal noise levels for $\theta_{\text{mic}} = 90^\circ$ at different propeller-pylon spacings. Reading for $J = 0.85$, $\delta_e = 0^\circ$, short configuration.]

At the closest propeller-pylon spacing ($\Delta X = 0.1 D$), the tonal level is 1.5 dB lower than the isolated propeller case. As the propeller-pylon spacing increases, there is an increase of the tonal levels compared to the isolated propeller case. As the spacing increases beyond 30% of the propeller diameter ($\Delta X = 0.3 D$), the tonal levels converges to an almost constant level at about 1 dB higher than the isolated propeller case. The reduction in the levels for the close propeller-pylon spacing is a result of the unsteady loading noise cancelling out the noise field of the steady noise [32, 45]. The proximity of the pylon affects the inflow of the propeller due to potential effects, causing unsteady blade loading in the localised region just ahead of the pylon. In order to assess the magnitude of change in the propeller inflow that the trailing pylon causes, the potential effects of the trailing potential is briefly examined. This is achieved by a 2D pylon section analysis using Xfoil at the same freestream conditions that were considered during the experiment. The results of the analysis are presented in Appendix B.0.1. The results confirm that for a propeller-pylon spacing of below $\Delta X = 0.3 D$ there is a considerable reduction in the axial freestream velocity with a maximum of 8% for the closest propeller-pylon spacing ($\Delta X = 0.1 D$). For propeller-pylon spacings above $\Delta X = 0.3 D$, the upstream potential effects are negligible. Thus, as the pylon moves further away, the potential effects reduce, reducing the unsteady loading noise.

The presence of unsteady blade loading is further confirmed by examining the levels of the higher harmonics. Figure 5.14 depicts the summation of the SPL of the higher harmonics ($2^{\text{nd}}$ to $4^{\text{th}}$ BPF) as a function of the propeller-pylon spacing for the same microphone location ($\theta_{\text{mic}} = 90^\circ$). The horizontal dashed black line represents the tonal level of the summed harmonics of the isolated propeller configuration.
Experimental Results: VarPW Model

5.4 Experimental Results: VarPW Model

Figure 5.14: Installed propeller harmonic noise levels (2nd to 4th BPF) for $\theta_{\text{m}} = 90^\circ$ at different $\Delta X$. Reading for $J = 0.85$, $\delta_e = 0^\circ$, short configuration.

There is a significant increase in the levels of the higher harmonics compared to the isolated case with decreasing propeller-pylon spacing. There is an increase of almost 7.5 dB in the SPL for closest propeller-pylon spacing ($\Delta X = 0.1D$). The increased levels of the higher harmonics are an indication of inflow distortion and other unsteady influences [32], confirming that the pylon installation penalty is dominated by the effects of unsteady blade loading due to inflow distorting.

Thus, to conclude, it was determined that the installation of the pylon significantly changes the circumferential directivity of propeller sound field. The main mechanism attributed to the levels witnessed is the unsteady blade loading due to the upstream potential effects of the trailing pylon. Inclined to the pylon, there are higher tonal levels in comparison the isolated propeller case. However, in the pylon plane ($\phi_{\text{mic}} = 180^\circ$), there is a trough in the circumferential directivity for propeller-pylon spacings below 30% of the blade diameter ($\Delta X = 0.3D$), leading to tonal levels lower than the isolated propeller case. These lower levels are a result of the cancelling of the steady noise field by the unsteady loading noise field. As the propeller-pylon spacing increases about 30% of the blade diameter ($\Delta X = 0.3D$), the trough disappears, with tonal levels about 1 dB higher than the isolated case. This indicates the reduction in the inflow distortion due to the potential effects, as the pylon moves further away from the propeller. This is reflected in the installation penalty of the largest spacing ($\Delta X = 0.5D$) being negligible, with the overall SPL at the same level as the isolated propeller case.

5.4.3 Tonal Noise Levels: Elevator Deflection Effects

The effect of elevator deflections on the propeller-pylon interaction noise was another aspect of interest in the experimental campaign. Multiple measurements were taken for different elevator deflections while varying the propeller-pylon spacing. Although measurements were taken for intermediate elevator deflections of $\delta_e = \pm 10^\circ$ and $\pm 20^\circ$, results are presented only for the extreme deflections of $+30^\circ$ and $-30^\circ$. Note that for a
positive elevator setting, the elevator is deflected towards the positive $Z$-axis as illustrated in Figure 5.1b.

To assess the effect of elevator deflections on the installed propeller noise, the tonal levels measured at inclined directivity angles ($40^\circ \leq \phi_{\text{mic}} \geq 150^\circ$ and $215^\circ \leq \phi_{\text{mic}} \geq 315^\circ$) are initially discussed. For the closest propeller-pylon spacing ($\Delta X = 0.1D$) the results for the most positive and the most negative elevator angles ($\delta_e = \pm 30^\circ$) are depicted in Figures 5.15.

![Figure 5.15: Propeller tonal noise levels measured by grid microphones. Reading for $J = 0.85$, $\Delta X = 0.1D$, short configuration.](image)

In Figure 5.15a the elevator is deflected towards the positive $Z$-axis, resulting in an increase in measured levels for the microphones towards which the elevator is deflected. Thus, for the case of the positive elevator deflection, an increase of about 1 dB is witnessed in the first semi-circle of microphones (i.e. $\phi_{\text{mic}} < 180^\circ$). There is also a reduction in the measured noise at the downstream microphone positions for the second semi-circle of microphones (i.e. $\phi_{\text{mic}} > 180^\circ$, $\theta_{\text{mic}} = [110, 125]^\circ$). For the negative elevator deflection in Figure 5.15b the mirrored response is witnessed. There is an increase in measured levels for the circumferential directivity angles towards which the elevator is deflected ($\phi_{\text{mic}} > 180^\circ$). This trend is present for all propeller-pylon spacings, albeit with reducing magnitude as the spacing increases.

The installation penalty due to the elevator deflections compared to the isolated propeller case for the extreme propeller-pylon spacings ($\Delta X = 0.1D$ and $\Delta X = 0.5D$) is plotted in Figure 5.16. For the closest propeller-pylon spacing ($\Delta X = 0.1D$), the elevator deflections effect the blade loading as the deflections influence the upstream flow distortion. This is observed in the similar levels for the negative deflection ($\delta_e = -30^\circ$) when compared to zero degree deflection case from Figure 5.11a. For the positive deflection ($\delta_e = 30^\circ$), there are reduced levels, signifying larger cancellation of the steady noise field due to unsteady loading effects. When compared to the isolated propeller baseline there is an additional increase in levels in the arc towards which the elevator is deflected. While being present for the closer propeller-pylon spacing, this effect is more evident for the larger propeller-pylon spacing ($\Delta X = 0.5D$) seen in Figures 5.16c and 5.16d.
The observed behaviour for the closest propeller-pylon spacing is associated primarily with the change in the blade loading caused by the inflow distortion which stems from the elevator deflections. A secondary interaction mechanism is also present, which is more evident for the larger propeller-pylon spacing ($\Delta X = 0.5D$). This is the direct interaction of the elevator with the slipstream. The higher tonal levels in the arc towards which the elevator is deflected is associated with this mechanism. Since the slipstream phenomenon occurs at the blade passage frequency, this interaction effect also contributes to the tonal levels at the blade passage frequency and its harmonics. In acoustic source identification experiments conducted on a fixed trailing high lift device [66], Akkermans found that the flap indeed was a location of tonal noise at the blade passage frequency, especially for the low and medium thrust setting. Thus, for the closer propeller-pylon spacing, the unsteady blade loading remains the dominant interaction mechanism for the tonal levels observed, but as the propeller-pylon spacing increases, the mechanism shifts to the direction interaction of the elevator with the slipstream.

For the directivity in the propeller plane ($\theta_{mic} = 90^\circ$, $\phi_{mic} = 180^\circ$), the extracted and
5.4 Installed Propeller Configuration

corrected tonal levels are plotted for the measured axial directivity angles for the extreme flap deflections $\delta_e = \pm 30^\circ$ and the baseline $\delta_e = 0^\circ$ case in Figure 5.17.

![Figure 5.17: Propeller tonal noise levels for different axial directivity and elevator deflections in the pylon plane ($\phi_{mic} = 180^\circ$). Reading for $J = 0.85$, $\Delta X = 0.1D$, short configuration.](image)

Far upstream ($\theta_{mic} = 47^\circ$), there is a difference in the tonal levels between the elevator deflections of about $2.5 \text{ dB}$ while downstream of the propeller plane the difference is almost $4.5 \text{ dB}$. The higher tonal levels downstream are in the vicinity of the elevator, and are thus attributed to direct slipstream-elevator interaction effects. In the propeller plane ($\theta_{mic} = 90^\circ$), there is a negligible difference due to the elevator deflections for the closest propeller-pylon spacing of $\Delta X = 0.1D$. To assess the effect of elevator deflections remains negligible at larger propeller-pylon spacings, the measured noise in the propeller plane ($\theta_{mic} = 90^\circ$) is plotted for all the propeller-pylon spacing in Figure 5.18.

The closest spacing of $\Delta X = 0.1D$ for all elevator deflections, has levels of about $1.9 \text{ dB}$ lower than the isolated propeller case. The positive elevator deflected $\delta_e = 30^\circ$ has further lowered levels compared to the no elevator deflection, but with differences with the variability of the measurements. The positive deflected elevator does however have combined marginally lower measured noise across the complete propeller-pylon spacing spectrum traversed. Given the propeller rotation direction, the positive elevator deflection causes the larger change in the propeller inflow condition as compared to the negative elevator deflection. As the propeller-pylon distance increases, the magnitude of the inflow distortion reduces, and thus, beyond a spacing of $\Delta X = 0.3D$, the magnitude of the unsteady loading noise is not large enough to reduce the steady noise to levels lower than the isolated case.

Thus, from these results in the propeller plane, it is concluded that while the elevator
deflections does result in unsteady blade loading, the propeller-pylon spacing influences the loading more than the elevator deflection. However, for a given spacing the deflected elevator causes a larger inflow distortion, and thereby changes the noise levels resulting from the cancellation of the steady noise field by the unsteady loading noise.

In view of the complete directivity of the noise field, it is thus concluded that the elevator deflections further affect the propeller-pylon noise interactions. The spectral directivity is influenced by the deflections, with increased levels at the circumferential directivity angles towards which the elevator is deflected. This is attributed to the interaction of deflected elevator with the propeller noise field. For the specific circumferential directivity angle in the pylon plane (φ_{mic} = 180°), the presence of the trough in the spectral directivity due to unsteady loading noise was further influenced by the elevator deflections, with the positive elevator deflection leading to a larger distortion in the inflow, leading to further lowered levels in the circumferential directivity trough.
Experimental Results: Prowim 2.0 Model

The tests for the Prowim 2.0 were split over two windtunnel campaigns in the V-tunnel and the LTT as mentioned in Section 4.3. The focus of the test campaigns was to investigate the slipstream interaction effects with the aid of acoustic, performance, pressure and PIV measurements. However, acoustic measurements were undertaken for limited directivity angles in comparison to the tests with the VARPW model. The results presented in this section are accordingly divided into separate sections for the noise, performance, and the flow field results. A brief overview of the parameters tested in these campaigns are outlined in Section 6.1. The results and discussions for the acoustic data is presented first in Section 6.2, followed by the performance results in Section 6.3. The flow field PIV investigations are discussed in 6.4 along with a discussion of the pylon loads in Section 6.5.

6.1 Prowim 2.0 Test Matrix

The operating parameters of the test campaigns with the Prowim 2.0 model are presented in Table 6.1. A complete description of the experimental setup along with the measurement techniques employed is provided in Section 4.2.2.

6.2 Effects of Propeller-Pylon Interactions on Acoustic Levels

The acoustic data considered in this section was measured during the V-tunnel campaign. The data was accumulated over a measurement time of 30s following which the data was

\[ \text{Ratio of the propeller diameter } D, \text{ achieved by two separate configurations of the model.} \]
Table 6.1: Test matrix for the test campaigns with the Prowim 2.0 model

<table>
<thead>
<tr>
<th>Tunnel</th>
<th>Velocity $U_\infty$</th>
<th>Advance Ratio $J$</th>
<th>Pylon Spacing $\Delta X$</th>
<th>Elevator Deflection $\delta_e$</th>
<th>Angle of Attack $\alpha$</th>
</tr>
</thead>
<tbody>
<tr>
<td>V-Tunnel</td>
<td>0.85</td>
<td>0.75</td>
<td>0.43D, 0.85D</td>
<td>0, ±15, ±30</td>
<td>0</td>
</tr>
<tr>
<td>LTT</td>
<td>0.85</td>
<td>0.70</td>
<td>0.43D</td>
<td>0, ±30</td>
<td>0 - 21</td>
</tr>
</tbody>
</table>

calibrated, corrected for tunnel jet shear layer and normalised to a reference radial distance of one meter ($R = 1$ m). The Prowim 2.0 model features an integrated pylon, hence the isolated propeller configuration could not be tested. Hence, the short configuration ($\Delta X = 0.43D$) is used as the baseline configuration for the subsequent result discussions.

### 6.2.1 Repeatability and Reproducibility of Acoustic Measurements

In order to evaluate the quality of the microphone data for the Prowim 2.0 model, data from multiple consecutive powered runs were averaged over and the mean of the difference is presented for each microphone in Figure 6.1. The repeatability for the 1st BPF is in the order of about 0.1 dB. For the harmonics, the largest variation was determined to be about 1 dB for the most downstream axial directivity microphone perpendicular to the pylon.

![Circumferential Directivity Angles $\phi_{lok}$ [deg]](image)

**Figure 6.1:** Repeatability of successive propeller noise measurements with discrete microphones. Reading for $J = 0.65$, $\delta_e = 0^\circ$, short configuration.
6.2 Effects of Propeller-Pylon Interactions on Acoustic Levels

6.2.2 Installed Propeller Sound Frequency Spectra

The sound spectra for two propeller-pylon spacings of the Prowim 2.0 model are depicted in Figure 6.2 for an advance ratio $J = 0.75$. The plotted spectra are for an axial directivity of $\theta_{mic} = 90^\circ$, and a circumferential directivity in the pylon plane ($\phi_{mic} = 180^\circ$). The two configurations ($\Delta X = 0.43D$ and $\Delta X = 0.85D$) are annotated by markers to indicate the peak tones that occur at blade passage frequency and its higher harmonics. The tunnel noise is identified at about 300 Hz and 718 Hz and is marked by vertical black lines. In addition, the model motor’s rotational frequency is marked by the green marker. The tonal levels of the motor are of magnitude comparable to the fundamental tone at the blade passage frequency. The presence of the motor tone was further confirmed from runs with the same freestream and motor conditions with the unmounted propeller. Hence, this tone is attributed to the model motor, most likely originating from motor shaft bearing. The difference in levels of this tone between the two configurations however, requires further investigation.

The tonal nature of the propeller-pylon interaction noise is clearly evident from the spectra. It is observed that only the fundamental tone is prominent, the levels of the harmonics are not significantly higher than the background noise. The spectra for the high thrust case ($J = 0.65$) is plotted in Figure 6.3 with the same notations. In comparison to the medium thrust case, the presence of the harmonics of the fundamental tone are evident. The closer propeller-pylon spacing ($\Delta X = 0.43D$), has a marginally lower fundamental tone than the larger spacing ($\Delta X = 0.85D$), however when comparing the 1st harmonic, the level for the closer spacing is significantly higher when compared to the larger propeller-pylon spacing. This is an indication of the presence of unsteady inflow distortions in the propeller inflow, similar to the effects observed in the pusher configuration due to upstream wakes, or in CROR system, due to aerodynamic interactions of the aft rotor [13, 32, 45]. Hence it is concluded that the closer propeller-pylon spacing ($\Delta X = 0.43D$) has larger unsteady loading noise than the larger propeller-pylon spacing ($\Delta X = 0.85D$).

![Figure 6.2: Comparison of sound spectra for two propeller-pylon spacings. Reading for $J = 0.75$, $\delta_e = 0^\circ$.](image-url)
Experimental Results: Prowim 2.0 Model

6.2.3 Tonal Noise Levels: Advance Ratio Effects

The spectrum data presented in the previous subsection was for an circumferential directivity in the pylon plane, with an axial directivity in the propeller plane (i.e. $\phi_{\text{mic}} = 180^\circ$, $\theta_{\text{mic}} = 90^\circ$). From the spectrum data at all directivity angles, the tonal levels at the blade passage frequency and its harmonics are extracted and corrected as mentioned previously. From these extracted levels, the effect of the advance ratio on the tonal sound levels is depicted in Figure 6.4.

The two circumferential directivity positions considered during the tests, in the pylon plane ($\phi_{\text{mic}} = 180^\circ$) and perpendicular to the pylon ($\phi_{\text{mic}} = 270^\circ$) are presented separately. As expected, there is an increase in the propeller tonal levels for a decreasing

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**Figure 6.3:** Comparison of sound spectra for two propeller-pylon spacings. Reading for $J = 0.65, \delta_e = 0^\circ$.

**Figure 6.4:** Effect of advance ratio on tonal noise levels. Reading for $\delta_e = 0^\circ$, short configuration.
6.2 Effects of Propeller-Pylon Interactions on Acoustic Levels

Advance ratio due increased loading and helical Mach number. Unlike the VarPW model, however there is a distinguishable difference between the advance ratios, with an approximate 9 dB increase between the high thrust setting and the medium thrust setting for an observer position in the propeller plane. From the axial directivity angles considered, the noise levels are highest in the propeller plane ($\theta_{mic} = 90^\circ$) and are reducing upstream and downstream of the propeller. Perpendicular to the pylon, there is again a difference of about 10 dB between the tonal levels for the mid thrust and high thrust case. Thus, across all directivity angles measured, for reducing advance ratios, there is an increase in the fundamental tone at the blade pass frequency. This increase in tonal level is attributed to steady state loading noise. From these results, it was decided to discuss the results only for the high thrust case in the subsequent sections.

6.2.4 Tonal Noise Levels: Propeller-Pylon Spacing Effects

Using the two configurations of the PROWIM 2.0 model, the propeller-pylon spacing could be varied. The measured tonal levels for these two spacings ($\Delta X = 0.43D$ and $\Delta X = 0.85D$) for an advance ratio of $J = 0.65$ are depicted in Figure 6.5. The results for the directivity in the pylon plane ($\phi_{mic} = 180^\circ$) are in agreement with results from the previous tests on the VarPW model, with increased tonal levels in the propeller plane for increasing propeller-pylon spacing. In the case of the PROWIM 2.0 model, in the propeller plane ($\theta_{mic} = 90^\circ$), the measured sound pressure levels for the short configuration is almost 1.25 dB lower than the levels of the long configuration for the same advance ratio. For the circumferential directivity perpendicular to the pylon, the larger propeller-pylon spacing has an increased level of about 2.3 dB in the propeller plane.

![Figure 6.5: Effect of propeller-pylon spacing on tonal noise levels. Reading for $J = 0.65$, $\delta_e = 0^\circ$, short and long configuration.](image)

From the results of the directivity angles parallel to the pylon depicted in Figure 6.5, the lobed circumferential directivity pattern is further confirmed. The effect of the pylon installation produces a trough in the polar directivity perpendicular to the pylon. In
the plane of pylon, the lower levels are attributed to unsteady loading noise cancelling a component of the steady loading noise. Thus, it is reasoned that the tonal levels of the closer propeller-pylon spacing are lower, as it gives rise to more unsteady loading given its proximity to the propeller compared to the larger propeller-pylon spacing as discussed earlier in Subsection 6.2.2. However, it is important to note, that the for the VARPW model, for propeller-pylon spacings above $\Delta X = 0.3D$, the unsteady loading effects were not witnessed. For the directivity angles perpendicular to the pylon, the results do not correlate with earlier results of the VARPW model, which had reducing sound pressure levels for the same directivity angle considered, as the pylon moved further away from the propeller. The precise interaction mechanism which leads to the observed tonal levels inclined to the pylon can not be determined from the current study. Nevertheless, the change in levels between the closer and larger propeller-pylon spacing indicates that the contribution of vortex impingement noise is low. This would lead to the conclusion that there is a direct interaction between the tonal noise field of the propeller and the pylon as witnessed in earlier studies by Akkermans [36].

### 6.2.5 Tonal Noise Levels: Elevator Deflection Effects

To investigate the effects of elevator deflections on the tonal noise levels, the results of the most extreme elevator deflections tested ($\delta_e = \pm 30^\circ$) along with the baseline zero deflection case are presented in Figure 6.6.

![Figure 6.6: Effect of elevator deflections on tonal levels. Reading for $J = 0.65$, short configuration.](image)

In the propeller plane for a directivity in the pylon plane ($\phi_{mic} = 180^\circ$), the deflected elevator leads to higher tonal levels than the baseline non deflected elevator. There is an increase in the tonal level of about 1.25 dB between the case with negative elevator deflection and the baseline zero deflection case. There is however a very marginal difference between the positive deflection and the baseline case. This is unexpected, as given the inboard up rotation direction of the propeller, the positive elevator deflection is expected
to contribute a larger change in the inflow as the upwash will be reinforced by the propeller induced components. The increase in the lift for the positive elevator deflection is confirmed by pylon load measurements discussed in Section 6.5. However, as the measurements were performed only on one propeller rotation direction, it is not possible to conclude if the change in inflow due to the elevator deflections is indeed the mechanism that causes the changes in the tonal levels. For the directivity angles perpendicular to the pylon, the measured levels do not follow the trends witnessed in the results of the VarPW model. From Figure 6.6b, it is observed that the negative elevator deflection has lower tonal levels than the corresponding positive elevator deflection. Given the circumferential directivity of $\phi_{\text{mic}} = 270^\circ$, the negative elevator deflection $\delta_e = -30^\circ$ corresponds to a deflection towards this directivity angle. This discrepancy between the observed tonal levels inclined to the pylon is attributed to the relative difference between the position of the elevator and the microphone stemming from the difference in pylon chord for the two models.

6.3 Effects of Propeller-Pylon Interactions on Performance Parameters

To evaluate the effects of pylon-spacing and elevator deflections on the performance of the propeller, force readings from the load cell are used to measure the propeller thrust. A bookkeeping approach is utilised to determine the drag of the entire model which is subtracted from the measurements with the thrusting propeller to obtain the propeller thrust. Given the power of the motor of the PROWIM 2.0 model, further corrections are applied to correct for the increase in the inflow velocity as a function of the propeller thrust setting. Hence for a given free stream velocity and a set advance ratio $J$, the propeller is actually experiencing a higher inflow velocity, resulting in a lower advance ratio $J_{\text{cor}}$ as described earlier in the measurement techniques section (Subsection 4.3.2). During the measurements, it was observed that the encoder responsible for reporting the rotational speed of the motor was malfunctioning. As a result, the rotational speed was determined by utilising noise data by identifying the peaks in the frequency spectrum corresponding to the blade passage frequency. All data discussed in this section utilises the corrected advance ratio and thrust. In this section, first the effects of propeller-pylon spacing on performance are examined, following which the effects of elevator deflections are discussed.

6.3.1 Propeller Performance: Propeller-Pylon Spacing Effects

The thrust curve for both configurations tested is presented in Figure 6.7. The results indicate that compared to the shorter propeller-pylon spacing, the thrust for the same advance ratio is higher for the greater propeller-pylon spacing.

The larger propeller-pylon spacing ($\Delta X = 0.85D$) has 15% greater thrust compared to the smaller propeller-pylon spacing ($\Delta X = 0.43D$) for the highest advance ratio considered. This difference reduces for an decreasing advance ratio, and was determined to be 3% for the lowest advance ratio. However, when looking at the absolute values of the measured thrust by the load cell, the difference is evaluated to be 0.3 N for the highest advance ratio.
tested. While this value is within the resolution of the sensor, in order to obtain the two propeller-pylon spacings, the model had to be dismounted and remounted on the flexure apparatus. This difference between the performance for the two spacings is attributed to the measurement variability due to the change in the model mounting.

6.3.2 Propeller Performance: Elevator Deflection Effects

From the previous section, it was reasoned that the effect of propeller-pylon spacing on the performance was negligible. However, elevator deflections could cause a change in the performance due to upstream interactions. To evaluate the elevator interaction effects, the performance curves for three elevator deflections are evaluated for the short configuration, depicted in Figure 6.8.

Across the advance ratios tested, the negative elevator deflection results in about a 9% reduction in the thrust coefficient. For the positive elevator deflection, at low thrust setting, there is a marginal difference when compared to the baseline zero elevator deflection. However, as the thrust goes up, the difference becomes greater, with a difference of 5% at the highest thrust setting. It is important to note that although the relative changes in the performance are large between the three elevator deflections, the difference in the value of the dimensional thrust was found to be in the order of 0.2 N between the baseline and negative elevator deflection for the lowest advance ratio. The relative results indicate the that the elevator has a marginal penalty on the thrust of the propeller. However based on the absolute thrust values and measurement variability, it is concluded that the elevator has negligible effects on the propeller performance.
6.4 Propeller-Pylon Flow Field Investigation

To study the propeller-pylon flow field, particle image velocimetry was employed. As explained while discussing the experimental setup in Section 4.3.6, two PIV planes were chosen. One inflow plane to determine the upstream interaction effects, and one slipstream plane to examine the downstream interaction effects. The details of these planes are given in Figure 4.13.

In this section, the upstream interaction effects are initially evaluated in Subsection 6.4.1, followed by the treatment of the downstream interactions. The PIV velocity fields are qualitatively evaluated in Subsection 6.4.2, followed by a more detailed analysis in Subsection 6.4.4.

6.4.1 Evaluation of Upstream Potential Effects of the Pylon

To assess the upstream interactions caused by the pylon, an inflow plane was utilised as described in Subsection 4.3.6. To investigate the magnitude of the upstream potential effects of the pylon, the short configuration ($\Delta X = 0.43D$) was chosen as it would witness the higher interaction effects compared to the long ($\Delta X = 0.85D$) configuration. The inflow velocity vectors were obtained for model without the propeller mounted. The operating conditions were maintained the same as for the runs with the thrusting propeller discussed thus far. Thus, the magnitude of the upstream interactions for the given freestream conditions of $U_\infty = 20$ m/s was determined. The measured inflow velocity field for the baseline zero elevator deflection configuration is depicted in Figure 6.9a.
6.4.2 Downstream Interactions - Elevator Deflection Effects

To investigate the downstream interactions, a PIV plane was positioned in the propeller slipstream. In order to identify the potential effects of the elevator deflections, mea-
measurements were undertaken initially without the propeller mounted. The baseline zero elevator deflection is presented in Figure 6.10.

Figure 6.10: Potential effects just ahead of the pylon without propeller present. Reading for $U_\infty = 20$ m/s, $\delta_e = 0^\circ$, short configuration.

Although the airfoil was symmetric, there are traces of asymmetry in the velocity vector field. This is again attributed to the PIV plane not being perpendicular to the tunnel jet. Results from the short configuration ($\Delta X = 0.43D$) for the extreme elevator deflections of $\delta_e = \pm 30^\circ$ are presented in Figure 6.11. For both elevator deflection cases, ahead of the deflected elevator, there is a reduction in the axial component of the velocity. This is due to the upwash generated by the deflected elevator. From the tangential and axial velocity field, the upwash was computed to be $4^\circ$ in the slipstream plane for an elevator deflection of $30^\circ$.

Figure 6.11: Potential effects just ahead of the pylon without propeller present. Reading for $U_\infty = 20$ m/s, short configuration.
With the thrusting propeller present, the potential effects of the pylon witnessed for the propeller off configuration are still visible outside the propeller slipstream as seen in Figure 6.12.

![Figure 6.12: Propeller slipstream and pylon potential effects in the slipstream. Reading for $J = 0.65$, $\delta_e = 0^\circ$, short configuration.](image)

The effect of the positive and negative elevator deflections are witnessed outside the propeller slipstream as well as seen from the velocity fields plotted in Figures 6.13a and 6.13b. The spanwise change in the flow field due to the increased lift in the slipstream region is further witnessed.

![Figure 6.13: Propeller slipstream and pylon potential effects in slipstream PIV plane. Reading for $J = 0.65$, $\delta_e = \pm 30^\circ$, short configuration](image)

The above discussed results are outside the propeller slipstream. In the propeller slipstream, the effects of the elevator are further investigated. The baseline case of zero elevator deflection is depicted in Figure 6.14.
From the results in the slipstream, the potential blockage of the pylon is visible, with a 11% reduction of the axial velocity when compared to the average propeller slipstream velocity \((U/U_\infty = 1.35)\). It is important to note the asymmetry in the axial velocity profile. The incidence of the measurement plane is reasoned to be the primary cause for the asymmetry observed. A secondary reason could be due to the close proximity of the slipstream plane to the pylon. Given the rotation direction of the propeller (inboard up), the pylon acts like a barrier for the tangential component of the velocity for the lower arc of the slipstream. As a result, the axial velocity component is higher for the lower arc of the slipstream in comparison to the upper arc.

The effect of elevator deflections inside the propeller slipstream are depicted in Figure 6.15. From these results, an unexpected result is observed. Given the rotation direction of the
propeller, the negative elevator deflection is reasoned to produce less upwash as compared to the positive elevator deflection. However, from the PIV results, it is observed that the negative elevator deflection results in higher axial velocity component in the upper arc of the slipstream compared to the positive elevator deflection.

6.4.3 Slipstream Analysis - Slipstream Contraction

To obtain details about the magnitude of the slipstream contraction, the PIV velocity fields were employed to obtain the radius of the slipstream as a function of advance ratio and propeller-pylon spacing. The PIV vector field consisted of 400 instantaneous vector fields that were averaged to obtain the mean vector field. As the measurements were not phase locked, the position of the blade tip vortex was not found at a constant position in each PIV frame. By locating the locus of the blade tip vortices in the average velocity field the radius of the propeller slipstream was determined. The blade tip vortex center was determined by obtaining the local minima of the RMS value of the instantaneous axial velocity fields. Thus, the radius of the slipstream was determined by fitting a circle through the points of center of the blade tip vortex at each circumferential position. An example of such a determined slipstream radius is illustrated in Figure 6.16a.

![Figure 6.16: Slipstream contraction analysis.](image)

In a similar fashion, the slipstream contraction is determined as a function of the advance ratio and the propeller-pylon spacing as depicted in Figure 6.16b. The results shows that for the short configuration ($\Delta X = 0.43D$), the slipstream is contracted at all advance ratios. However, for the highest advance ratio of the long configuration ($\Delta X = 0.85D$), the slipstream is around the same size as the propeller diameter. For the lowest advance
ratio considered, the slipstream radius can be considered the same for both spacings, while for the higher advance ratios, the trends show that the slipstream is less converged further away from the propeller when compared to the closer propeller-pylon spacing. This however, is attributed to measurement variability, given the maximum difference of 1% between the spacings. The determination of the radius of the slipstream serves as a tool in the detailed determination of the propeller performance from the PIV obtained velocity fields described in the subsequent subsection.

6.4.4 Slipstream Velocity Profiles and Propeller Performance

Information about the velocity in the propeller slipstream gives insight into the operating condition of the propeller. To obtain the slipstream velocities from the PIV planes, first the slipstream radius was determined. This was utilised in the extracting the velocity components along circumferential arcs in the slipstream. Due to the potential effects of the pylon in the PIV obtained vector field as was seen in Figure 6.15, the entire azimuthal segment could not be utilised in the calculation of the slipstream velocity. Hence, azimuthal positions were chosen such as to provide a constant circumferential velocity profile for each radial position in the slipstream. The velocities were then averaged across all azimuthal positions for a given radial location. This circumferentially averaged velocity serves as an indication of the propeller slipstream velocity profile. An example of this method of determining the slipstream velocities is depicted in Figure 6.17.

![Figure 6.17: Determination of slipstream velocities from PIV velocity field.](image_url)

The resulting propeller induced velocity profiles obtained from these circumferentially averaged velocities are presented in Figure 6.18.
Figure 6.18: Slipstream induced axial velocity profile. Reading for $\delta_e = 0^\circ$, short configuration.

From Figure 6.18, it can be seen that the induced velocities profiles maintain the same shape, and scale with the advance ratio. The effect of the advance ratio can be seen especially in the region of $0.4 < r/R < 0.6$. As the advance ratio increases, the blade sections experience a higher angle of attack, thus being able to produce more lift, thus increasing the magnitude of the induced velocities. A point to note, are the levels of the velocity profiles just outside the propeller radius. The induced velocities outside the slipstream (i.e. $r/R > 1$) do not converge to freestream levels immediately. It is only after $r/R = 1.2$ that it reaches freestream levels. An uncertainty arises if this is due to a measurement error, or if indeed the slipstream is diffusing with the freestream. Previous results would indicate that the order of magnitude of the diffusion effect is not in the order of the results obtained here [67].

From the velocity profile, the velocities across the blade span were radially averaged to obtain a single slipstream velocity value for the given operation conditions. Using the actuator disk theory discussed in Measurement Techniques Section (Subsection 4.4.1), it was possible to determine the thrust of the propeller using the slipstream velocity obtained from the velocity profiles in Figure 6.18. The thrust coefficient obtained was corrected for the change in the inflow velocity due to effect of the motor as described previously (in Subsection 4.4.1). The corrected performance curves are presented as a function of elevator deflection angles in Figure 6.19.
To aid in the discussion, the performance curves obtained from the load cell in Section 6.3 are replotted in this figure. While the zero elevator deflection cases are in agreement with each other, the cases with the elevator deflections do not. This could be due to the fact that the given the close proximity of the PIV plane to the pylon, the deflected elevator’s potential effects leads to an error in determining the true slipstream velocity from the PIV velocity fields. On the other hand, it could also be attributed to the measurement error which originates from the inability of the bookkeeping method to determine the incremental drag due to the higher induced velocities in the slipstream. However, given the variability in the measurements, the difference in absolute thrust measured due to these changes are again in the order of 0.3 N which is considered marginal. Given these results, it is concluded that the upstream interactions due to the potential effect of the elevator has a negligible impact on the performance of the propeller.

6.5 Pylon Loads Measurements

A four day campaign with the PROWIM 2.0 model in the LTT was also carried out as part of the study. During these tests a preliminary investigation into the effects of angle of attack was performed. Due to time limitations, measurements could be conducted only for the model with the thrusting propeller. Thus bookkeeping could not be applied to determine the thrust of the propeller. The effects of angle of attack, elevator deflections (for $J = 0.70$) on the pylon loads were evaluated. The lift polar for the model is presented in Figure 6.20. The results obtained are as expected, given the notation followed, the positive deflection increases the lift polar, while the negative elevator deflection reduces it.
Experimental Results: Prowim 2.0 Model

Figure 6.20: Prowim 2.0 model lift and drag polars. Reading for $J = 0.70$, $\Delta X = 0.43D$, short configuration.

With respect to the horizontal tailplane-tip mounted tractor configuration, the elevator effectiveness is an important parameter. The interaction of the propeller slipstream with the elevator was determined as depicted in Figure 6.21. It was confirmed that the propeller slipstream increases the effectiveness of the elevator. For the advance ratio considered ($J = 0.7$), a 20% increase in the effectiveness of the elevator was observed as compared to the case with no propeller present.

Figure 6.21: Elevator effectiveness increase due the slipstream effects. Reading for $J = 0.70$, $\Delta X = 0.43D$, short configuration.
6.6 Pylon Surface Pressure Measurements

The **Prowim 2.0** model included surface pressure orifices that made it possible to study the pressure distributions and hence the spanwise lift distribution of the trailing pylon. However, during the measurements in the LTT test campaign, it was discovered that connectors inside the model in the elevator had snapped, rendering the pressure data on the elevator erroneous. However, another test campaign was performed for the same model at the same freestream conditions as part of a different study, and was used to evaluate the usability of the original dataset. A comparison for a sample pressure row data \((y/b = 0.18)\) which corresponds to most inboard position, furthest away from the propeller is presented in Figure 6.22. The black vertical dashed line marks the elevator position. Given the symmetric inflow and airfoil profile, there should be no lift generated by the airfoil. However, there is a small amount of lift generated resulting from the higher dynamic pressures in the slipstream being felt outside the slipstream region due to viscous mixing effects. Additionally, the pressure distribution for a data from the new and old test for pressure row situated in the propeller slipstream \((y/b = 0.8)\) is compared in Figure 6.23. The increased dynamic pressure in the slipstream region is clearly visible, with majority of the lift generated before the midchord of the profile. Initially, it was believed that the malfunctioning pressure port was only for a single row, but from the figures and data from all pressure rows, it was determined that the malfunction was present in all pressure rows.

![Figure 6.22: Chordwise \(C_p\) distribution for a pressure row positioned outside the propeller slipstream. Evaluation of the usability of data from faulty pressure ports. Reading for \(\alpha = 0, J = 0.7\), short configuration.](image)

The influence of the malfunctioning pressure ports was further evaluated by calculating the spanwise lift coefficient. This was again compared with the new data set, a sample of such a comparison is plotted in Figure 6.24. In the figure, the vertical blue line represents the propeller radius while the vertical dashed green line represents the nacelle radius. From the plot, the effect of the slipstream is clearly visible with increased lift in the region of the propeller. However, it is also interesting to observer the viscous, potential effects of the propeller slipstream, the local increase of lift in the slipstream region, leads to an overall increased lift along the span of the pylon. The reduction in the pressures just outside the blade radius is attributed to the velocity field of the blade tip vortex.
Experimental Results: Prowim 2.0 Model

Figure 6.23: Chordwise $C_p$ distribution for a pressure row positioned in the propeller slip-stream. Evaluation of the usability of data from faulty pressure ports. Reading for $\alpha = 0$, $J = 0.7$, short configuration.

However, from Figure 6.24, and further comparisons it was ascertained that the error in the measurement of pressures in the elevator region was not a error that could be neglected. The variation seen is further exaggerated for the deflected elevator case as expected. It was hence concluded to omit the surface pressure measurements from the original campaign for further result discussion.

Figure 6.24: Spanwise lift distribution evaluated from surface pressure distributions. Evaluation of the usability of data from faulty pressure ports. Reading for $\alpha = 0$, $J = 0.7$, short configuration.
Part III

Numerical Analysis
A preliminary numerical analysis was performed to apply the pre-existing numerical propeller-wing interaction tool \textit{xf1rp} and check its validity against the experimental data discussed in Chapter 6. The setup of the numerical analysis model is discussed in the forthcoming sections, following which Chapter 8 examines the results and comparisons against the experimental data.

7.1 Propeller Performance Model

A propeller performance model was developed for the \textsc{Prowim 2.0} model using the propeller lifting line code \texttt{XROTOR} \cite{68}. This tool is written in \textsc{fortran} and is distributed as source code under the GNU General Public License. \texttt{XROTOR} solves the circulation equation by calculating the propeller induced axial and tangential velocities. The sectional angle of attack and the corresponding lift and drag coefficients are calculated and then utilised to recompute the circulation. This procedure is solved using an iterative Newton method until the circulation converges. From the converged circulation, the steady state propeller performance parameters are computed.

\texttt{XROTOR} provides three methods to calculate the propeller induced velocities, the first is the graded momentum formulation which utilises the classical lifting line theory for propellers. This model implements the tip loss factors as determined by Prandtl \cite{69} and was developed for a rotor with many blades operated at a low advance ratio ($J < 0.5\pi$). The second method, the potential formulation, extends Goldstein’s formulation for two to four lightly loaded blades \cite{69}. This model computes the potential flow field in the presence of the propeller helical wake field, and is valid for all advance ratios and blade...
numbers. The third option is the discrete vortex formulation, which is utilised to analyse blades with non-radial lifting lines, i.e. swept or raked blades.

With the induced velocities obtained, blade element theory is utilised to determine the local induced angle of attack at each blade section. With this information, the local sectional lift and drag is calculated using the lift and drag model as described in reference [67] and the XROTOR user guide [70]. The XROTOR tool was incorporated into a MATLAB script to allow for the coupling with the other tools necessary to determine the inputs required for XROTOR.

In order for XROTOR to calculate the circulation and thrust, multiple inputs are required. The inputs can be broken down into operational parameters for the entire propeller, and sectional parameters that are defined at multiple radial sections. An overview of these inputs is provided in Figure 7.1. The aerodynamic inputs required at each blade section are:

1. Zero lift angle $\alpha_0$
2. Lift coefficient gradient $c_{l\alpha}$
3. Maximum lift coefficient $c_{l\text{max}}$
4. Lift coefficient increment from incipient to total stall $\Delta c_{l\text{stall}}$
5. Stalled lift coefficient gradient $c_{l\text{alpha}}$
6. Minimum drag coefficient $c_{d\text{min}}$
7. Lift coefficient at minimum drag coefficient $c_{l\text{cd}0}$
8. Ratio of Lift coefficient gradient squared to drag coefficient gradient $\frac{\delta c_{d}}{\delta c_{l}^{2}}$
9. Critical Mach number $M_{\text{crit}}$
10. Reynolds scaling number $f$

The blade section geometry is necessary for the computations of the local angle of attack and lift. The sectional pitch and chord details were extracted from a CAD model of the blade. XROTOR also requires the aerodynamic parameters to determine the lift and drag coefficients at each blade section as a function of the local angle of attack.

To calculate the aerodynamic parameters, the blade sectional geometry is analysed with an external 2D airfoil analysis tool Xfoil. The airfoil analysis is run for each blade section, using the local chordwise determined Reynolds number for the given operating conditions. This approach was employed so that the blade section polars were calculated at approximately the same Reynolds number as determined by XROTOR (excluding the effect of the induced velocities). This thereby reduced the interpolation of sectional lift and drag values that would have had to be performed by XROTOR. To obtain the XROTOR lift and drag inputs, a multi-variable optimisation was performed. The objective function for this optimisation was:
Figure 7.1: Overview of XROTOR inputs.

\[ J = \min_{\forall \alpha \in \alpha_j} \sigma \left\{ \sum_{i=1}^{n} \left( c_l^{x\text{rotor}}(\alpha_i) - c_l^{x\text{foil}}(\alpha_i) \right)^2 + \lambda \left( c_d^{x\text{rotor}}(\alpha_i) - c_d^{x\text{foil}}(\alpha_i) \right)^2 \right\} \]  

(7.1)

where \( \lambda \) is a weight factor for the drag coefficient. A heuristics analysis was performed to determine the optimal weighting factor to be:

\[ \lambda = \frac{\sum c_l^{x\text{foil}}}{\sum c_d^{x\text{foil}}} \]  

(7.2)

The optimisation was performed using the non-gradient based Simpler-Mead [71] optimisation algorithm. Additional bounds were imposed on the optimisation by utilising \( \sigma \) as a penalty function if the bounds were violated. To illustrate this fitting procedure, an example of the fitted and the original polars for a sample blade section is provided in
Figure 7.2. A measure of error of the fit is determined for each section as the summation of the RMS error of the lift and drag at each angle of attack. It should be noted, that due to the sharp leading edge of the airfoil at the tip section, the stall prediction by Xfoil might not be an accurate representation of the actual stall for the given section. This leads to an overprediction of the lift by XROTOR, resulting in higher lift at these blade sections, leading to higher computed induced velocities in these regions.

Figure 7.2: Sample fitted polar to determine XROTOR inputs for the given blade section airfoil. Section details for $r/R = 0.9$.

7.1.0.1 Rotational Effects Corrections

The input lift and drag polars were calculated using 2D analyses for the blade section airfoils. These resulting characteristics of the airfoils aerodynamics are utilised by XROTOR to compute the axial and tangential flow components. However, the rotation of the propeller leads to a radial flow component that is not considered in the simplified XROTOR model. This leads to underprediction of the blade forces, especially at the higher angles of attack prevalent at low advance ratios. To correct for this underprediction, a rotational correction was provided based on an empirical model developed by Snel et al.[72]. The corrections applied are a function of the local blade solidity $\xi$, the 2D linear lift coefficient $c_{l,\text{lin}}$, and the 2D non-rotating lift coefficient $c_l$. The rotational lift model is then given
for each blade section as:

\[
cl_{\text{rotating}} = cl + f(cl - cl_{\text{lin}})
\]  
(7.3)

The multiplication factor \( f \) is a function of the blade solidity and is determined by:

\[
f = \tanh \left\{ A \left( \frac{c}{r} \right)^{B} \right\}
\]  
(7.4)

The correction employs empirical tuning factors, for which the default values were chosen as \( A = 3 \) and \( B = 2 \). As the corrections were based on the 2D linear lift coefficient, the corrections were applied to the fitted data, following which the optimisation was run again with the corrected lift polar for which the XROTOR inputs were fit.

### 7.2 Propeller Slipstream Velocities

To compare the slipstream velocities and run further interaction analysis, the circumferentially averaged propeller induced axial and tangential velocities just behind the propeller were extracted from XROTOR. The contraction of the slipstream was taken into account by a slipstream development factor based on the axial position behind the propeller. The development factor \( k_d \) is computed using the equation proposed by McCormic [16].

\[
k_d = 1 + \frac{s}{\sqrt{s^2 + R^2}}
\]  
(7.5)

where \( s \) is defined as the axial distance behind the propeller plane where the slipstream properties are computed. At larger distance behind the propeller, i.e. \( s \to \infty \), the slipstream is fully developed hence, \( k_d = 2 \). To compute the axial and radial velocity components, the methodology used by Stone, Hunsaker and Mc Veight et al. [73–75] was employed. In this formulation, the radial velocity is prescribed per blade station as a function of the radial location of the station. As the axial velocity increases as a function of the slipstream development factor, the tangential velocity determined must ensure that the mass is conserved for each radial segment. The radial development factor (illustrated in Figure 7.3) at each radial station of the slipstream is then determined as:

\[
r'_m = \begin{cases} 
  r_{\text{nacelle}} & m = 1 \\
  \sqrt{r'_{m-1}^2 + (r_m^2 - r_{m-1}^2)}K_v & m = 2, \ldots, n 
\end{cases}
\]  
(7.6)

where

\[
K_v = \frac{2U_{\infty} + u_m + u_{m-1}}{2U_{\infty} + k_d(u_m + u_{m-1})}
\]  
(7.7)

The subscript \( m \) in the above equations indicates each radial station, while the \( r' \) indicates the slipstream radial stations. With these development factors defined, the propeller induced slipstream velocities are determined as:

\[
u_{ss} = uk_d
\]  
(7.8)
\[ w_{ss} = 2w \left( \frac{r_m}{r_m'} \right) \] \hspace{1cm} (7.9)

Where the induced axial velocity behind the propeller is defined by \( u \) and the induced tangential velocity is defined as \( w \).

\[ X = (|Pt(y) - Pt_0(y)| < R) \land (|Pt(z) - Pt_0(z)| < R) \] \hspace{1cm} (7.10)

The slipstream model has two simplifications:

- The model does not account for the diffusion that occurs at the periphery of the slipstream tube.
- The model utilises the time-averaged propeller induced velocities, thus providing time-averaged slipstream velocities.

### 7.3 Propeller-Pylon Interaction Model

To analyse the effects of the propeller slipstream on the trailing pylon, a modified version of the open source tool *xflr5* is utilised. *xflr5* allows for the simulation of a finite wing using the 2D sectional airfoil polars generated by *Xfoil* which is inbuilt into the program. The tool allows for three formulations for the finite wing calculations: Lifting Line Theory (LLT), Vortex Lattice Method (VLM), and 3D Panel Method.

The base *xflr5* tool was modified to include the effects of the propeller slipstream as part of the thesis work of Dimchev [23], and the resulting tool was dubbed *xflrp*. The tool allows for the input of the slipstream velocity profiles and uses these in the calculation of the lift and drag of the wing in the slipstream. This is achieved by traversing each control point on the wing. For each control point \( Pt(x, y, z) \), a test \( X \) is applied to check if the control point is within the propeller radius \( R \):

![Figure 7.3: Slipstream radial development stations for the calculation of the tangential slipstream velocities; reproduced from [73]](image-url)

The slipstream model has two simplifications:

- The model does not account for the diffusion that occurs at the periphery of the slipstream tube.
- The model utilises the time-averaged propeller induced velocities, thus providing time-averaged slipstream velocities.
where the propeller center point is defined as \( P_{t0}(x, y, z) \). If Equation 7.10 is true, then the velocity components at the given control point can be calculated as a function of the radial and azimuthal positions \( r \) and \( \phi \) in the slipstream. Depending on the direction of the blade, the tangential component is either added or subtracted as depicted in Figure 7.4. Thus, the velocity components at the control points are defined as:

\[
\begin{align*}
X = \top &\Rightarrow \begin{cases} 
u = U + u'(r) \\
v = V \mp w'(r) \sin \phi \\
w = W \pm w'(r) \cos \phi \end{cases} \\
X = \perp &\Rightarrow \begin{cases} u = U \\
v = V \\
w = W \end{cases}
\end{align*}
\]

(7.11) (7.12)

![Diagram of propeller induced velocities](image)

(a) Upward travelling blade

(b) Downward travelling blade

**Figure 7.4:** Direction of propeller induced velocities in the slipstream depending on the blade rotation direction.

The current version of xflrp can position the propeller slipstream at any spanwise position relative to the wing, however it is limited in the vertical \( Z \)-axis where the slipstream center \( P_{t0}(z) \) is fixed at the origin. The relative angle between the slipstream and the wing is also fixed at zero degrees.
Numerical Results

The results of the numerical analysis undertaken for this thesis are discussed in this chapter. The propeller performance determined by experimental methods is compared to the numerical results in Section 8.1. Following which, Section 8.2 compares the slipstream profiles obtained using the slipstream propagation model to the experimentally determined slipstream velocity profiles from the PIV analysis. The chapter is concluded with a comparison of the pylon aerodynamic parameters.

8.1 Propeller Performance

From the experimental tests, two means of determining propeller performance was employed. The first method was the load cell that utilised a bookkeeping methodology to determine a measure of the propeller thrust. The second method was based on the actuator disk theory by determining the velocities in the propeller slipstream using PIV. In this section, the results of these two methods of determining the propeller performance are compared to the numerical results computed.

The computations undertaken did not consider the effect of upstream interactions on the propeller. Thus, a uniform inflow for the propeller was considered in the analytical performance model. Figure 8.1 depicts the performance curve for the experimental and numerical results. For the advance ratios considered, there is no significant difference between the load cell and PIV results, with a difference of less than 1% between the two thrust coefficients. For the lowest advance ratio considered $J = 0.6$, the numerical tool predicts the thrust coefficient to within 3% of these results which is considered a good agreement. For the advance ratio of $J = 0.75$, the numerical tool predicts a thrust coefficient 3% lower than the experimental results. However, for the lowest advance ratio, the model continues with a quasi linear behaviour, resulting in the over prediction in comparison to the experimental results. The reason for the non linearity of the experimental results can not be confirmed. It is reasoned as the trends are witnessed from both the PIV and load cell results, that the issue stems from the model operating conditions.
Based on the results for the lower advance ratios considered, it can be concluded that the performance model is able to predict the propeller performance to a good agreement with the experimental results.

![Figure 8.1: Comparison of experimental and numerical propeller performance results. Reading for $\delta_e = 0^\circ$, short configuration.](image)

### 8.2 Propeller Slipstream Velocity Profiles

The propeller slipstream velocity profiles were derived using the propeller induced velocities generated by XROTOR and the slipstream development model as described in Section 7.2. In order to compare the numerical velocity profiles with the PIV determined slipstream profile, the numerical model is set to calculate the slipstream components at the location of the PIV plane.

The experimental and numerical determined velocity profiles are compared in Figure 8.2. The horizontal blue dashed line indicate the propeller radius, and the green dashed line the propeller hub. During the numerical study, it was found that for the given blade geometry, the induced velocities determined by the model was extremely sensitive to the input sections at which the aerodynamic data was provided. Hence, in the plots, the black upside down triangles indicate the blade sections at which the fitted aerodynamic parameters are provided to XROTOR.

From the results, it is evident that the tool over predicts the induced velocities at the tip section. For the blade sectional region $0.55 < r < 0.7$ the tool is able to closely predict the slipstream velocity profile. The RMS error for this region was found to be 0.0072 for an advance ratio of $J = 0.85$ which corresponds to a corrected advance ratio of $J_{cor} = 0.83$. For the higher thrust setting, the RMS error increases to 0.0904. Thus, based on the RMS
error values, it is determined that the tool shows good agreement with the experimental data for this blade span region. However, the tool overpredicts the velocity profile at the root and tip of the blade. The discrepancy at the root of the blade is attributed to the highly cylindrical airfoil sections (See Appendix xx for blade geometry). The lift and drag polars calculated at these sections by the 2D airfoil analysis tool are unlikely to capture the true nature of the flow at these sections. Similarly, at the tip the propeller blade consists of thin sections with a sharp leading edge. For a non dimensional span near the tip ($r/R = 0.9$) the airfoil section had a thickness $t/c = 0.07$. This again leads to difficulties in the calculation of the lift and drag polars at these sections. Furthermore, Xfoil is unable to accurately predict the stall at these sections that would be occurring at lower advance ratios.

![Figure 8.2: Slipstream induced velocity calculations compared to experimental results.](image)

From the PIV velocity profiles, the slipstream does not immediately converge to the freestream conditions outside the propeller radius. The PIV determined velocities reach freestream levels outside the propeller radius ($r/R = 1.2$). As discussed in Subsection 6.4.4, this could be either due to measurement error, or actual slipstream diffusion with the freestream. The slipstream contraction model does not account for this effect and hence sets the velocity at blade tip ($r/R = 1$) to zero.
8.3 Downstream Pylon Aerodynamic Interaction

During the experiments, the propeller-pylon downstream interaction effects were determined by measuring the lift and the drag of the pylon. This was achieved using balance measurements along with surface pressure orifices on the model. As explained in Section 6.6, during the measurement campaign the pressure orifices in the elevator region were found to be malfunctioning at certain times. Moreover, the short experimental campaign carried out in the LTT as part of this thesis did not include complete pressure data at varied angle of attack. Hence, for the pressure data additional measurements from a more recent test on the same PROWIM 2.0 model carried out in the LTT was utilised to aid in the validation of the xflrp tool.

8.3.1 Spanwise Sectional Lift Coefficient

The spanwise lift coefficient was computed using the pressure data using the procedure outlined in Subsection 4.4.2. The section lift is numerically determined from the slipstream velocities determined from the slipstream propagation model and the xflrp tool which was run for relevant freestream conditions. Figure 8.3 presents the numerical data along with the experimentally determined spanwise lift coefficient distribution.

![Figure 8.3: Comparison of experimental and numerical spanwise lift distribution. Reading for \( \Delta X = 0.43D, \delta_e = 0^\circ, \alpha = 0 \), Reynolds number = \( 0.65 \times 10^6 \).]

The vertical dashed blue vertical line depicts the propeller radius, and the vertical dashed green line the radius of the nacelle. It is clear that the numerical tool underpredicts the sectional lift in the propeller slipstream. A point to note is that the influence of the propeller is limited to within the propeller radius in the numerical model. The experimental results show the contrary, for symmetric inflow condition with the propeller running, the pylon has a local lift coefficient of 0.05 even outside the slipstream. This effect is attributed to the potential nature of the slipstream causing viscous mixing, thus increasing the local lift coefficient marginal even outside the slipstream.
8.3.2 Pylon Lift and Drag Polars

To obtain the lift and drag polars of the pylon, for each angle of attack, the sectional lift and drag of the pylon were integrated to obtain the total lift and drag coefficients of the wing. The resulting data are compared to the lift and drag polars obtained from xflrp in Figure 8.4a and Figure 8.4b respectively. The lift and drag measured for the total model by the balance are also plotted in the figures. From the lift polar, it is seen that xflrp provides an acceptable prediction of the trend of the lift polar of the pylon alone. It does however underpredict the lift coefficient. The numerical results indicate an approximate 12% lesser lift coefficient value at an angle of attack $\alpha = 5$ as compared to the experimental value determined from the wing surface pressure measurements. When comparing the wing only lift to the model lift, the additional lift generated by the nacelle is clearly visible especially at higher angles of attack. As mentioned earlier, this effect can be considered similar to the influence of tip-tanks on the lift polar.

The deviation of the experimental data is expected given the magnitude of difference in the sectional lift coefficient determined previously. As expected, xflrp is limited in the high angle of attack region and is unable to predict the stall characteristics as the tools VLM implementation utilises a linear model.

xflrp however fails to predict the drag polar. Compared to the wing only drag, the numerical results include the thrust component in the drag coefficient. When the thrust component is excluded in the computations, the results still fail to match with the experimentally determined drag polar. Furthermore, the post stall drag converges to a fixed value for both cases. This behaviour requires further investigation in drag computation model of the xflrp tool.

![Figure 8.4: Experimental and numerical lift and drag polars comparison.](image-url)
Part IV

Conclusions and Recommendations
Conclusions and Recommendations

“I don’t want to believe. I want to know.”

– Carl Sagan

From the results of the experimental studies and the comparison of the data by the numerical analysis, the study has helped narrow down the propeller-pylon interaction to certain mechanisms. These interaction mechanisms are discussed in Section 9.1. In order to explore and identify the contribution of each of these interaction mechanisms, recommendations for future studies are given in Section 9.2.

9.1 Conclusions

The aim of this thesis was to understand and explore the propeller-pylon interaction effects present in the horizontal tailplane-tip mounted tractor propeller propulsion concept. In the previous parts of this report, results from varied experimental investigations has been presented with this aim in mind. The study mainly focused on the aeroacoustic and aerodynamic interaction effects, while including the effect of elevator deflections. The conclusions drawn are divided into aeroacoustic interaction effects, and aerodynamic interaction effects.

9.1.1 Aeroacoustic Tractor Propeller-Pylon Interaction Effects

Experimental studies were carried out using the VARPW model to study the aeroacoustic interaction of a trailing pylon with a tractor propeller. The analysis was performed at multiple propeller-pylon spacings and control surface deflections. By comparing the isolated propeller case to the installed pylon, an installation penalty was determined. From the results it was concluded that the installation of the pylon influences both the levels and the directivity of the propeller noise field. Due to the presence of the pylon,
there are increased tonal levels for the circumferential directivity inclined to the pylon. The levels for these directivity angles are inversely proportional to the propeller-pylon spacing. An additional characteristic observed was the change in the spectral directivity shape due to the installation of the pylon. The presence of the pylon leads to the formation of a trough in the noise field for the circumferential directivity in the propeller plane (i.e. $\phi_{\text{mic}} = 180^\circ$). This localized trough is caused by inflow distortions in the propeller inflow caused by the upstream potential effects of the pylon which in turn leads to unsteady blade loading noise. The extent of the inflow distortion is a function of the propeller-pylon spacing. Results indicate that for propeller-pylon spacings lower than $\Delta X = 0.3D$, the unsteady loading noise field cancels the steady loading noise component, leading to lower levels than the isolated propeller case for the considered directivity in the pylon plane ($\phi_{\text{mic}} = 180^\circ$). Based on the acoustic data results from the PROWIM 2.0 model as well, it was concluded that the upstream interaction effect is present only for propeller-pylon spacings less than $\Delta X = 0.3D$, for the given advance ratios considered. Furthermore, elevator deflections cause an increase in the inflow distortion for spacings lower than $\Delta X = 0.3D$.

For larger propeller-pylon spacings, the downstream interaction effects are more prominent. The mechanism of the interaction noise due to the presence of the pylon was narrowed down to the direct interaction of the noise field from the propeller with the pylon and the interaction of the slipstream phenomena with the pylon. One of slipstream interactions being the noise generated due to the impingement of the propeller tip vortex on the pylon.

### 9.1.2 Aerodynamic Tractor Propeller-Pylon Interaction Effects

In order to investigate the magnitude of the upstream and downstream propeller-pylon interactions, a test campaign with PIV was conducted as part of the study. From the results for a propeller-pylon spacing of $\Delta X = 0.43D$ it was concluded that for the given free stream conditions the upstream effect of the trailing pylon was indeed negligible. However, the upstream effect could only be studied for the model excluding the thrusting propeller at a plane 0.33D ahead of the propeller. Given this magnitude of distance ahead of the propeller, and the lack of the higher dynamic pressure induced in the slipstream in the PIV results, it would be premature to conclude that there is no upstream interaction for the considered propeller-pylon spacing $\Delta X = 0.43D$.

The upstream effect of the pylon on the propeller inflow was further investigated by propeller performance measurements using a load cell. Results indicate that for the same operating conditions, the larger propeller-pylon spacing results in an increase in the thrust coefficient. However, due to the inability to determine the exact propeller thrust using a bookkeeping approach, there is a component of the incremental increase in the drag due to the propeller slipstream included in the propeller thrust determined. However, the magnitude of the dimensional thrust difference between these readings were marginal, and was considered to be within the variability of the measurements. A comparison between the propeller performance determined from the PIV flow field and the numerical tool XROTOR was performed to further investigate the magnitude of the upstream interactions. The PIV and load cell readings show good agreement (< 1% relative error) at all advance ratios. However, the numerical tool marginally overpredicts the thrust, given its inability
to accurately determinate the lift and drag of the blade tip region. The effect of elevator deflections on propeller performance was also reconfirmed from calculations of the thrust using the PIV velocity fields. It was concluded that while there was a large relative change in the thrust coefficient due to the effects of the elevator, the absolute change in the dimensional thrust was in the order of 0.3 N which was considered to be within the variability of the measurements. These results are in correlation with the negligible upstream interaction effects determined from the PIV analysis. It was hence concluded that the upstream interaction effects for the given propeller-pylon spacing ($\Delta X = 0.43D$) are negligible, and do not have a significant impact on the propeller performance.

For the downstream interaction effects, PIV measurements in the slipstream plane were undertaken. The results indicate that the potential effects of the elevator deflections are dominated by the propeller slipstream. There exists minor changes in the propeller slipstream velocity profiles due to the elevator deflections, however the changes are not considered to be significant. A numerical slipstream propagation model based on the induced propeller velocities determined by the XROTOR tool was utilised to confirm the PIV determined propeller slipstream velocity profiles. It was found that the numerical model overpredicts the velocity profile in the tip region of the propeller owing to the lack of prediction of stall in this region. However, the tool shows good agreement in the axial velocity profiles in the mid blade span regions ($0.55 > r/R > 0.7$).

The aerodynamic loads on the pylon model were determined using the balance and surface pressure measurements. The results of the balance confirmed prior research with the propeller slipstream increasing the lift of the pylon. The experimental results were compared with numerical results determined using the propeller interaction tool xflrp which utilised the numerically modelled slipstream velocity profiles. The sectional lift coefficient determined by the tool was lower when compared to the calculated values from experimental surface pressure measurements. The experimental determined spanwise lift coefficient distribution shows that the influence of the propeller slipstream is present thorough the span, also outside the boundary of the slipstream. This effect is however neglected in the numerical model. The numerical analysis was able to predict the lift coefficient polar of the pylon model alone to good agreement, but failed to predict the stall of the model which was expected from the VLM based linear method. The drag of the model however could not be determined by the numerical model. From the balance data the effect of the slipstream on elevator deflections showed the expected results, with the propeller slipstream increasing elevator effectiveness by 20%.

From the objectives initially proposed for this thesis in Section 3.1, the effect of the upstream interactions on the acoustic levels and propeller performance could be evaluated. The downstream aerodynamic interactions of the elevator were further quantified and studied. However, the downstream aeroacoustic interactions requires further work. While the current study was able to identify the propeller-pylon interaction mechanism, detailed investigations with the aim of exploring and quantifying the downstream interactions are required.
9.2 Recommendations for Future Work

The thesis aimed to investigate the aerodynamic and aeroacoustic installation effects of the tailplane-tip mounted tractor configuration including the effect of elevator deflections, which led to the conclusions discussed in the previous section. During the experimental research and post-processing of the data, numerous ideas for improvement of the investigations were formed as summarised below.

From the aspect of the aeroacoustic interaction effects, challenging as it is, accurate acoustic source location would give insight into the mechanism of the propeller-pylon interaction. Methods to improve the microphone data such as using a single known source for calibration should be explored. A denser distribution of microphones is recommended to obtain the exact lobes in the directivity due to the installation of the propeller. A comparison of the results determined in this study would be achieved by studying the effect of the direction of rotation of the propeller. To further determine the acoustic interaction mechanism, experimental techniques employing high-frequency pressure measurements such as Kulite transducers on the pylon model, would lead to a deeper understanding of the effects of the blade tip vortex impingement on the acoustic levels.

With respect to the installation effect on propeller performance, there is a lot of scope for further work. The use of a rotating shaft balance to determine time-accurate propeller forces and moments, would provide insight into the effects of the theorised inflow distortions due to the trailing pylon. Furthermore, to determine the exact contribution of the upstream effects of the pylon on the propeller inflow, PIV analysis of the inflow plane could be undertaken with the presence of the thrusting propeller. An extension to this would be to directly determine the pressure field on the surface of the blades using PIV. Phase-locked blade pressure fields would provide the blade loading conditions, which can be utilised to determine the contribution of the unsteady and steady blade loading noise.

As a concluding remark, the work covered thus far serves as a preliminary investigation of the tailplane-tip mounted tractor propeller configuration. A rigorous structural and stability analysis would be required before actual implementation of this configuration.


This chapter contains some additional finer details of the experimental model setup. The details of the external balance utilised in the LTT tests with the Prowim 2.0 model are described in Section A.1 and the motor control unit is discussed in Section A.2. Additional details about the model are presented in Section A.3.

A.1 External Balance Details

The LTT has an integrated external balance as mentioned earlier. A schematic of the components of the balance is illustrated in Figure A.2. The balance has a three strut attachment system for full aircraft models. For half wing models, the model is connected via a turntable mount that serves as the reflection plane. The balance has the ability to rotate in the horizontal plane, resulting in a change of yaw for a full aircraft mode or a change in angle of attack for a half wing model. An illustration of the mounting options is provided in Figure A.1.

The data acquisition from the balance is handled by the CompactRIO system from National Instruments. The is comprised of an embedded Field-Programmable Gate Array (FPGA) that can accept multiple input-output modules and contains a real-time LabVIEW operating system. The custom code pipes the raw stepper motor step counts to an Unix system where these steps are converted to forces via a Fortran program. The custom code accepts a model input file defining the model parameters to calculate the force coefficients from the stepper counts directly.

A.2 Motor Control Unit

The Prowim 2.0 model’s motor is controlled by a dedicated Motor Control Unit (MCU) which is located above the LTT. An interface computer is connected to the MCU which relays the
control input of the computer running the control program at the LTT control terminal. The control program can vary and set the motor speed while monitoring parameters such as the motor’s rotational speed, current, voltage and Volts/Hz ratio. The control computer also reads the temperature in the motor via a thermocouple, and the cooling pump state. There are physical emergency stop buttons, as well as software defined checks that trigger a shut down of the motor in case any of the parameters violate the pre-defined limits.

### A.3 Prowim 2.0 Model Details

As discussed earlier, the PROWIM 2.0 model was equipped with eight spanwise rows of pressure orifices. The chordwise distribution of these pressure orifices is provided as a plot of the distribution in Figure A.3.

![Figure A.3: Schematic of the chordwise distribution of the pressure orifices on the PROWIM 2.0 model.](image)
Figure A.2: Schematic layout details of the 6-component external balance in the LTT; reproduced from [23]
Appendix B

Important Additional Results - VarPW Campaign

The main results from the VarPW campaign were discussed in Chapter 5, however some additional supplementary results from the test are presented in this chapter.

B.0.1 Xfoil Computations of the Upstream Potential Effects

In order to assess the magnitude of change in the propeller inflow that the trailing pylon causes, the potential effects of the trailing potential are briefly examined. This is achieved by a 2D pylon section analysis using Xfoil at the same freestream conditions as were considered during the experiment. The results of the analysis are presented in Figure B.1. The velocity profiles extracted for an upstream position equivalent to the propeller-pylon spacing. For the closest spacing $\Delta X = 0.1D$, there was a computed reduction in the axial freestream component of about 8% of the freestream velocity. It should be noted that the analysis was carried out for the free stream conditions, given the increased dynamic pressure in the slipstream it is expected that the reduction in the freestream axial component would be higher in the actual experimental case due to larger potential effects. The aim of this preliminary analysis is not however to quantify the change in the inflow, but to substantiate and indicate the occurrence of the inflow distortion for upstream propeller due to the pylon at close proximity.

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For the case of the deflected elevator $\delta_e = 30^\circ$, there is a further increase in the inflow distortion as expected due to the increased upwash due to the elevator. This is seen in Figure B.2. Being a symmetric airfoil, the results for the negative elevator deflection $\delta_e = -30^\circ$ is not presented in this report.