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Aerodynamic Loads on an Aft-Mounted Propeller
Induced by the Wing Wake

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Delft University of Technology, Delft, 2629 HS, The Netherlands

This paper presents an experimental and numerical study of the aerodynamic in-plane and out-of-plane loads of a propeller which are induced by the wake of an upstream wing impinging on the lower half of the propeller disk. These loads affect the aircraft’s trim and performance of an aft-mounted propeller configuration at high angle of attack. A simplified wing–propeller model was installed in a low-speed wind-tunnel and measurements were taken with an external balance and a rotating shaft balance to determine the aerodynamic characteristics of the wing and propeller. The installation of the wing shows negligible changes in propeller thrust at low advance ratios, while at medium thrust conditions (\(C_T \approx 0.3\)), the wing shows a small increase in propeller thrust in the order of 1%. The installation of the propeller aft of the wing shows a change on propeller efficiency ranging from \(\Delta \eta_p = -0.01\) to \(+0.04\). The location of the wake impingement at the propeller plane is shown to play an important factor for the time averaged and unsteady propeller loads, confirmed by unsteady RANS computations of a full wing–propeller geometry. The radial location where the largest change in load occurs due to wake impingement, coincides with the location of highest propeller loading. A simplified and computationally efficient method for estimation of the unsteady propeller loads is presented based on predetermined non-uniform inflow fields and the radial distribution of the sensitivity of thrust-to-advance ratio obtained for a propeller with uniform inflow. Although the change in thrust of local sections is overpredicted up to 30%, the method shows good agreement for the integral unsteady blade thrust and integral propeller for different wake impingement locations. This makes the approach suitable as engineering method to predict the effect of a wake impingement on aircraft level.

Nomenclature

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<tr>
<th>Symbol</th>
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<tr>
<td>b</td>
<td>Span [m]</td>
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<tr>
<td>B</td>
<td>Number of propeller blades</td>
<td>(B)</td>
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<td>Chord, mean aerodynamic chord [m]</td>
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Subscripts

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<td>Wing</td>
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I. Introduction

Because of the inherently high efficiency of turboprop propulsion systems compared to turbofan engines, propellers are an attractive means of propulsion for the next-generation passenger aircraft [1,2,3]. However, the installation penalty of large diameter propellers and their noise levels induced to the cabin in the propeller plane, makes the wing-mounted propeller configuration less practical. An alternative is to mount the propellers on pylons or at the aircraft tail, which has the potential to reduce the cabin noise compared to the wing-mounted configuration [2,4,5]. Such layout has been investigated in several studies with a focus on design, aerodynamics and performance [2,4,5]. Recently, the rear mounted propeller is also of interest for distributed propulsion concepts (e.g. in Ref. [9]).

Compared to wing-mounted configurations, the inflow field to the propeller may be less uniform. This inflow is characterized by the wing downwash and, at high angle of attack conditions, also by the wing wake, which will impact the aircraft’s performance. To avoid excessive interaction noise, conceptual design studies (e.g. Ref. [2]) avoid a wing wake impingement on the propeller in normal operating conditions by placing the propeller relatively high compared to the wing. However, at high angle of attack, as sketched in Fig. 1(b), a wake impingement can still occur and increases the propeller thrust and torque locally. As shown in Fig. 1(c), an asymmetric impingement also leads to non-zero inplane forces. These forces directly contribute to the aircraft trim and stability and are especially pronounced for an aft-mounted location of the propeller. For design purposes, a computationally efficient method is preferred to predict the effect of an asymmetric wake impingement on the propeller in-plane, out-of-plane and moments.

![Fig. 1 Schematic of a tail-mounted propeller operating in a downwash and wake field from the main wing. Schematic of in-plane and out-of-plane propeller forces due to non-uniform and asymmetric inflow.](image)

The interaction with an aerodynamic surface upstream of a propeller has been investigated in several studies on traditional pusher configurations. The assessment of increased propeller noise due the impingement of a wake from an upstream wing or pylon are addressed by e.g. Refs. [4,10,14]. In these studies the propeller was positioned relatively close to the trailing edge and the propeller rotation axis is typically aligned with the wing camber line, hence the impingement is close to symmetric and the complete blade encounters the inflow distortion. The studies show that the wake-impingement results in an unsteady propeller loading and changes of the integral loads, both highly depending on the propeller operating condition. The impact of the relative position of the propeller compared to the upstream surface and the in-plane propeller forces were not discussed in detail. The thin wake with a steep velocity profile coming from a pylon generates large suction peaks, primarily in the leading edge region of the blade. Hence the focus of these studies is on interaction noise. Compared to a wake from a pylon, the wing wake generally has more momentum loss but has developed downstream by a widening and reduction of the velocity gradient, while the integral momentum defect is approximately maintained within a few chords from the wing trailing edge [15]. This results in a relatively larger effect on the average inflow to the propeller, while the unsteady loading is more gradual and a larger area of the propeller disk is encountering the disturbance. For this particular non-uniform inflow, a study of the propeller forces is necessary.

This paper addresses the time-average and time-dependent propeller forces due to wake impingement through a combined experimental–numerical analysis. An experiment was performed on a typical wing–propeller layout featuring a straight wing and two locations were considered relative to the downstream propeller. These experimental results provide insight in the time averaged and time dependent integral propeller forces over a large range of operating conditions.
conditions. The presented numerical study on a similar geometry consist of two parts. First, full-blade unsteady RANS CFD simulations provide detailed propeller loading distributions which will contribute to the understanding what the influence is of the location of the wake impingement on the propeller disk. In the second part of the numerical study, a simplified method is presented to predict the location of maximum fluctuations, the unsteady load per blade as well as the integral propeller thrust for the non-uniform inflows considered in this paper. These results can be valuable in the assessment of different locations of the propeller relative to a wake inflow. Additionally, the forces generated by a wake impingement can be put in perspective on aircraft levels.

II. Experimental Setup

This section discusses two experimental setups. The wing–propeller configuration is presented in Section II.A, while a separate isolated propeller experiment was conducted which serves as validation for the numerical simulations of the propeller and is discussed in Section II.B.

A. Wing–Propeller Configuration

The experimental campaign was performed at the Open-Jet Facility (OJF) at Delft University of Technology. The octagonal test section of this low speed open-jet tunnel has a width and height of 2.85m. The wing and propeller model, shown in Fig. 2(a), were installed on a ground plane flush with the wind tunnel exit and acted as a symmetry plane. The propeller featured a diameter of 0.4064 m and its six blades were set to a blade pitch angle of 45 deg at 70% of the radius. The blade geometry is presented in Li et al. [16]. This rather high blade pitch angle was used to achieve representative $T_C$ and $C_T$ values at high advance ratios, typical for full scale propellers at cruise conditions. The propeller was driven by an TDI 1999A airmotor housed inside a nacelle with a radius of 0.2R. The nacelle was connected to a straight support strut, as shown in Fig. 2(a). The propeller forces and moments were measured by means of a Rotating Shaft Balance [17]. A time average was taken over 10 seconds and each condition was measured four times.

The selected wing had no taper, sweep and featured a NACA 64-418 airfoil. Although the setup is a conceptual representation of the configuration shown in Fig. 1, typical values of wing–propeller spacing and the relative size of the wing for an aft-mounted configuration are used, and were based on Goldsmith [2]. Figure 2(b) provides an overview of the chosen values. Two z-positions of the wing were considered such that the location of wake impingement could be altered. The two locations were selected as $z_w = 0$ and $z_w = -0.74R$, where the former case can be considered as a high-wing configuration (Fig. 2(b)), and the latter represents an aft-mounted propeller configuration featuring a low-wing.

The boundary layer on the wing was tripped using zig-zag strip installed on the wing surface at 5% and 10% of the chord on the suction and pressure side of the wing respectively and transition was checked for the considered range of angles of attack. This wing is referred to as the ‘smooth wing’. In addition to this smooth wing geometry, additional roughness as shown in Fig. 2(c) was applied on the wing surface to manipulate the wing boundary layer obtain a wing wake with more momentum loss impinging the propeller, while the Reynolds number for the propeller remained unaltered. The selected roughness was sandpaper with an average particle diameter of 425 $\mu$m. The constant thickness of the sheets resulted in an increase of maximum thickness-to-chord ratio from 18.0% to 18.8%.

The integral forces generated by the wing were acquired using an external six-component balance, which was mounted on a turntable to change the wing angle of attack about its quarter chord line. For the isolated propeller measurements, the propeller setup was installed on this turntable. For the wing–propeller combination, the propeller rotation axis remained parallel to the freestream flow direction. This means that a positive change in wing angle of attack resulted in a wake impinging more towards the propeller tip. It is noted that when installed on an aircraft, the propeller also is at an angle of attack with the freestream flow, hence the location of wake impingement varies more in the experiment by a change of wing lift.

A freestream velocity of 29 m/s was selected for all measurements and was the maximum achievable value for all test cases. This velocity leads to a wing chord based Reynolds number $Re_{ch}$ of approximately 500,000. This Reynolds number is significantly lower than the flight regime of a full scale configuration. Hence, both cases with a smooth wing surface and the surface with applied roughness result in a wake characterized by large momentum thickness. This may result in an over-predicted wake effect on the propeller. The propeller was operated at a range of advance ratios $J$ by changing rotational speed. The cases where additional roughness was applied to the wing were limited to $J = 1.0$, 1.6 and 2.4, which correspond to a high thrust coefficient $C_T$, high efficiency and low thrust coefficient respectively as depicted in Fig. 7.
B. Isolated Propeller for Validation

Both flow field and balance measurements were obtained for a 1:2 scaled version of the propeller in the wing–propeller study, shown in Fig. 2(d), and are used as validation for the computational study discussed in the next section which features this scaled propeller. The minimum trailing edge thickness of the steel blades was maintained at 0.2mm.

The performance of the isolated propeller is characterized by installing it on a nacelle which was mounted on a sting in the center of the wind tunnel section of the Low-Turbulence Tunnel (LTT) at Delft University of Technology, a closed-return low-speed wind tunnel. The drag of the nacelle, spinner and sting are estimated by replacing the propeller with a dummy spinner. Balance measurements were performed at a range of advance ratios at different freestream velocities to determine the dependency of the propeller performance on Reynolds number. A freestream velocity of 40 m/s was selected as baseline and the advance ratio was varied from $J = 2.3$ up to $J = 1.8$. At this velocity, the turbulence level was below 0.1%. Total pressure measurements were taken at $1.1R_p$ distance from the isolated propeller using a pitot tube.

III. Computational Strategy and Validation

An overview of the computational strategy of the isolated and wing–propeller simulations is given in Section III.A. The results of the isolated propeller simulation is validated in Section III.B. Section III.C discusses the computationally efficient method to predict the propeller loads due to the wake impingement.

A. Full Blade CFD Simulations

The numerical analyses of the wing–propeller model are performed by solving the RANS equations for compressible flow time-accurately. These full-blade sliding mesh propeller simulations are performed to capture the unsteady
nature of the wing–propeller interaction. The isolated propeller simulations are solved in a steady manner using a multi-reference frame approach.

The isolated propeller is solved in a wedge shaped domain depicted in Fig. 3(a), consisting of subdomains with refined grids. The propeller blades, spinner and nacelle are modeled as no slip walls. Periodic conditions are used on the sides of the wedge, while a pressure inlet, pressure outlet and pressure far-field boundary condition is set on the remaining boundaries. The rotating domain and the adjacent domain capturing the propeller slipstream are coupled by means of interfaces. To maintain consistency, the wing–propeller domain, shown in Fig. 3(b), contains a copy of the full revolution of the propeller and slipstream domain, matching the mesh of the isolated propeller. A symmetry boundary condition is set on the symmetry plane. A NACA 0015 wing with a semispan equal to three propeller diameters and having an aspect ratio of 7.5 is placed in a refined domain upstream of the propeller. This domain is coupled to the rotating propeller domain by means of an interface. Both domains contain refined grids to avoid excessive dissipation of the wing wake and to reduce the interpolation error at the interfaces.

**Fig. 3 Computational domain and boundary conditions used for the simulations.**

ANSYS® Fluent Release 18.1, an unstructured finite volume cell-centered solver, was used to perform the simulations. A second order spacial and temporal discretization was employed, using a coupled pressure-velocity scheme. The sliding mesh simulations are time-accurate, with a time step equivalent to 2 deg propeller rotation with 35 inner iterations. The flow was prescribed to be fully turbulent using the Spalart–Allmaras turbulence model with the strain/vorticity-based production equation, which was shown to compare well with experiments of propeller–wing simulations with similar Reynolds number. A maximum $y^+$ of 1 is obtained, resulting in a resolved boundary layer. Values for the inlet turbulence quantities were based on the recommendations by Spalart and Rumsey, which resulted in an eddy viscosity ratio of 0.21044. Standard sea-level atmospheric conditions were used for the freestream flow. For the equation of state, an ideal gas was assumed and Sutherland’s law was used to predict the corresponding dynamic viscosity.

**MESH STUDY + MESH DESCRIPTION IS MISSING**

B. Validation of Isolated Propeller Simulation

Figure 5(a) shows the experimentally obtained thrust coefficient $C_T$ curves obtained by means of the balance measurements and the integration of total pressure in the propeller slipstream for a range of freestream velocities. In addition, the CFD results are shown. The lowest advance ratio for each freestream velocity corresponds to the maximum operating condition of the electric motors. Similarly to the findings by Bass, there is a clear influence of the Reynolds number on $C_T$, with a significant shift in the $C_T$ curve at $V_\infty = 20 m/s$ compared to $50 m/s$. For these conditions, the range of Reynolds number based on propeller chord at $r/R_p = 0.7$ is between 40,000 and 150,000. A two-dimensional viscous-inviscid coupled panel code indicates the presence of laminar separation bubbles for moderate lift coefficients (not shown in this paper) and large regions of laminar flow over the blade airfoils at these low Reynolds numbers resulting in a reduction in lift curve slope and lift coefficient at $\alpha = 0$, explaining the dependency of $C_T$ on $V_\infty$. The total pressure profile measured at 1.1 $R_p$ behind the propeller in Fig. 5(b) shows a quite irregular loading distribution at high advance ratios at a radial location with highest loading, which may be the result from laminar separation.
Compared to the measured thrust coefficient, there is both a shift and a lower slope of the $C_T$ curve computed by CFD with $V_\infty = 40\text{m/s}$. From repeat measurements, it was found that the uncertainty in setting the blade pitch was $\pm 0.05$ degree. In addition, the maximum fluctuations of the propeller rotational speed as measured by an encoder were 0.1 Hz. As a conservative estimation of the uncertainty, the two cases with positive and negative are plotted for $J = 1.8$ as well. These uncertainties show to be much smaller than the deviation from the experiment, hence are not likely to be the main factor for the differences with the experiment data. The $C_T - J$ curve intersects at approximately $J = 1.8$ with the experimental data, while the difference at $J = 2.3$ is quite significant. The reduced slope indicates the presence of laminar flow in the experimental test. A better insight in these deviations can be found by comparing the total pressure profile in the slipstream (Fig. 5(b)). The phase averaged total pressure profile by CFD shows good agreement with experiment up to $r/R_p = 0.7$ for the complete range of advance ratios, while at intermediate advance ratios the deviation is clearly in the highly loaded radial locations. The $J = 2.4$ profile nearly coincides with the $J = 2.3$ profile of the experiment, indicating the offset in sectional lift curve slope at those low Reynolds numbers. The higher pressure losses near the nacelle in the experiment are expected to be the result of the transition strip on the nacelle (Fig. 2(e)). From the figure it becomes clear that the appreciable contraction of the slipstream is slightly under estimated by the numerical simulations, similarly to the results in e.g. Ref. [19]. Additionally to more diffusion of the propeller tip vortex in the CFD simulations, it is expected that the large flow angles with respect to the probe leads to an inaccuracy of the total pressure measurement in this region. Although there is an offset in $C_T - J$ curve, the phase average of the propeller slipstream is considered acceptable to assess the installed configuration.

Fig. 4  Mesh refinement study for the isolated propeller and isolated wing grids.
C. Simplified Approach to Predict Propeller Loads

Several authors have treated a wake impingement as a local gust to a blade element model, assuming no influence of the gust on adjacent blade sections and from blade to blade. The latter assumption is justified in e.g. Martinez [22] for inflow disturbances that lead to changes in propeller slipstream having a short wavelength compared to the propeller diameter, typically caused by a thin wake. Due to a local increased circulation at location $r$, the circulation of adjacent sections $r \pm \delta r$ will also increase, which effectively damps the rise in circulation at location $r$; the disturbance is being spread in radial direction and is especially the case for disturbances which cause a gradual change in load. A disturbance as large as the propeller disk itself is effectively a change in advance ratio for the complete disk. If the rise in local thrust at location $r$ due to a change in local advance ratio $\Delta J$ is assumed to be the same as if this $\Delta J$ applies to the complete propeller disk, then the change in local thrust can be approximated by:

$$\Delta \frac{dT}{r}(r, \phi) \approx \Delta \frac{dC_T}{r}(r) \cdot \frac{\Delta J(r, \phi)}{J}$$

where $\Delta \frac{dC_T}{r}$ is the sensitivity of the thrust of a blade section to a change in advance ratio for a propeller in uniform inflow and is only a function of radial position. This approach requires only the multiplication of two matrices to find the change in thrust caused by the non-uniform inflow. In this approach, it is neglected that the unsteady lift lags behind the instantaneous lift [23]. Therefore in the distribution $\Delta \frac{dC_T}{r}$, it is incorporated that the induced velocities at the encounter of non-uniform inflow have developed instantaneously. The $\Delta \frac{dC_T}{r}$ can be found either experimentally, from CFD or from a blade element model. Figure 5(a) shows such distribution computed with CFD for the propeller used in the computational study of this paper for a perturbation in velocity and a perturbation in rotational speed. The sensitivity $\Delta \frac{dC_T}{r}$ follows the distribution of thrust, with its maximum coinciding at the location of highest load. Effectively, the highest loaded region on a blade is more prone to a disturbance in the inflow field. It is noted that the sensitivity is only valid for small perturbations in the inflow field. The non-linear characteristics of the local sections are not included.

To assess the change in local thrust, the most simple approach is to map the non-uniform inflow on a line at a particular phase angle by representing the blade as a line. However, the finite chord of a blade sweeping through the non-uniform flow means that each part of the chord is exposed to a different inflow. To compensate for this effect, one can determine a weighted average of the projected chord on the non-uniform inflow and represent the blade as a surface. These two representations are illustrated in Fig. 6(b), while in Fig. 6(c) an example is depicted of a disturbance distribution and the estimated change in thrust for the two blade representations, where the weighting of the thrust contribution along the chord is taken constant. As expected, the surface representation results in a more smeared change in thrust in radial direction. The two methods applied to a non-uniform flow field will be compared with full blade
simulations in Section IV.C.

IV. Results

In Section IV.A the experimental results of the wing–propeller aerodynamic interaction are presented. The numerical results to illustrate the changes in local blade loading are presented in Section IV.B. Finally, Section IV.C compares the results of the full blade simulations with the simplified approach.

A. Experimental Results Wing–Propeller Interaction

1. Isolated Propeller and Wing Performance

Figure 7(a) provides the thrust and torque coefficients and efficiency of the propeller at no angle of attack which serve as baseline. At this blade angle, the maximum efficiency of the propeller is around \( J = 1.8 \) and corresponds to a high thrust coefficient in the range at which \( C_T \) varies linearly with \( J \). At lower advance ratios, it becomes clear that part of the blade experiences stall, while windmilling occurs at an advance ratio of around \( J = 2.3 \) to 2.4. Because the wing generates a downwash to the propeller, the performance of the isolated propeller at an angle of attack is relevant as reference and is provided in Fig. 7(b). The relative influence of the asymmetric inflow on the local angle of attack on a blade section vanishes at low advance ratio, hence the variation of \( C_T \) with \( \alpha \) is small. At high advance ratios there is a consistent increase in \( C_T \) with increasing angle of attack. At \( J = 1.4 \), the trend of increasing \( C_T \) with angle of attack stagnates and decreases slightly beyond \( \alpha = 10 \) deg. It is expected that this is the result of stall on the propeller blades due to the effectively lower \( J \), as indicated by the \( C_T - J \) curve in Fig. 7(a).

The lift and drag coefficients of the wing are presented in Fig. 7(c) and Fig. 7(d) respectively for two advance ratios representing the maximum thrust and windmilling conditions. The effect of the applied roughness to the wing is clearly observable as a significant reduction in lift curve slope due to the decambering effect of the thicker boundary layer and a significant shift of the minimum drag coefficient indicating the difference in momentum thickness between the two wings. The propeller is shown to slightly increase the lift curve slope of the wing and postpones the onset of separation to a slightly higher angle of attack. The propeller is also shown to decrease the wing drag coefficient slightly over the complete range of \( C_L \), in particular at higher lift coefficients. Similar results are found by Chao and Zhaoguang [24]. It is expected to be the result of the favourable pressure gradient of the propeller for the wing boundary layer, the slightly higher dynamic pressure upstream of the propeller, a slight offset of lift induced drag and the increase in wing circulation, analogous to over-the-wing propellers (e.g. [25]). The lift-to-drag ratio (not shown) is found to increase over the complete range of lift coefficients, with the maximum \( L/D \) value being increased by 4.5%.

2. Downstream Effect of Wing on Propeller Thrust

Figure 8(a) shows the \( C_T \) as function of \( C_L \) for the high and low-wing configurations. The wing lift is altered by providing an incidence of the wing with respect to the freestream flow, while the propeller orientation remains fixed. For
Fig. 7 In (a) and (b), the isolated propeller performance curves at $V_{\infty} = 29$ m/s as measured using a rotating shaft balance. In (c) and (d), the integral lift and drag coefficients of the wing at $z = 0.74R$, with and without additional roughness applied. Results for two advance ratios are shown to indicate the upstream effect of the propeller on the wing.

$J = 1.0$ to $2.0$, the low-wing configuration shows a negligible effect of the wing lift on $C_T$. At the higher lift coefficients, the location of the wing trailing edge and the wing downwash cause the wing wake to go below the propeller disk. The small increase in $C_T$ at lift coefficients larger than $C_L = 0.8$ is therefore dominated by the downwash. This effect diminishes towards the higher advance ratios, in line with the $C_T - \alpha$ curves of the isolated propeller. The high wing configuration shows similar trends which are more pronounced. Lower advance ratios inherently are less sensitive to the inflow disturbance due to the smaller impact on the angle of attack of the blade sections. In addition, the absence of an increase in $C_T$ at $J = 1.0$ compared to $J = 1.6$ can partially be explained by the isolated propeller $C_T - J$ curve. At $J = 1.0$, the performance curve shows a quite insensitive behaviour to $J$ due to expected blade stall. An impinging wing wake reduces the advance ratio locally even further. At $J = 2.4$ and $J = 1.6$, there is a sudden increase of thrust at a lift coefficient coinciding the deviation from the linear $C_L$ vs. $\alpha$ curve (Fig. 7(c)) and indicating that the growing wing wake dominates the change in $C_T$, which is in line with the $C_T - C_D$ curves, where there is especially an increase in thrust for drag coefficients corresponding to the non-linear range of lift. However, the increase in thrust is a combination of the wake-thickening at higher lift coefficients and the different location on the propeller disk of
the wake impingement. Figure 6 (a) shows a clear dependency of thrust sensitivity to a change in inflow on the radial location, indicating that the location of wake impingement affects the integral propeller thrust. The limitations of this experimental setup therefore mean that it cannot directly be determined what the relative contribution of these two effects is to the increase in propeller thrust.

An incidence angle to an isolated propeller leads to a sinusoidal thrust over a full propeller rotation, while a wake entering the propeller disk results locally in a rapid change in propeller thrust. A distinction between the effect of the wing downwash and the impingement of the viscous wake can be made when the propeller thrust is assessed as a function of phase angle. To this end, an phase-averaged thrust is shown in Fig. 8 (a) and (b) for the low, respectively high wing configuration. The results are shown for the isolated propeller and the installed configuration at three different wing lift coefficients at $J = 2.4$ as this is the case with the largest time-average increase in thrust. The curve of the isolated propeller shows a slight dependency on phase angle, characterized by a sinusoidal behaviour. It is expected that this is the result of both the upstream effect of the nacelle support and possibly a slight misalignment of the propeller with the freestream flow. For the installed configuration in Fig. 8 (a), the sinusoidal character of the thrust remains unaltered, indicating the minor influence of the wing downwash on the propeller thrust. With the wing installed,
six distinct peaks can be observed. Over a full rotation, each of the six propeller blades experiences the wing wake when the wing produced almost no lift. The shifts of the wave forms for the different lift coefficients are likely due to the different radial position of the wake which impinges on the propeller disk and is discussed in the next section. At the low lift coefficients, it is expected that the wing wake affects the region in which the propeller loading is highest. At higher lift coefficients, the peaks disappear, in line with the observation that the wing wake goes underneath the propeller disk. The high-wing configuration, Fig. 8(d) shows a similar trend, where the sinusoidal character of the thrust with a period of one revolution is comparable to the isolated propeller thrust. However, the fluctuations with a period equal to \( 2\pi/B \) become more distinct. At \( C_L = 0.02 \), the are small peaks in thrust leading and lagging the highest peak, while these disappear at \( C_L = 0.62 \) and \( C_L = 1.02 \). The origin of these additional peaks is not completely understood. The phase shift between the different wing lift and therefore wake location is apparent. The numerical analysis presented in the final paper will give more insight in the influence of the location at which the wake impinges the propeller on the unsteady loading.

\[ \Delta \eta_p = \eta_p - \eta_{p,iso} \]

\[ \Delta \eta_p = \eta_p - \eta_{p,iso} \]

(a)

Fig. 9 Change in propeller efficiency by the installation of the wing. In (a) the effect of added roughness is highlighted and in (b) the effect of wing position can be observed.

In addition to the change in propeller thrust, the effect of the upstream wing on the propeller efficiency is a relevant quantity to assess as it directly impacts the aircraft’s performance. The non-uniformity of the inflow produced by the wing distorts the propeller loading and hence the propeller efficiency may deviate from the propeller in uniform inflow, a condition for which the propeller is typically designed for. Two factors influencing the propeller efficiency for a given blade pitch angle are the advance ratio and the magnitude of the axial inflow velocity to the propeller disk. Both factors are influenced by the wing wake and downwash, as it reduces both average advance ratio and the average inflow velocity compared to freestream conditions. To this end, it is of interest to compare the propeller loading for the configurations where additional roughness was applied to the wing surface. The change in propeller efficiency as function of wing drag coefficient is shown in Fig. 9(a) for two advance ratios for the high-wing configuration. At an advance ratio of \( J = 1.6 \), considered to be a typical cruise condition (Fig. 7(a)), the propeller efficiency is shown to increase from a small positive value to a \( \Delta \eta_p = 0.03 \) for the smooth wing model. For the same wing drag coefficient, the \( \Delta \eta_p \) for the wing with roughness is shifted and shows values between \( \Delta \eta_p = 0.01 \) up to \( \Delta \eta_p = 0.04 \). The increase in propeller efficiency defined relative to \( V_{\infty} \) is analogous to the higher thrust-to-power ratios found in boundary layer ingestion studies, e.g. Ref. [27]. At \( J = 1.0 \), a condition where part of the propeller blades experience stall, the wing installation results in a reduction of propeller efficiency in the order of \( \Delta \eta_p = -0.005 \) and is not varying significantly with \( C_D \). It is expected that the efficiency gain of the lower inflow velocity due to the wake is being off-set by locally more separated flow. The difference of the high and low-wing configuration without applied roughness is shown in Fig. 9(b). The figure indicates a rather insensitive change in propeller efficiency with \( C_L \) for the low-wing configuration, while the high-wing shows a monotonous increase in \( \Delta \eta_p \). This indicates that the location at which the wake impinges the propeller disk is expected to be a key factor on the propeller efficiency. At a typical cruise lift coefficient of around
$CL = 0.6$, the wing installation shows that the propeller efficiency is increased by $\Delta\eta_p = 0.01$.

**B. Full Blade CFD Analysis of Wing Wake Impingement**

In this section the full blade CFD simulations as described in Section IIIA are discussed which provide more detailed force distributions per blade to determine which regions of the blade are most affected by a wake impingement.

Figure 10 shows the wing wake at the propeller plane for different wing trailing edge–propeller spacings. A spacing of $3.2R$ is selected as baseline case. At this location, the maximum velocity defect is 10% of the freestream flow, equivalent to a maximum $\Delta J = 0.18$ for the reference propeller operating condition of $J = 1.8$. The velocity profiles show that away from the viscous region that the velocity has not fully developed to freestream values due to the presence of the upstream wing. Based on the total pressure profile, the boundary layer thickness of the reference case is $0.20R$ with the edge defined as $0.99^2P_{fr}$.

The three considered wing locations at $J = 1.8$ are $z_w/R = 0.50, 0.75, 1.00$. The propeller thrust distributions for these wake impingement locations are shown in Fig. 11(a). The distribution is determined by integrating the pressure and shear stress along the chord at each phase angle to determine the local section thrust. For the $z_w/R = 0.50$ case there are two distinct regions with increased thrust, while at $z_w/R = 0.75$ there is a quite gradual increase of thrust at the location of wake impingement. Azimuthal variations up to 10% of the local thrust are observed for these two cases. The case where the wake is impinging the propeller tip shows little azimuthal variation in thrust. A better insight is gained by plotting the difference with the isolated propeller thrust distribution, shown in Fig. 11(b), with the red dashed line indicating the blade section with highest thrust in isolated conditions. The increase in thrust for both $z_w/R = 0.50$ and $z_w/R = 0.75$ occurs at a phase angle at which the highest propeller load encounters the wing wake. This is in line with the distribution in Fig. 6(a). Figure 6(a) also shows only a moderate sensitivity of the thrust to a change in advance ratio toward the blade tip, hence the relatively small change in thrust for the $z_w/R = 1.00$ case in Fig. 11(b). By integrating the thrust distribution for one blade at each phase angle, shown in Fig. 11(c), the maximum deviations to isolated conditions are 3 to 8%. For the wake impinging near the blade tip, there is a one-per-revolution thrust increment. At $z_w/R = 0.50$, there is first a peak in blade thrust at $\phi = 130$ deg, then a reduction followed by a higher second peak. It is expected that the second peak is higher due to the thrust enhancement around $\phi = 180$ deg at which the circulation has already developed. The apparent sinusoidal thrust with a period equal to one revolution is expected to be a combination of two effects. When a blade is advancing towards the wake, the blade which already encountered the wake induces axial velocity on this subsequent blade. This results in a reduction in the local angle of attack of the blade advancing towards the wake, hence, a small decrease in thrust at these phase angles can be expected. In addition, the propeller is found to have a small upstream effect to the wing by introducing a positive angle of attack, which leads to a non-zero wing lift and therefore a downwash to the propeller. This downwash leads to a typical sinusoidal blade forcing.

The integral thrust of the propeller is depicted in Fig. 11(d), showing fluctuations around 1% of the propeller thrust at the blade passing frequency. At a blade phase angle of $\phi = 0$ deg, both the $z_w/R = 0.50$ and $z_w/R = 1.00$ show the maximum propeller thrust, coinciding with the phase angle at which the blades encountering the wake. At $z_w/R = 0.75$, this is the case for $\phi = \pm 30$ deg, explaining the phase shift of the integral propeller thrust. A similar phase shift between the different wake impingement locations is also observed in the experimental results in Fig. 8(d). The torque, also depicted in Fig. 11(d), is shown to follow the same trend as the thrust. The smaller increase in thrust than torque indicates in a higher propeller efficiency, equivalent boundary layer or wake ingesting propellers. This figure shows that in case of a wake impingement, no significant degradation of propeller performance is expected.
Fig. 11  Blade thrust distributions at three wing locations with respect to the propeller with a wing trailing edge–propeller spacing of $3.2R$ at $J = 1.8$ and $\alpha = 0$ deg, computed by the full blade CFD simulations.
In addition to changes in propeller thrust, the change of the in-plane forces are an important aspect for the tailmounted propeller configuration. In increment of thrust and therefore torque that arises from an asymmetric wake impingement lead to a net side force, while a torque distribution that is not symmetric with the $x-z$ plane results in a net normal force. The distribution of these in-plane forces are shown in Fig. 12(a) and (b) for the side force coefficient in $y$-direction and normal force $z$-direction respectively. The largest increment in thrust for $\frac{z_w}{R} = 0.50$, occurs relatively close to the propeller axis, hence the additional torque is limited. Therefore the largest local side force on a blade section is observed for $\frac{z_w}{R} = 0.75$, while the impingement at the blade tip at $\frac{z_w}{R} = 1.00$ shows only small increment in side force. Although the maximum sideforce acting on one blade (depicted in Fig. 13(a)) occurs for the $\frac{z_w}{R} = 0.75$ case, the integral of the six blades show a marginally larger side force for $\frac{z_w}{R} = 0.50$ due the bigger segment of the disk which is encountering the wing wake. For all wake impingement locations the side force is positive and do not exceed 2% of the propeller thrust. The normal force in $z$-direction, also shown in Fig. 13(a), is on average slightly negative and is the result of the asymmetry of this force distribution depicted in Fig. 12(b). The higher second peak of thrust and torque when the blade tip is encountering the wake for the second time in a revolution results in a net force on the propeller in negative $z$-direction. To put the magnitude of these in-plane forces in perspective, one can compare them to the normal force arising from an angle of attack. The time average of the gradient of normal force with angle of attack $C_{N_a}$ at $J = 1.8$ was found to be 0.59 rad$^{-1}$ computed at $\alpha = 5$ deg and is comparable with the value of 0.63 rad$^{-1}$ estimated by the method of de Young [28]. A side force of 1% of the isolated thrust is equivalent to a propeller at 0.25 deg angle of attack and shows that these in plane forces are small. Practically, for an aft-location of the propeller in a tail mounted configuration, these inplane forces lead to a yawing and pitching moment, but an equilibrium will be found at a small different yaw and pitch angle. These forces contribute to a different trim attitude, rather than altering the longitudinal and lateral stability significantly.

![Fig. 12 Blade in-plane force and moment distributions at three wing locations with respect to the propeller with a wing trailing edge–propeller spacing of 3.2R at J = 1.8 and $\alpha = 0$ deg, computed by the full blade CFD simulations.]
C. Estimation of Unsteady Propeller Load Using a Simplified Approach

In Section IIIC an approach is presented to estimate the local blade forces due to a non-uniform inflow, based on the sensitivity of the blade sections of a propeller in uniform inflow and the non-uniform inflow which is not altered by the propeller installation. In this section the predicted force distributions are compared with the results obtained from the full blade simulations.

First, a comparison is made between the representation of the blade as a line or a surface sweeping through the non-uniform inflow. The change in local thrust distribution is shown in Figure 14(a) for \( z_w/R = 0.50 \) as for this case local changes in thrust are more pronounced than the other considered wing locations. The line model does capture the location of maximum increment in thrust quite well, although there is a significant overprediction of the local thrust and the area of the increased thrust exactly follows the wake inflow. In Fig. 6(c) the difference is highlighted in section thrust computed by a line and a surface that equally extends in both chordwise directions. The line model concentrates the increment in load only to the region of non-uniform. Describing the blade as a quarter chord line with a chord approximately equal to the projected area of the blade, the distribution in thrust is more smeared and is no longer symmetric, as shown in Fig. 14(a). The magnitude of the thrust increment is still overpredicted, but the distribution of \( \Delta T/r \) is close to the full blade solution. It is also observed that the instantaneous thrust of the surface representation is not as smeared in radial and azimuthal direction compared to the full blade solution. This means that although there is a local overprediction of the thrust, the integral blade load is not necessarily overpredicted with by the same factor. The estimated integral blade loads are compared in Figs. 14(b) through (d) for different wing locations by integrating \( \Delta T/r \) for each phase angle. The curves show that the phase angle as well as the magnitude corresponding with the highest load of the full blade simulation are predicted well by the simplified approach. The sinusoidal behaviour of the thrust for all three wing locations was noted as the upstream effect of the propeller on the wing in the previous section. This effect is not included in the simplified approach, hence the difference with the full blade simulation outside the wake region. To indicate its impact on the rest of the distribution, a sinusoid is added to the estimated distribution, also depicted in Figs. 14(b) through (d). Especially for the case where the wing wake is impinging at the tip, this yields to almost an exact match with the full blade simulation. The slight underprediction of the blade loading is expected to be caused by the additional wake inflow due to the pressure field induced by the propeller. For the estimated curve, the inflow is not corrected for the upstream effect. For the other two cases, there is an overprediction of the blade load especially at the peaks.

The change in integral load of the propeller compared to isolated condition is presented in Figs. 14(e) through (g). The change in thrust as a function of wing location is a smooth curve with a relatively constant maximum between \( z_w/R = 0.3 \) to 0.6. The largest gradient occurs around \( z_w/R = 0.75 \) where the blade sections most sensitive to a inflow disturbance contribute to the thrust increment. The estimated curves follow the trend of the full blade simulation well, although the overprediction of propeller thrust compared to full blade simulations is 10%. As expected, the estimation model predicts no change in propeller normal force due to the symmetry of the distribution.

It is noted that the computational time of the simplified approach compared to the full blade simulations is extremely reduced. While the full blade simulations presented in this paper required 4,500 CPU hours for each point, the data...
points of the curves presented in Figs. 14(e) through (g) were computed within seconds, excluding the computation of the non-uniform inflow field and the sensitivity distributions. This makes the simplified method an attractive method for design purposes where the disturbance of the inflow field is moderate.
(a) Estimated change in thrust due to wake impingement

(b) Wing $z_w/R_p = 0.50$

(c) Wing $z_w/R_p = 0.75$

(d) Wing $z_w/R_p = 1.00$

(e) Change in propeller thrust for different wing locations

(f) Change in propeller thrust for different wing locations

(g) Change in propeller thrust for different wing locations

Fig. 14  Comparison of full blade simulation to the simplified approach to predict blade loadings with a wing trailing edge–propeller spacing of $3.2R$ at $J = 1.8$ and $\alpha = 0$ deg.
V. Conclusions

This paper present an experimental and numerical study of the propeller forces due to an asymmetric impingement of a wake. The following conclusions are formulated.

The installation of an upstream wing in a low-wing configuration shows negligible changes in propeller thrust at low advance ratios. At medium thrust conditions ($C_T = 0.3$), the wing shows a small increase in propeller thrust in the order of $\Delta C_T = 0.01$. A high-wing configuration where the propeller is approximately aligned with the wing chord, shows the largest effect on propeller thrust, observed in both time-average and time-dependent propeller loading. At a high advance ratio of $J = 2.4$, the installation of the high-wing resulted in a change in $C_T$ from $-0.045$ to $0.03$. The wake impingement is the dominating factor in changing the propeller thrust and the wing induced downwash is a secondary factor. This is confirmed by the distinct loading pattern observed in the time-dependent integral thrust values, with an increase in thrust at the phase angle at which the wing wake impinges the propeller blades.

From the full blade unsteady RANS simulations it is found that largest increment in propeller thrust coincides with the radial locations having the largest thrust in isolated conditions. This is analogous to the largest increase in section thrust observed of an isolated propeller subject to a small change in advance ratio. The larger sensitivity to the non-uniform inflow at these radial location results in a two-per-revolution blade thrust increment if the wake impinges at a wake location from the hub towards the highest loaded blade region (around $r/R = 0.85$), while a wake impingement near the tip results in a one-per-revolution thrust increment. The maximum changes in integral out-of-plane and in-plane forces occur when a wake impinges from in the region spanning the hub up to 60% of the radius.

A wake with a maximum velocity defect of 10% of the freestream flow results in deviations from isolated blade thrust in the order of 8%, while the integral thrust is increased in the order of 2%. Inplane forces are found up to 1% of the isolated thrust and are equivalent to a propeller at 0.10 to 0.25 deg angle of side slip or angle of attack. These inplane forces lead to a small change in trim attitude, rather than altering the longitudinal and lateral stability significantly.

A simplified approach is presented to estimate the propeller load distribution by the multiplication of the distribution force-to-advance ratio sensitivity with the non-uniform inflow field. A representation of the blade as a surface sweeping through the non-uniform field is found to overpredict section thrust coefficients up to 30%. However, the integral blade load as function of phase angle shows good agreement with the full blade simulations for the considered wake impingement locations. The significant benefit in terms of computational efficiency compared to full blade CFD simulations makes the simplified method an attractive approach for design purposes where the disturbance of the inflow field to the propeller is moderate.

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References


