

UC Davis

UC Davis Previously Published Works

Title

Prediction of Interactional Aerodynamic Noise of Propellers and Wing Using High-Fidelity CFD

Permalink

<https://escholarship.org/uc/item/5sf35396>

Journal

Proceedings of the Vertical Flight Society 79th Annual Forum and Technology Display

Authors

Trembois, Nikos
Ramsarran, Tyler
Brown, Ethan
et al.

Publication Date

2023-05-16

DOI

10.4050/f-0079-2023-17954

Prediction of Interactional Aerodynamic Noise of Propellers and Wing Using High-Fidelity CFD

Nikos Trembois

PhD Candidate
University of California
Davis, California, USA

Ethan Brown

Undergraduate Student
University of California
Davis, California, USA

Seongkyu Lee

Associate Professor
University of California
Davis, California, USA

Tyler Ramsarran

Graduate Research Assistant
Penn State University
University Park, PA, USA

Kenneth S. Brentner

Professor
Penn State University
University Park, PA, USA

ABSTRACT

This study aims to enhance the understanding of the upstream-downstream interaction between propellers and wings in terms of their acoustic impact using high-fidelity simulations. By comparing the high-fidelity CFD results with medium-fidelity results obtained using a viscous vortex particle method, the validity of each model assessed with respect to experimental data. The research focuses on a propeller placed upstream of a wing, with the wing positioned at various horizontal and vertical offsets to evaluate the sensitivity of noise to wing placement. The isolated propeller thrust is found to match well with the experimental data. However, when the wing is present, the thrust is over-predicted compared to the experimental data. The pressure coefficient and its root-mean-square (RMS) values are used to evaluate the wing aerodynamics. The RMS of the pressure coefficient reveals significant unsteady loading near the wing leading edge due to the interactional effect between the propeller and the wing. In terms of noise prediction, the isolated propeller noise is well predicted in both high-fidelity and medium-fidelity simulations, but marginal improvement is observed in the high-fidelity simulations. When the wing is present, the wing loading noise exceeds the propeller loading noise, particularly in closer proximity to the propeller. It is found that the vertical offset of the wing relative to the propeller has a more significant influence on the noise produced at the blade passing frequencies compared to the horizontal offset. As the leading edge of the wing approaches the blade tip at $\Psi = 0$, noise levels increase. Finally, the directivity of the noise is compared with experimental data for the propeller and wing configuration. The predictions do not agree as well with the experimental data as desired, possibly due to the over-prediction of propeller thrust, the associated inaccuracies in the phase of the wing noise, and the acoustic shielding by the wing.

INTRODUCTION

The aerodynamic interaction between propellers and wings has gained significant interest recently, particularly for modern high-speed tiltrotors, unmanned aerial systems (UAS), and advanced air mobility (AAM) aircraft that use distributed electric propulsion. Tiltrotors, for instance, have the ability to transition from hover to forward flight, enabling them to fly faster than traditional helicopters. Despite this, limited research has been conducted to understand the impact of flow interactions on the performance, aerodynamics, and acoustics of such vehicles. In order to advance the design of these aircraft, it is necessary to have validated predictive capabilities and an improved understanding of the detailed flow physics and acoustics. This requires the use of high-fidelity computa-

tional fluid dynamics (CFD) to accurately capture the intricate flow physics and predict the aerodynamic noise generated by both the propeller and wing.

Previous studies have demonstrated that the installation effects of propeller and wing interaction have a significant impact on propeller and wing unsteady loading, as well as overall vehicle performance (Refs. 1–4). These unsteady aerodynamic loads can exacerbate the noise generated by the propeller blades. Moreover, the impingement of propeller tip vortices and wakes on the wing can induce unsteady aerodynamic force fluctuations on the wing, which in turn lead to noise. Turbulent flows from rotor wakes, in particular, can generate a combination of nearly periodic and stochastic loads on the wing surface, resulting in both narrowband random noise and broadband noise. Furthermore, additional unsteady loading can be generated from the interaction between propeller wakes, flaps, and other propellers. At high angles of attack and during transition, flow separation on the wing and flaps

Presented at the Vertical Flight Society 79th Annual Forum & Technology Display, West Palm Beach, Florida, May 16–18, 2023. Copyright © 2023 by AHS - The Vertical Flight Society. All rights reserved.

can substantially influence propeller and wing interactional aerodynamics and acoustics. Therefore, it is essential to unravel the complex flow physics involved in propeller and wing interaction and determine the directivity, level, and characteristics of propeller and wing interactional noise sources.

In a recent investigation using high-fidelity CFD, Lim (Ref. 1) analyzed the mechanisms of rotor and wing interactions in the XV-15 tiltrotor. Lim found that wing thickness and lifting wing circulation can cause perturbations of in-plane and out-of-plane velocities on the blade. These perturbations resulted in changes in the propeller blade angles of attack, leading to 3/rev harmonic loadings for a three-bladed propeller. However, Lim did not examine the aerodynamic noise.

While high-fidelity CFD is necessary to investigate detailed flow physics, performing sensitivity studies for a wide range of parameters may be impractical due to CFD's computational costs. Zhang et al. (Ref. 2) used medium-fidelity vortex models in the Rotorcraft Comprehensive Analysis System (RCAS) (Refs. 5, 6) to investigate proprotor and wing acoustics. They found that the effect of interactional aerodynamics on acoustics is significant, and the noise increase due to the interaction could be as large as 10 dB or more. However, they did not include the wing as a noise source, which could be significant. Quackenbush et al. (Ref. 3) used medium-fidelity numerical models in CHARM to predict proprotor/wing/flap interactions and validated their vehicle performance predictions against experimental data (Ref. 7). However, the correlation with pitching moment data showed significant dependence on model settings, and detailed unsteady aerodynamics and acoustics were not presented.

Stephenson et al. (Ref. 8) conducted experimental studies in a wind tunnel on the interactions between a pusher propeller and a wing. The tests were conducted with various rotation rates, wind tunnel speeds, yaw conditions, and wing configurations (none, half, and full). However, the authors reported data measurement issues related to acoustic reflection, flow and tunnel noise, and microphones being impinged from upstream wakes. Despite these data issues, the results showed that the presence of an upstream body can significantly impact the acoustic emissions of a propeller system. Furthermore, the study found that any unique change in wing configuration, such as going from half to full wing, produced a distinct acoustic emission from the propeller system. This highlights the importance of accurate modeling of upstream bodies for predicting acoustic emissions correctly.

Previous studies demonstrate the importance of interactional aerodynamics of the proprotor and wing problem on performance and acoustics. However, physical understanding of the interactional aerodynamics and noise for proprotor and wing has not been well developed due to the limitations of previous work and the inherent complexity of rotor/wing and rotor/rotor interactions. The current medium-fidelity vortex models (free-wake or vortex particle method) may not be sophisticated enough to accurately capture complex flow behavior during interactional aerodynamics. Additionally, narrow-band random noise and broadband noise, significant contrib-

utors to annoyance and civil noise certification metrics, may increase for proprotor and wing interactions due to turbulence flow interactions between the rotor wake, wing, and flaps or interactions between rotors. However, these non-deterministic noise sources have not been thoroughly studied for proprotor and wing interactions. Current empirical models for rotor broadband noise are unlikely to capture these effects.

In the current paper, the Mejzlik propeller (Ref. 4) is selected to study the aerodynamic and acoustic interactions between a propeller and wing. The main objectives of this paper are to utilize high-fidelity CFD to capture aerodynamic noise sources on both the propeller and wing, investigate the noise characteristics and sensitivity for different wing positions, and compare noise predictions between CFD and mid-fidelity vortex methods such as a viscous vortex particle method. This paper solely focuses on the tonal noise generated from the propeller blades and wing, specifically the first blade passing frequency (BPF). Li and Lee (Refs. 9–11) highlighted the importance of broadband noise in rotorcraft noise. However, broadband noise will not be considered in the current paper. A companion paper (Ref. 12) presents some preliminary results of broadband noise of the Mejzlik propeller with wing.

TECHNICAL APPROACH

Model

This paper studies the Mejzlik 16x11 3-bladed propeller and validates against wind tunnel experiments (Ref. 4). Figure 1 shows the complete configuration of the CFD model. The propeller geometry is obtained from off-the-shelf sources. The motor and spinner are modeled as a single solid object, which is referred to as the nacelle. On the other hand, the wing is disconnected from the propeller-nacelle assembly. This is consistent with the reference wind tunnel setup, which uses a traverse wing assembly to place the wing in proximity of the propeller without fastening the wing to the nacelle. The wing section shape is the cambered NACA 63₂ – 215 MOD B airfoil. The span of the wing is 3 ft, with the mid-span located at the propeller centerline. Table 1 summarizes the geometric parameters and flow conditions.

The model is simulated as an isolated propeller and with the wing in six different positions. The wing with closest proximity to the center of the propeller is ($\Delta x = 0.25R$, $\Delta z = 0.5R$) where R indicates the propeller blade radius. From this point, the wing moves either upward or aft. There are two additional variations in the wing height $\Delta z = 0.75R$ and $\Delta z = R$. The horizontal wing positions are $\Delta x = 0.5R$, $\Delta x = R$ and $\Delta x = 1.5R$. Figure 2 shows the wing leading-edge location for the simulations as blue diamonds.

Figure 3 shows a schematic of the measurement locations for the pressure coefficient (C_p) along the chordwise direction on the wing. The advancing and retreating locations are used to extract data for C_p . Figure 4 shows the measurement locations for the root-mean-square (RMS) of the pressure coefficient fluctuation ($C'_{p,RMS}$). The inboard and tip vortex trajectory locations are used to extract the RMS.

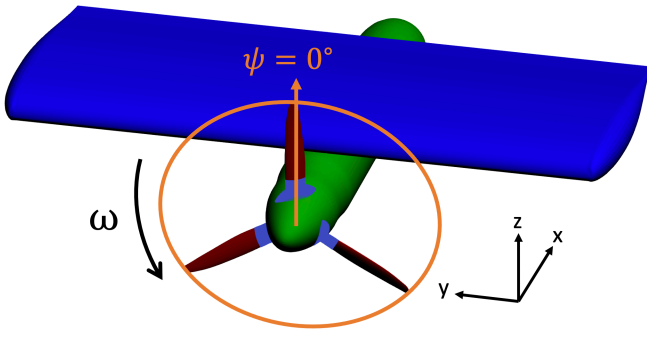


Fig. 1: CFD model for propeller-nacelle assembly with wing surface and the definition of azimuth angle.

Table 1: Configuration geometry and flow conditions.

Parameter	Value
Propeller radius	7.875 in
RPM	6000
M_∞	0.068
M_{tip}	0.361
Advance ratio (J)	0.592
Wing chord	16 in
Wing span	36 in
Wing angle of attack (α)	0°

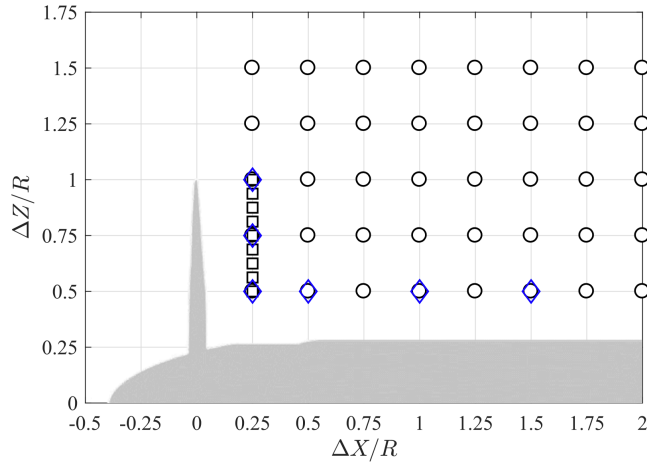


Fig. 2: Wing positions used for simulations shown as blue diamonds (Ref. 4).

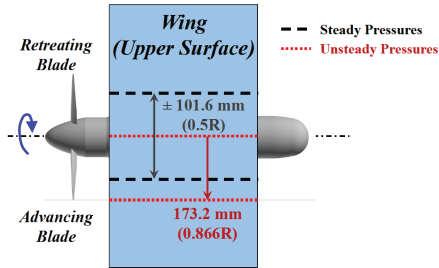


Fig. 3: Schematic of C_p measurement locations (Ref. 4).

CFD Grid

The propeller geometry is modeled in the near-body mesh using structured overset grids created with the aid of Chimera

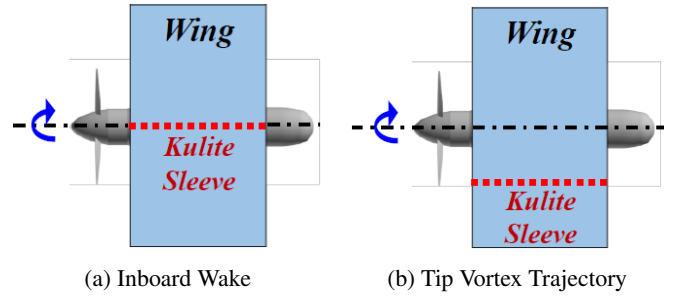


Fig. 4: Schematic of $C'_{p,RMS}$ measurement locations (Ref. 4).

Grid Tools (Refs. 13, 14) and inhouse UCD-BladeGenerator (Ref. 15). The blade is connected to the stationary nacelle via a sliding mesh collar grid. The wing is detached from the nacelle and uses rounded cap grids to enclose the gaps of the O-type grid.

The near-body grids, shown in Fig. 5, extend one blade chord length from the surfaces of the propeller and nacelle. The spacing of the grid components is measured with respect to the reference chord. The reference chord, c_{ref} , is chosen to be the chord length where $r/R = 0.75$, which is 1.2 inches. The blade surface uses 125 points in the chordwise direction, both on the suction and pressure sides. There are 150 points along the span of the blade with 0.1% c_{ref} spacing at the tip to resolve gradients in the vortices, where c_{ref} represents the blade tip chord length. The surface normal spacing of the near-body grids increases from $y^+ = 0.5$ at the wall to 10% c_{ref} . The level-1 refinement box overlaps with the near-body mesh and has a uniform Cartesian grid spacing of 10% c_{ref} . This refinement box is necessary to capture the fine details in the propeller wake. Its domain is set to fully enclose the propeller wake and tip vortices, as shown in Fig. 6. The Cartesian off-body grids are extended from the refinement box to the freestream boundary conditions in eight-level increments. The computational domain is a cube with a length, width, and height equal to 20 propeller radii. The grid spacing is summarized in Table 2, while the computational points required for this mesh are provided in Table 3. A total of 24.9 million grid points are used in the near-body mesh, while 15.8 million grid points are used in the off-body mesh. Therefore, the total number of grid points used is 47.3 million.

Table 2: Grid spacing

Grid spacing location	Grid Spacing
Tip spanwise spacing	0.1% c_{ref}
Leading-Edge chordwise spacing	0.05% c_{ref}
Trailing-Edge chordwise spacing	0.02% c_{ref}
Wall-Normal spacing	($y^+ < 1$)
Level-1 Spacing	10% c_{ref}

CFD Simulation

NASA's OVERFLOW (Refs. 16, 17) is used as CFD tool to solve unsteady compressible Reynolds-averaged Navier-Stokes (RANS) equations. To capture the detailed vortical

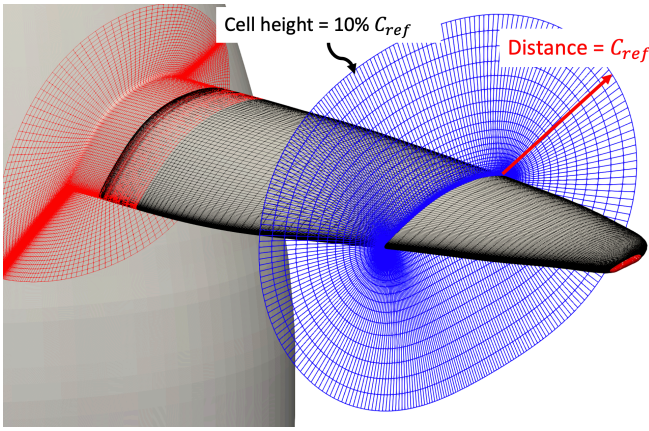


Fig. 5: The blade surface grid with a slice of the near-body volume grids.

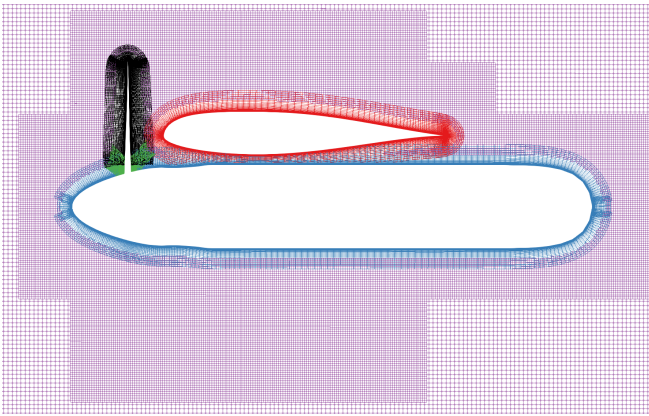


Fig. 6: Near-Body and off-body grids of the Mejzlik propeller with nacelle and wing

Table 3: Grid points

Component	Grid Points	Percent of Total
Blade	2.54 M	5.4%
Propeller & Nacelle	8.96 M	18.8%
Wing	2.84 M	6%
Near-Body	24.9 M	52.6%
Off-Body	15.8 M	47.4%
Total	47.3 M	100%

eddies in the wake region, the Spalart-Allmaras (SA) delayed detached eddy simulation (DDES) turbulence model is used. The simulation is advanced by 0.25 azimuthal degrees per physical time step. Dual-time stepping with 40 pseudo-time subiterations is used for each physical time step. A second-order backward temporal differencing scheme and a fifth-order central spatial scheme for inviscid flux terms are used.

OVERFLOW has been widely used in the field of rotor-to-rotor interactional aerodynamic and acoustic problems. For example, Jia and Lee have recently applied OVERFLOW to study the aeroacoustics of coaxial rotors (Refs. 18–20), quadrotors (Ref. 21), and side-by-side rotors (Ref. 22).

RCAS Simulation

Although this paper focuses on high-fidelity CFD, results were compared against the viscous vortex particle method (VVPM) mid-fidelity model in RCAS (Refs. 5, 6). This comparison is useful to evaluate the differences between CFD and medium-fidelity results and to assess whether a subset of CFD results can provide guidance to improve the medium-fidelity models.

Quoting Ref. 5, “The Rotorcraft Comprehensive Analysis System (RCAS) is a comprehensive analysis developed by the U.S. Army to provide state-of-the-art rotorcraft modeling and analysis technology for Government, industry, and academia. It is a multi-disciplinary software system capable of detailed modeling a wide range of rotorcraft configurations operating in hover, forward flight, and maneuvering flight to predict performance, loads, vibration, flight dynamics, and aeroelastic stability.” RCAS is a medium-fidelity level tool enabling more rapid testing of designs than CFD approaches – at the cost of accuracy and detail. For this study, RCAS is used to model a Mejzlik propeller with the intent of focusing on the aerodynamic modeling for periodic test cases to allow for acoustic analysis. As such, the RCAS simulation models an isolated case of the Mejzlik propeller (Fig. 7), and an interaction case of the Mejzlik propeller in front of an unswept wing with NACA 63₂ – 215 MOD B airfoil throughout the span (Fig. 8). The propeller alone is first used to establish a baseline validation before moving to the interaction case. The blades and wing are modeled as structurally rigid and the only control implemented is a collective pitch bearing. Adjustment of the collective pitch is used to match the thrust coefficient of experimental data. In the noise prediction, only loading noise from the propeller and wing is considered.

The aerodynamic model for RCAS defines a constant rotational rate for the propeller, which is chosen to match the test conditions used in the CFD simulation. VVPM is selected for calculating the inflow of the propeller because it simulates the interactional effects between the propeller and a wing, which cannot be captured by a prescribed or free-wake method. A periodic analysis is chosen to calculate the performance and acoustics in level, axial flight. Vortex particles are released from 20 spanwise segments along each blade. Seven airfoils are also specified spanwise for the blades of the propeller, each with C81 lookup tables of lift, drag, and moment coefficients. The specified airfoil geometries are generated by measurements of the propeller CAD model, which implement closer spacing near the blade tip to best resolve the twist distribution of the original propeller. In the current paper, however, NACA 0012 airfoils with zero-lift angle of attack correction are utilized in the C81 table.

Aeroacoustics

The Ffowcs Williams and Hawkings (FW-H) solver PSU-WOPWOP (Refs. 23–27) is used to predict thickness and loading noise from propeller blade and wing surfaces using Farassat’s formulation 1A (Refs. 28, 29).

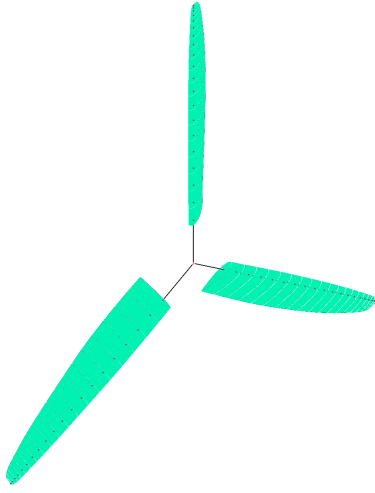


Fig. 7: Aerodynamic model of the isolated propeller in RCAS

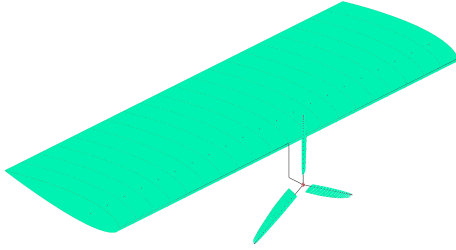


Fig. 8: Aerodynamic model of the propeller-wing in RCAS

The loads on the propeller remain invariant from one revolution to another to within a tolerance of 0.1% thrust once the solution is converged. Hence, the loading is considered periodic, and only one revolution of surface data is used to predict the noise. The surface data is extracted at every 0.5° azimuthal angle over the last revolution. The surface pressure and loading data are then propagated to the observers in the FW-H equation through an undisturbed atmosphere. The impermeable surfaces, including the propeller blades and wing, are used to calculate the acoustic contributions from the thickness noise and loading noise from the propeller and wing.

Tonal noise acoustic pressure and sound pressure level (SPL) are obtained at microphones. The directivity is calculated at the first BPF at 28 observer locations in a linear array, as shown in Fig. 9. The linear array is parallel to the propeller axis at a distance of 3.5 m at its closest point. This places the closest observer 17.5 blade radii from the propeller, which is in the acoustic far field. The elevation angle of the observers range from 45° to 150° , as measured from the axis of rotation of the propeller, as shown in Fig. 9(a). The microphone array has also a lateral angle of 50° , as shown in Fig. 9(b).

RESULTS

Aerodynamics

Flow Fields Figures 10 and 11 visualize flow interactions between the propeller and the wing with its leading-edge located

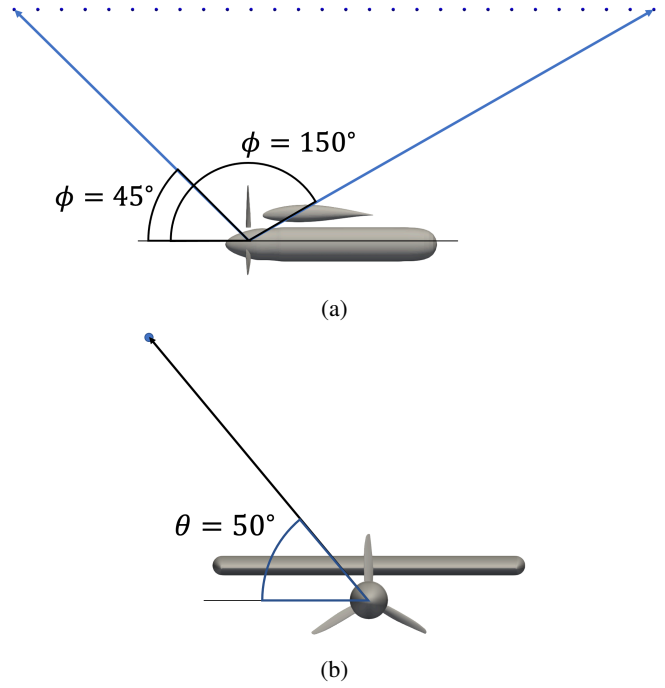


Fig. 9: Linear array of microphone positions with respect to propeller: (a) elevation angle and (b) lateral angle

at ($\Delta x = 0.25R$, $\Delta z = 0.5R$) from the center of the propeller. Figure 10 shows vorticity contours in a side view that cuts through the center of the propeller, visualizing the propeller tip vortices and the boundary layers near the wing and nacelle. The contraction of the tip vortex is more pronounced above the wing suction side after the flow passes by the wing due to the flow suction effect. Weak vortices are also shed at the root-nacelle connection from the sliding mesh grid. Figure 11 shows the iso-surface of the normalized Q-criterion colored by the vorticity magnitude in an isometric view. The wingspan in the computation is large enough to completely contain the propeller tip vortices and avoid interaction between the propeller wake and wing tips. It is expected that the interaction of the propeller tip vortices with the wing generates unsteady pressure fluctuations on the wing.

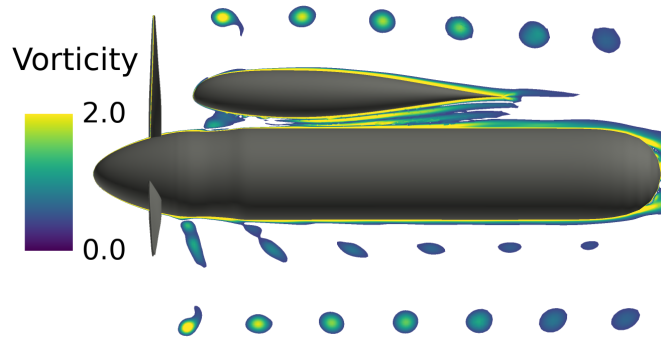


Fig. 10: Vorticity contours in the side view at the center of the propeller and wing.

Figure 12 shows the pressure coefficient contours at an instant

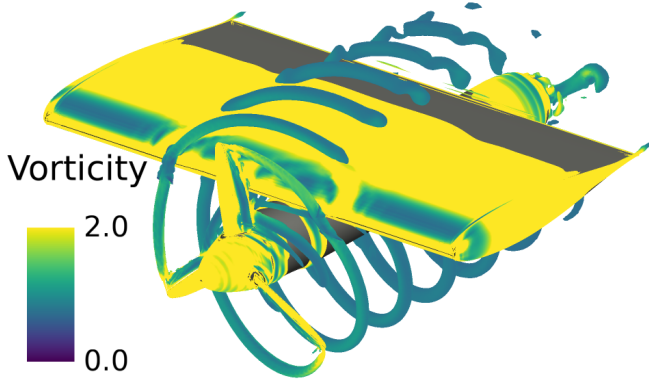


Fig. 11: Iso-surface of normalized Q-criterion colored by the vorticity magnitude.

in the plane that cuts through the center of the nacelle for the isolated propeller and the propeller with the wing. It clearly shows the wing increases the pressure on the pressure side of the propeller, leading to an increase in the propeller thrust, which will be quantified in more detail later. Additionally, it has been observed that the wing is influenced by the potential flow or pressure exerted by the propeller blades. This will induce unsteady aerodynamics on the wing, which generates noise, in addition to the propeller wake and tip vortex interactions with the wing.

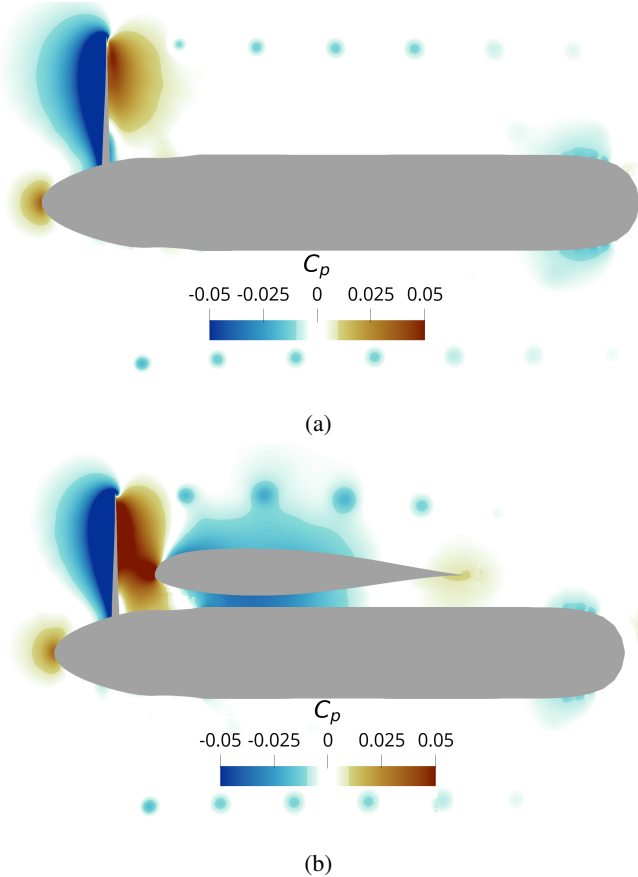


Fig. 12: Pressure coefficient contour: (a) without the wing and (b) with the wing mounted.

Thrust and Torque The isolated propeller aerodynamics are calculated as the averaged value from all blades during the last revolution of the simulation. The quantities evaluated are the thrust coefficient ($C_T = T / \rho_\infty n^2 D_p^4$) and torque coefficient ($C_Q = Q / \rho_\infty n^2 D_p^5$), where ρ_∞ is the freestream density, n is the revolutions per second, and D_p is the propeller diameter. The thrust coefficient is predicted within 2% of the experimental measurements. However, the torque coefficient has 11% deviation compared to the measurements. The thrust and torque calculations are summarized in Table 4.

Table 4: Thrust and torque coefficient comparison between simulation and experiment (Ref. 4)

	Experimental	Simulation	Error (%)
C_T	0.0874	0.0890	1.78
C_Q	0.0128	0.0113	11.78

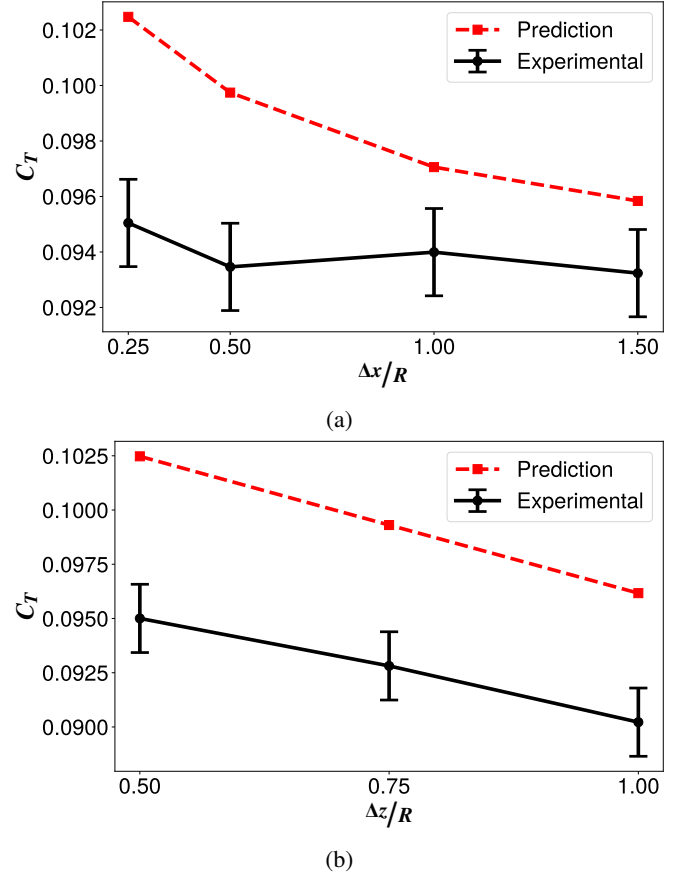


Fig. 13: Variation of the propeller thrust as a function of the wing position : (a) vertical movement and (b) aftward movement (Experimental data are from (Ref. 4)).

Figure 13 shows the effect of the wing position on the propeller's thrust evaluated with computational prediction and experimental results, with the experimental results including 2% error bars. As the wing is positioned closer to the propeller disk, the bound circulation from the wing increases the propeller thrust, as shown in Fig. 13(b). Additionally, the prop-

propeller thrust decreases as the wing position moves upward, as shown in Fig. 13(a). However, the simulation over-predicts this behavior. Although the trends are well captured in the simulations, the thrust is consistently over-predicted. This high-magnitude thrust can affect the noise levels, not just from the propeller but also from the wing due to the interaction effect. For instance, the tip vortex and wakes generated by the propeller blades convect rapidly in high thrust conditions, leading to alterations in both the magnitude and phase of the unsteady loading experienced by the wing. This will be discussed in more detail later.

Propeller Aerodynamics The vertical and horizontal position of the wing affects the thrust distribution on the propeller disk. Figure 14 shows the sectional thrust coefficient of one blade throughout one revolution of the isolated propeller. For the steady freestream and unobstructed wake, the thrust contour for the isolated propeller is azimuthally invariant. Figure 15 shows the effect of the wing location on the sectional thrust coefficient as the relative increase of sectional thrust with respect to the isolated propeller. The horizontal bar indicates the wing position. When the wing is included, the propeller thrust increases over the majority of the propeller disk for all wing positions. However, the thrust decreases in the inboard section near $\Psi = 90^\circ$ (directly to the left), especially at $\Delta x = 0.25R$. The maximum increase in sectional thrust is near the wing leading-edge due to the wing circulation creating an increased angle of attack on the blades. As the wing moves downstream, the results approach the characteristics of the isolated propeller.

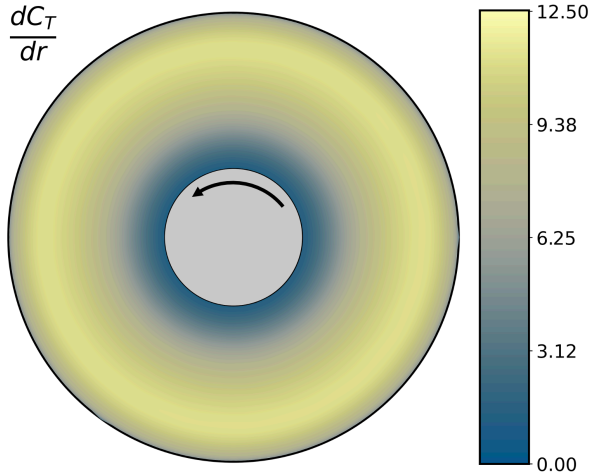


Fig. 14: Thrust of one blade over one revolution for the isolated propeller.

Figures 16 and 17 show the distribution of sectional thrust coefficient along the blade span when the blade is positioned perpendicular to the wing ($\Psi=0^\circ$) and along the azimuth angle for $r/R = 0.9$, respectively. The wing's vertical position affects distribution of thrust on the propeller disk, whereas increasing the wing's horizontal distance tends to make the distribution more uniform. Hence, only variation in vertical wing positions are included. All wing posi-

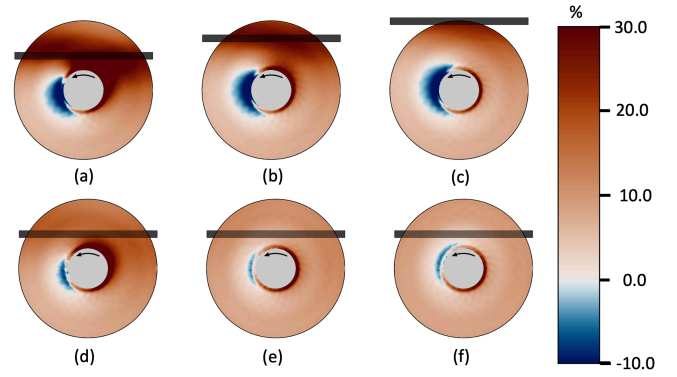


Fig. 15: Relative sectional thrust coefficient as a percent increase from the isolated propeller with the wing mounted at: (a) ($\Delta x = 0.25R, \Delta z = 0.5R$), (b) ($\Delta x = 0.25R, \Delta z = 0.75R$), (c) ($\Delta x = 0.25R, \Delta z = 1.0R$), (d) ($\Delta x = 0.5R, \Delta z = 0.5R$), (e) ($\Delta x = 1.0R, \Delta z = 0.5R$), (f) ($\Delta x = 1.5R, \Delta z = 0.5R$)

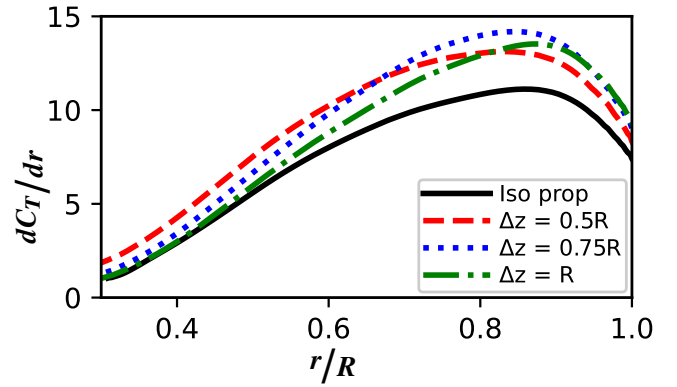


Fig. 16: The radial distribution of thrust when the blade is at $\Psi = 0^\circ$ for varying wing heights..

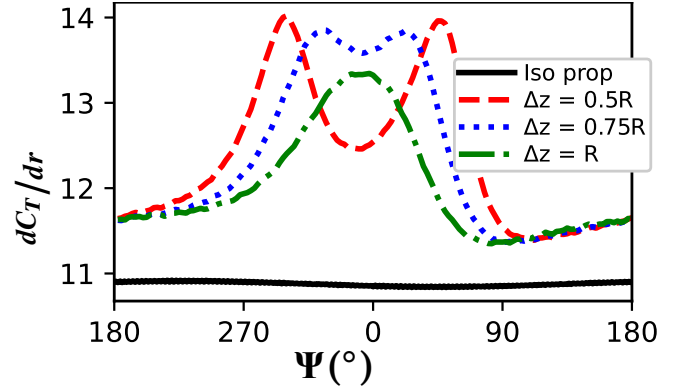


Fig. 17: Sectional thrust variation over one revolution of one blade at 90% radius for varying wing heights.

tions, ($\Delta x=0.25R, \Delta z=0.5R$), ($\Delta x=0.25R, \Delta z=0.75R$), and ($\Delta x=0.25R, \Delta z=1R$), increase the sectional thrust compared to the isolated propeller. However, the ($\Delta x=0.25R, \Delta z=0.5R$) wing position contributes more to the inboard section and the gained thrust diminishes near the tip where the downwash from the suction side of the wing begins to create an adverse effect. Meanwhile, the wing position at ($\Delta x=0.25R$,

$\Delta z=0.75R$) contributes more of the increased thrust near the tip where the wing leading-edge creates an increased angle of attack on the blade sections. Although the wing position ($\Delta x=0.25R$, $\Delta z=1R$) does not differ in thrust from the isolated propeller until 50% blade radius, the gained thrust is more consistent and maintains the largest thrust increase near the tip.

Figure 17 shows the sectional thrust coefficient near the tip ($r/R = 0.9$) throughout one revolution. The sectional thrust coefficient increases with proximity to the leading-edge of the wing. However, at $\Psi=0^\circ$ the bound vortex and downwash from the wing increases the perpendicular velocity on the propeller near the tip, which in turn decreases the angle of attack and thrust. As the wing moves upward, the region of the downwash moves beyond the propeller disk and diminishes the effect. While the wing located at $\Delta z = 1R$ avoids the thrust deficit at $\Psi=0^\circ$, the thrust increase is less than the other wing positions over a majority of the azimuth angles. For all wing positions, the peak thrust magnitude is within 4% of each other, but the twice per revolution component of thrust variation becomes weaker as the wing moves upward from the center of the propeller. The increase in propeller thrust is also skewed toward the advancing side of the propeller between $\Psi=180^\circ$ and $\Psi=360^\circ$. The peak thrust as the blade approaches the wing leading edge is slightly greater than the peak after the blade passes the wing. After the blade passes the wing, the lowest thrust is reached. This is due to the wing thickness effect. As the flow diverts from the wing's leading edge the blade tangential velocity of a blade approaching the wing increases while the tangential velocity of a blade moving away from leading edge decreases.

Wing Aerodynamics Figure 18 shows C_p comparisons for the prediction and experiment at various wing positions. The value is averaged over a period equal to one revolution of the propeller. The predictions demonstrate strong agreement with the measured data across all cases. Figure 19 shows the RMS of the C_p fluctuations ($C'_{p,RMS}$). This quantity is useful for analyzing the unsteady loading noise source on the wing, induced by the propeller and wing interaction. It is evident that the leading edge has higher amplitudes of $C'_{p,RMS}$, indicating the primary wing loading noise source location. Overall, a close agreement is observed between the prediction and experiment. However, the deviation is more distinct near the leading edge. This could be attributed to the inaccurate capture of the propeller tip vortex and wakes. However, it is confirmed that our predicted results are very similar to the predicted results in reference (Ref. 4).

Aeroacoustics

The aeroacoustics of both the isolated propeller and the propeller with the wing are estimated using CFD and RCAS simulations and validated against experimental data. Thickness and loading noise are assessed using an impermeable FW-H surface from the CFD. Although RCAS simulations only include the loading noise, it is shown from the CFD results

that the loading noise is dominant and within 3% of the total SPL for the first BPF of the isolated propeller. As the acoustic frequency increases, the experimental data becomes predominantly influenced by motor noise (Ref. 4). Consequently, the analysis shows tonal noise up to the fourth BPF, and most of the comparisons with experimental data are conducted at the first BPF. Moreover, the impact of the wing's position is evaluated as it shifts along the propeller axis or above the wing.

Isolated Propeller Acoustics The SPL of the isolated propeller, predicted using CFD and RCAS, is compared with experimental data in Fig. 20. The observer location in the linear microphone array is chosen to be nearly overhead, which has an elevation angle of 93° . The simulations capture the first two BPFs. A summary of the comparison between the predictions and experimental results is provided in Table 5. The first BPF is accurately predicted by both medium-fidelity and high-fidelity methods, with CFD yielding marginally better results. For the second BPF, CFD provides better agreement with the experimental data than RCAS.

Table 5: SPL at the first and second BPFs.

Source	First BPF	Second BPF
Experimental SPL (dB)	69.9	53.3
CFD SPL (dB)	69.7	50.3
RCAS SPL (dB)	69.1	47.7
CFD Error (%)	0.3	5.6
RCAS Error (%)	1.1	10.5

The components of the isolated propeller's acoustic pressure time history and acoustic spectrum for the CFD simulations are depicted in Fig. 21. Both thickness and loading noise exhibit the same period but with a 45° phase shift. Loading noise contributes more significantly to the total acoustic pressure, as its peak acoustic pressure is 2.2 times greater than that of the thickness noise. In the frequency spectrum, loading noise dominates the first BPF. It is 5.46 dB higher than the thickness noise, resulting in a total sound pressure level (SPL) of 70.5 dB. The discrepancy lessens at the second BPF, where the loading noise is only 2.22 dB greater, yielding a total SPL of 50.0 dB.

The directivity of the isolated propeller acoustics is assessed at a linear array of observer locations, as shown in Fig. 9, and the results are depicted in Fig. 22. The directivity is validated for the first three BPFs. The CFD and RCAS predictions are compared with wind tunnel measurements of both the motor-driven propeller and the motor alone. CFD and RCAS predictions demonstrates good agreement with the wind tunnel measurements for the first BPF as shown in Fig.22(a). CFD provides a marginally better results than RCAS. The second BPF in Fig.22(b) also exhibits satisfactory correspondence with the experimental values for moderate elevation angles ($\phi = [60, 105]^\circ$). However, the third BPF in Fig. 22(c) is consistently under-predicted across all elevation angles. Nonetheless, the total noise measured is predominantly influenced by the propeller's motor for the second and third BPFs. Since the

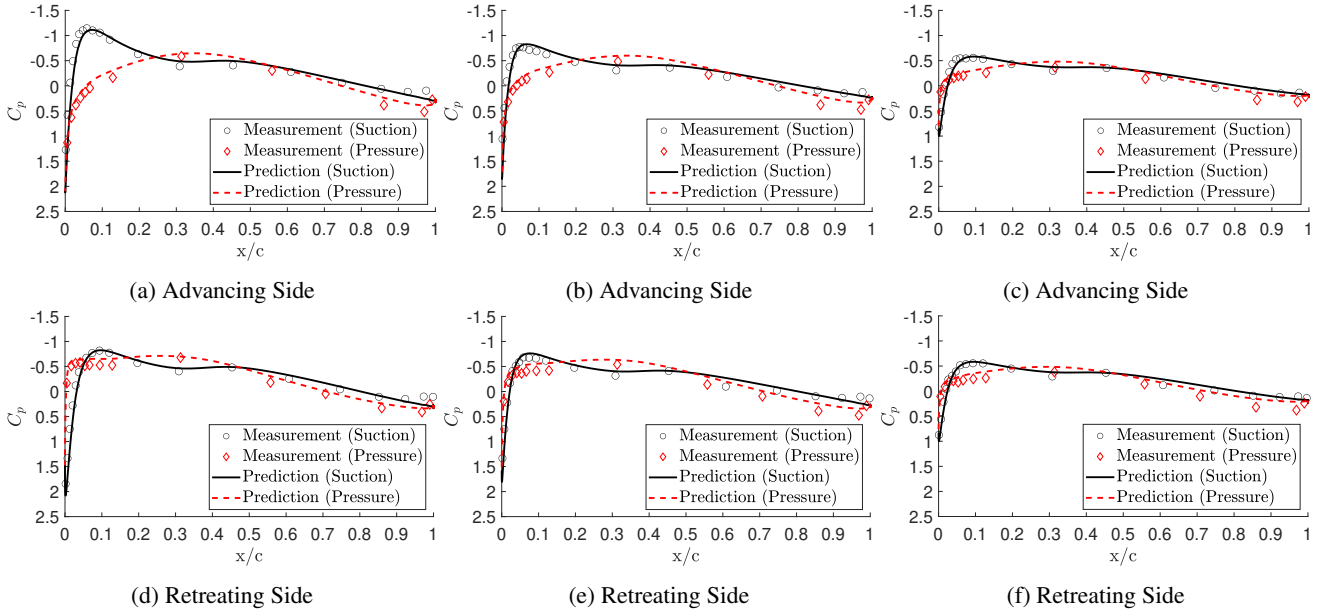


Fig. 18: C_p on the wing with the wing leading positioned at (a), (d): ($\Delta x = 0.25R$, $\Delta z = 0.5R$), (b), (e): ($\Delta x = 0.25R$, $\Delta z = 0.75R$), (c), (f): ($\Delta x = 0.25R$, $\Delta z = 1.0R$). The advancing and retreating sides refer to the locations spaced 4 in from the propeller centerline.

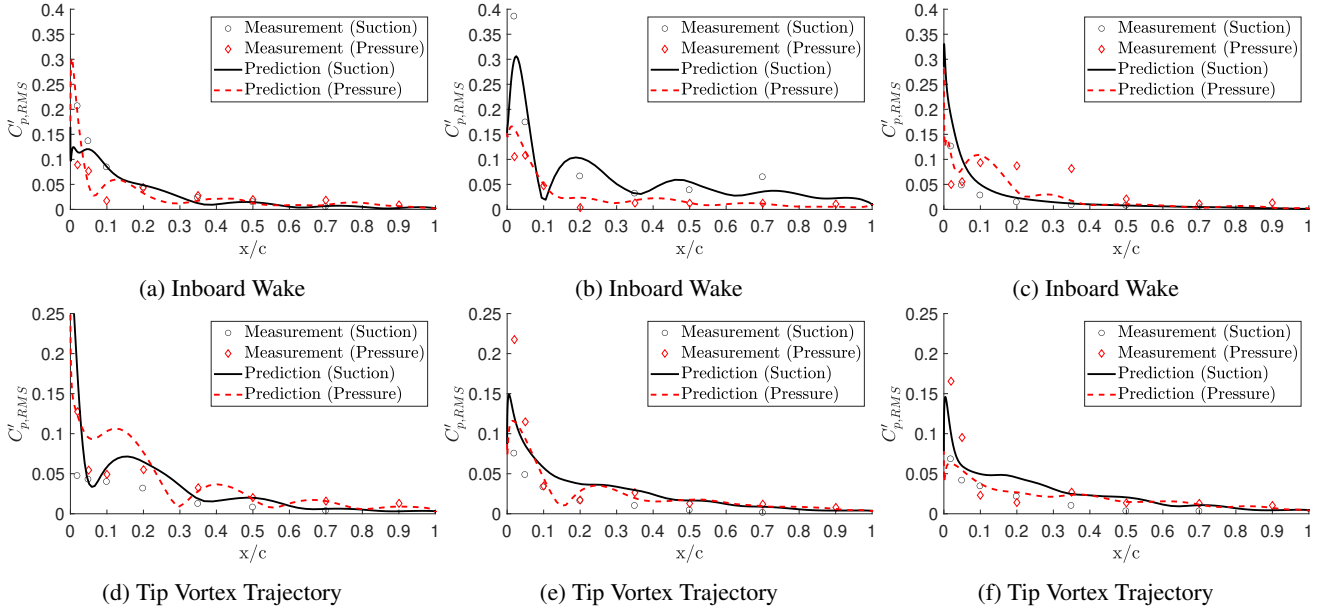


Fig. 19: $C'_{p,RMS}$ on the wing with the wing leading positioned at (a), (d): ($\Delta x = 0.25R$, $\Delta z = 0.5R$), (b), (e): ($\Delta x = 0.25R$, $\Delta z = 0.75R$), (c), (f): ($\Delta x = 0.25R$, $\Delta z = 1.00R$), (e): ($\Delta x = 0.5R$, $\Delta z = 0.5R$), (f): ($\Delta x = 1.0R$, $\Delta z = 0.5R$).

motor masks the propeller noise at higher harmonics, only the first BPF will be used for the acoustic directivity validation for the propeller with wing cases later.

Propeller with Wing Acoustics The wing's influence on acoustics, as well as its impact on the propeller's acoustics, are examined. The trends in acoustic pressure, SPL, and directivity are assessed in relation to changes in wing position. A detailed analysis of the wing's effect on propeller acoustics is conducted for a single observer and a single wing position

($\Delta x = 0.25R$, $\Delta z = 0.5R$), which is the closest to the propeller. The observer location is positioned at a 93° elevation angle.

Figure 23 illustrates the noise generated by the propeller surface for both the isolated propeller and the propeller with the wing present. Flow interactions between the propeller and wing do not affect the thickness noise, so the loading and total noise are shown. Overall, SPLs at the first and second BPFs increase by 1.1 dB and 0.34 dB when the wing is mounted.

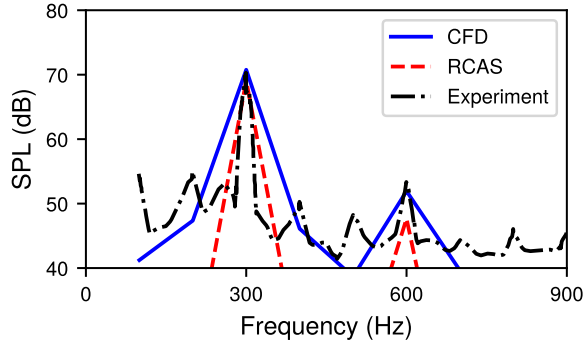
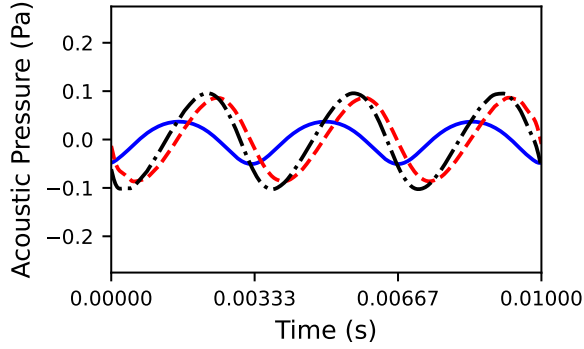
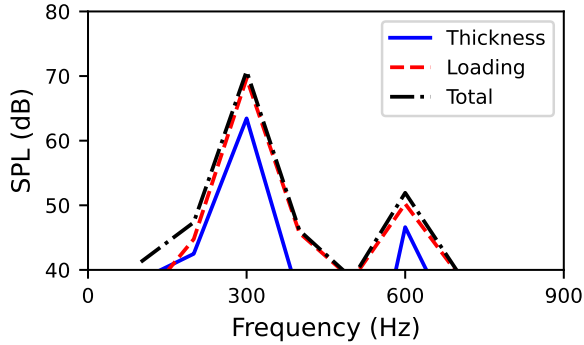


Fig. 20: Comparison of SPL from CFD and RCAS simulations for the isolated propeller with experimental data from reference (Ref. 4) at a microphone positioned at 93° .



(a)



(b)

Fig. 21: Predicted noise components based on CFD for the isolated propeller at a microphone positioned at 93° : (a) acoustic pressure time history and (b) SPL.

The acoustic contributions for the propeller-wing system are shown in Fig. 24. As the wing remains stationary in a constant, low-speed freestream flow, its thickness acoustic pressure becomes zero. The wing and propeller loading noise contribute equally to the SPL of the first BPF. However, the propeller's acoustics decay more rapidly with higher harmonics compared to the wing. The wing loading noise exhibits higher harmonic acoustics. Similar to the isolated propeller, the loading noise contributes more significantly to the sound.

The relative acoustic contribution between propeller and wing for each wing position are displayed in Fig. 25. The propeller's acoustics exhibit a weak dependence on the wing po-

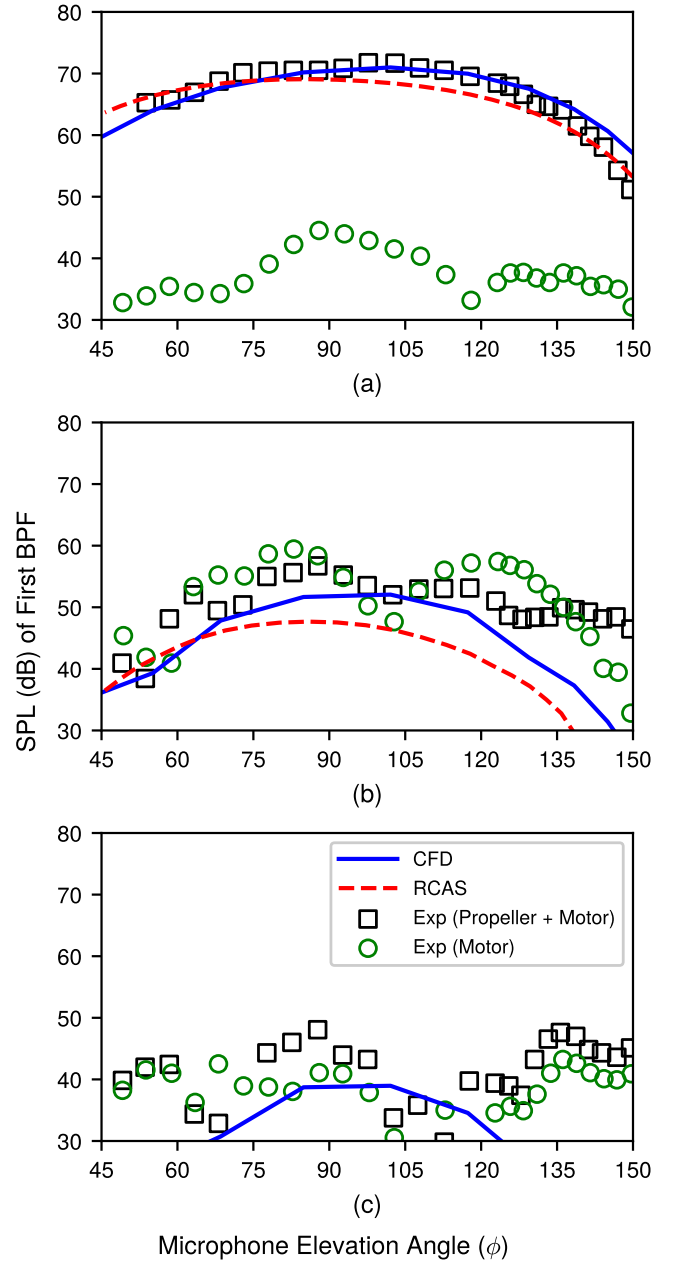


Fig. 22: Isolated propeller noise directivity comparison: (a) first BPF, (b) second BPF, and (c) third BPF. (Experimental data are from reference (Ref. 4).)

sition. As the wing moves upward, the composition of the wing's loading noise changes. When the wing is positioned half the propeller radius above the propeller axis, $\Delta z = 0.5R$, in Fig. 25(a), the SPL is 71.3 dB. As the wing continues to move upward, the SPL reaches 79 dB in Fig. 25(c). In the propeller slipstream, the flow exhibits greater energy near the tip. Additionally, when the wing is at its highest position, the propeller thrust reaches its maximum. Overall, as the wing height increases, the high thrust on the propeller disk influences on the wing surface, not just in the magnitude but also in the phase, as will be discussed in the next paragraph. When the wing is nearest to the propeller center, the second and third BPFs have SPLs of approximately 55 dB, while the

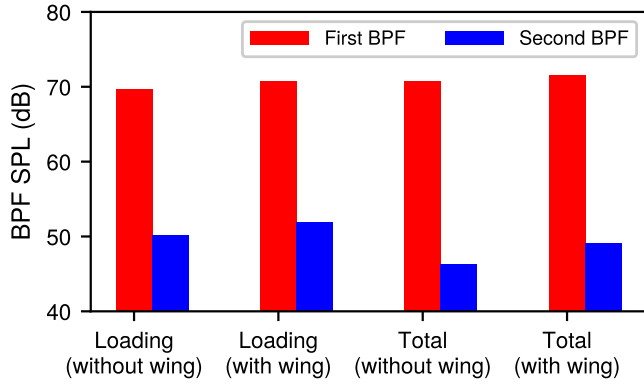


Fig. 23: Propeller noise with and without the wing mounted (when mounted at $\Delta x = 0.25R$, $\Delta z = 0.5R$).

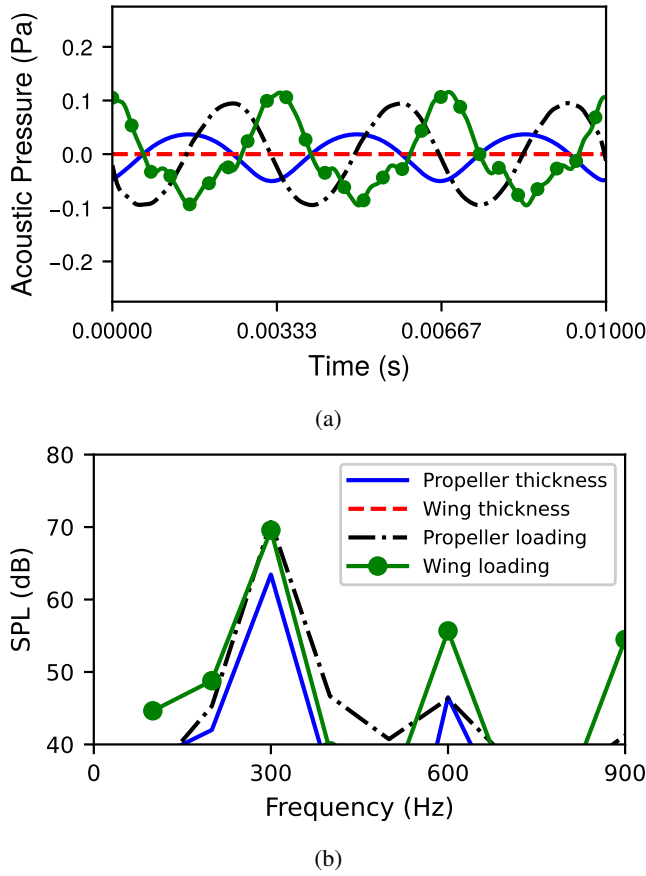


Fig. 24: Relative contribution from propeller and wing to noise for the wing position of $\Delta x = 0.25R$, $\Delta z = 0.5R$: (a) time history of acoustic pressure and (b) SPL

fourth BPF exhibits an SPL of 50 dB. For the wing located at $\Delta z = 0.75R$ and $\Delta z = R$ in Fig. 25(b)-(c), the second BPF remains above 50 dB, with the higher harmonics diminishing rapidly as the third BPF falls below 50 dB and the fourth BPF drops below 40 dB. As the wing position moves further away from the propeller in Fig. 25(d)-(f), the wing noise diminishes more noticeably at higher harmonics.

Figure 25 also reveals that the effect of the vertical wing position on SPL is more pronounced than the axial location. This

is presented in greater detail in the acoustic pressure time histories in Fig. 26. As the wing height increases, the propeller acoustic pressure remains almost the same while the wing acoustic pressure becomes higher than that of the propeller noise. However, these changes in the magnitude do not entirely explain the total pressure magnitude. The phase between the propeller and wing noise must also be considered. When the wing is closest to the propeller axis, the phase difference is most pronounced. In Fig. 26(a), the total noise peak-to-peak value has a similar magnitude to the propeller and wing components. In contrast, the propeller and wing noise are nearly in phase for the other two positions. The amplitude of the total acoustic pressure is significantly increased for wing heights 0.75R and 1.0R in Fig. 26(b)-(c). The increased wing noise peak-to-peak values and constructive interference between propeller and wing noise lead to an 8 dB increase in the first BPF SPL, as shown in Fig. 25(b)-(c).

The directivity is evaluated at a linear array of observer locations for all wing positions and presented in Fig. 27. The CFD and RCAS predictions are compared against the wind tunnel measurements for the first BPF. The directivity depends on wing position, but the trends from the experimental data, CFD, and RCAS simulations do not align. The null region above the propeller, observed in the experiment when the wing is 0.75R above the propeller axis in Fig. 27(b) does not occur in either simulation. The CFD simulations show the best agreement with the experiment at high elevation angles when the wing's axial position is 0.25R behind the propeller. However, as the wing moves away from the propeller, the directivity calculations deviate further from the experiment across all elevation angles. The CFD simulation predicts a large null region at low elevation angles as the wing moves aft of the propeller, but this is not observed in the experiment nor predicted in the RCAS simulations. Acoustic scattering by the wing can also significantly alter the directivity especially at higher elevation angles, which was not included in the simulations.

It is crucial to recognize that the first BPF sound is sensitive to phase shifts, as destructive interference can occur when the propeller and wing acoustic pressure are out of phase, as discussed earlier in the context of Fig. 26. The over-prediction of thrust in the simulations, as shown in Fig. 13, can result in faster convection of the propeller wake and an artificial phase lead for the wing's acoustic pressure. The incorrect phase angle between the propeller and wing acoustics pressure could generate inaccurate destructive and constructive interference at certain elevation angles, leading to inaccuracies in the directivity. It would be interesting to reexamine the directivity plots after trimming the propeller thrust.

CONCLUSIONS

In this paper, the aerodynamics and aeroacoustics of the Mejzlik propeller, both with and without a wing, were investigated using high-fidelity CFD and medium-fidelity VVPM. For the isolated propeller, the thrust was accurately predicted, but the torque was over-predicted by about 10%. As the wing moves

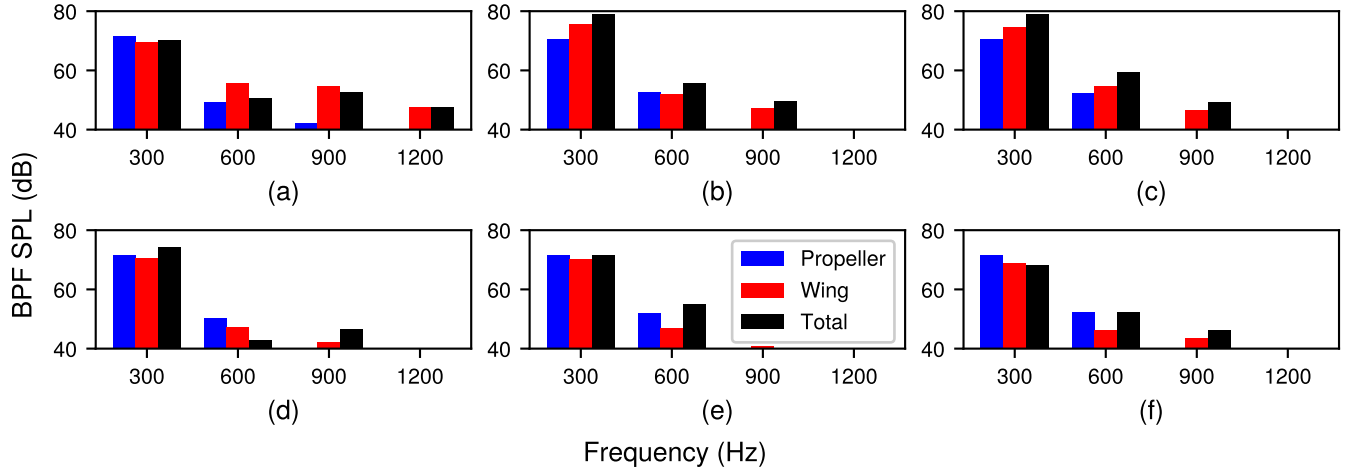


Fig. 25: Sound pressure level contribution from the propeller and wing: (a) ($\Delta x = 0.25R$, $\Delta z = 0.5R$), (b) ($\Delta x = 0.25R$, $\Delta z = 0.75R$), (c) ($\Delta x = 0.25R$, $\Delta z = 1.0R$), (d) ($\Delta x = 0.5R$, $\Delta z = 0.5R$), (e) ($\Delta x = 1.0R$, $\Delta z = 0.5R$), (f) ($\Delta x = 1.5R$, $\Delta z = 0.5R$).

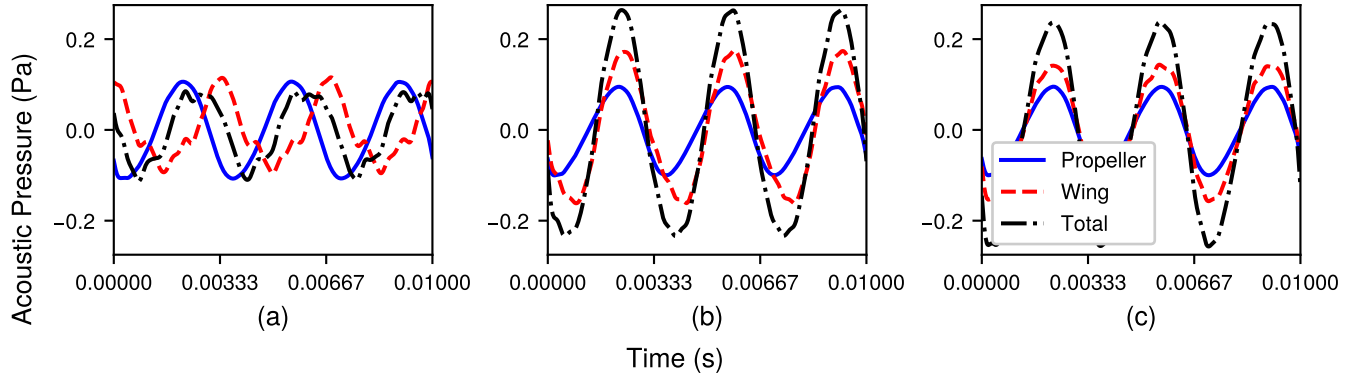


Fig. 26: Sound pressure level contribution from the propeller and wing: (a) ($\Delta x = 0.25R$, $\Delta z = 0.5R$), (b) ($\Delta x = 0.25R$, $\Delta z = 0.75R$), (c) ($\Delta x = 0.25R$, $\Delta z = 1.0R$).

closer to the propeller hub vertically and to the propeller blades horizontally, the propeller thrust increased. However, the prediction overestimated the thrust increase. The sectional thrust distribution on the propeller blades was investigated as a function of the blade radius and azimuthal angle for various wing positions. The predicted wing pressure coefficient matched well with the experimental data on both the advancing and retreating sides for different wing positions. The RMS of the pressure coefficient exhibited a significant increase near the leading edge, indicating unsteady loading on the wing due to propeller-wing flow interactions. However, some discrepancy between the prediction and the experimental data was observed at the leading edge.

The noise of the isolated propeller was well validated between predictions and experimental data at the first BPF. The CFD prediction demonstrated a slight improvement over the RCAS prediction at the first and second BPFs. It was observed that the loading noise was higher than the thickness noise, and the wing noise exceeded the propeller noise, particularly at the closest wing position. The propeller loading noise exhibited little dependence on the wing position. Conversely, the

wing was highly sensitive to its position. As the wing height increased, the first BPF noise rose while maintaining higher harmonic acoustics. When the wing was moved away from the propeller, the SPL diminished across the entire frequency spectrum.

Capturing the directivity of acoustics was found to be challenging. Wind tunnel data revealed a minimum in SPL at higher elevation angles, ranging from 90° to 135° , depending on the wing position. On the other hand, CFD simulations showed a minimum SPL at lower elevation angles, between 45° and 90° , for most wing positions. Possible factors contributing to these discrepancies include simulation errors related to the over-prediction of the thrust coefficient, which may affect the phase relationship between the propeller and wing acoustics leading to constructive and destructive interference. Incorrect capture of the phase between the propeller and wing, potentially due to a higher propeller thrust causing faster convection of the propeller wash, can result in inaccurate phase differences between the propeller and wing acoustics. In the future, the directivity will be reexamined with the thrust matched simulations. Acoustic scattering by the wing

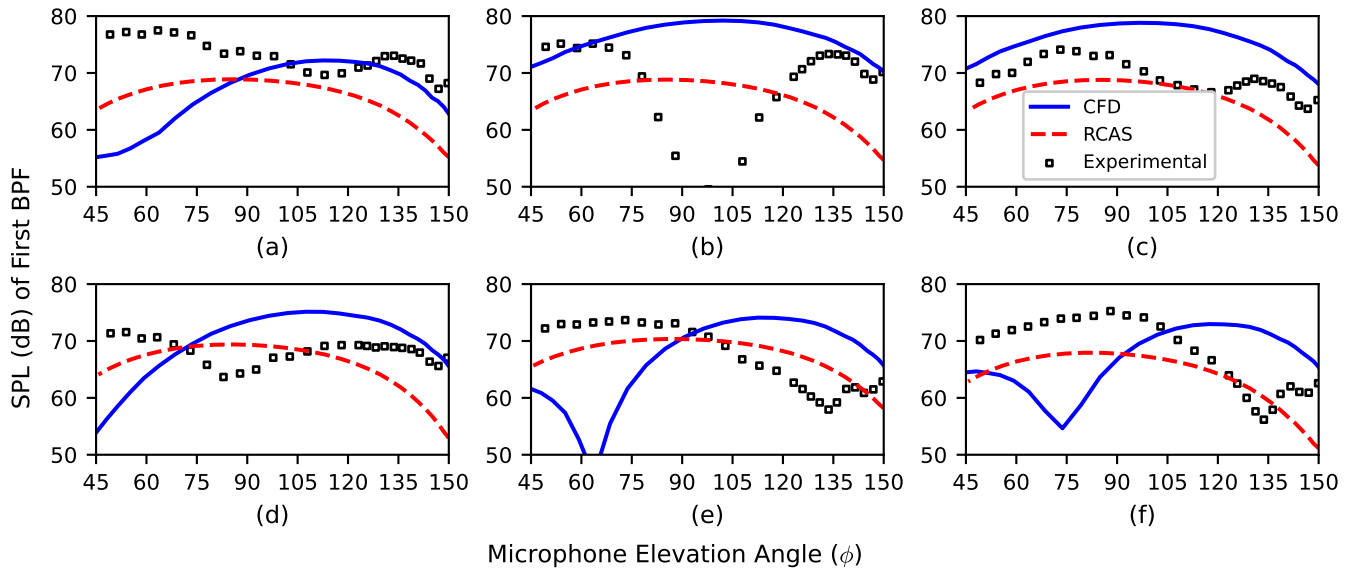


Fig. 27: First BPF SPL directivity: (a) ($\Delta x = 0.25R$, $\Delta z = 0.5R$), (b) ($\Delta x = 0.25R$, $\Delta z = 0.75R$), (c) ($\Delta x = 0.25R$, $\Delta z = 1.0R$), (d) ($\Delta x = 0.5R$, $\Delta z = 0.5R$), (e) ($\Delta x = 1.0R$, $\Delta z = 0.5R$), (f) ($\Delta x = 1.5R$, $\Delta z = 0.5R$).

can also significantly alter the directivity especially at higher elevation angles, which was not included in the simulations.

ACKNOWLEDGMENTS

This research is funded by the Government under Cooperative Agreement No. W911W6-21-2-0002. The U.S. Government is authorized to reproduce and distribute reprints for Government purposes notwithstanding any copyright notation thereon. The views and conclusions contained in this document are those of the authors and should not be interpreted as representing the official policies or position, either expressed or implied, of the U.S. Army Combat Capabilities Development Command (DEVCOM), Aviation & Missile Center (AvMC), or the U.S. Government. Resources supporting this work were provided by the NASA High-End Computing (HEC) Program through the NASA Advanced Supercomputing (NAS) Division at Ames Research Center.

REFERENCES

- ¹Lim, J. W., “Fundamental Investigation of Proprotor and Wing Interactions in Tiltrotor Aircraft,” Proceedings of the 75th Annual Forum of the Vertical Flight Society, Philadelphia, Pennsylvania, May 13–16, 2019.
- ²Zhang, J., Brentner, K. S., and Smith, E. C., “Prediction of the Aerodynamic and Acoustic Impact of Propeller-Wing Interference,” Proceedings of the Vertical Flight Society Technical Conference on Aeromechanics Design for Transformative Vertical Flight, San Jose, California, May 21–23, 2020.
- ³Quackenbush, T. R., Wachspress, D. A., Yu, M. K., and Solomon, C. L., “Modeling of Proprotor/Wing/Flap Interaction for Advanced Vertical Lift Aircraft,” Proceedings of the 76th Annual Forum of the Vertical Flight Society, Virtual, October 5–8, 2020.
- ⁴Zawodny, N. S., Boyd, D. D., and Nark, D. M., “Aerodynamic and Acoustic Interactions Associated with Inboard Propeller-Wing Configurations,” Proceedings of the AIAA Scitech Forum, Virtual, January 19–21, 2021, AIAA Paper 2021-0714.
- ⁵Saberi, H., Hasbun, M., Hong, J., Yeo, H., and Ormiston, R., “Overview of RCAS Capabilities, Validations, and Rotorcraft Applications,” Proceedings of the 71th Annual Forum of the American Helicopter Society, Virginia Beach, Virginia, May 5–7, 2015.
- ⁶Saberi, H., Hasbun, M., Hong, J., Yeo, H., and Ormiston, R., “Overview of RCAS and Application to Advanced Rotorcraft Problems,” Proceedings of the American Helicopter Society 4th Decennial Specialists’ Conference on Aeromechanics, San Francisco, California, January 21–23, 2004.
- ⁷Draper, J. W. and Kuhn, R. E., “Investigation of the Aerodynamic Characteristics of a Model Wing-Propeller Combination and of the Wing and Propeller Separately at Angles of Attack up to 90°,” NACA TN 3304, 1954.
- ⁸Stephenson, J. H., Schatzman, N. L., Cheung, B. K., Zawodny, N. S., Sargent, D. C., and Sim, B. W.-C., “Aeroacoustic Measurements from the Aerodynamic and Acoustic Rotorprop Test (AART) in the National Full-Scale Aerodynamics Complex (NFAC) 40- by 80-Foot Wind Tunnel,” Proceedings of the 77th Annual Vertical Flight Society Forum and Technology Display, Virtual, May 10–14, 2021.
- ⁹Li, S. and Lee, S., “Prediction of Rotorcraft Broadband Trailing-Edge Noise and Parameter Sensitivity Study,” *Journal of the American Helicopter Society*, Vol. 65, 2020, pp. 042006.

- ¹⁰Li, S. and Lee, S., "Prediction of Urban Air Mobility Multirotor VTOL Broadband Noise Using UCD-QuietFly," *Journal of the American Helicopter Society*, Vol. 66, 2021, pp. 032004.
- ¹¹Li, S. and Lee, S., "Acoustic Analysis and Sound Quality Assessment of a Quiet Helicopter for Air Taxi Operations," *Journal of the American Helicopter Society*, Vol. 67, 2022, pp. 1–15.
- ¹²Won, J., Trembois, N., and Lee, S., "Towards Accurate Broadband Noise Predictions Using CFD," Proceedings of the 79th Annual Vertical Flight Society Forum and Technology Display, West Palm Beach, Florida, May 16–18, 2023.
- ¹³Chan, W., "Developments in Strategies and Software Tools for Overset Structured Grid Generation and Connectivity," Proceedings of the 20th AIAA Fluid Dynamics Conference, Honolulu, Hawaii, June 27–30, 2011, AIAA Paper 2011-3051.
- ¹⁴Chan, W., Gomez, R., Rogers, S., and Buning, P., "Best Practices in Overset Grid Generation," Proceedings of the 32th AIAA Fluid Dynamics Conference, St. Louis, Missouri, June 24–26, 2002, AIAA Paper 2002-3191.
- ¹⁵Trembois, N. P., *Automatic Mesh Generation of Structured Overset Rotor Blades*, Master's thesis, University of California, Davis, Davis, CA, December 2020.
- ¹⁶Nichols, R. and Buning, P., *User's Manual for OVERFLOW 2.2*, NASA Langley Research Center, Hampton, Virginia, August 2010.
- ¹⁷Nichols, R., Tramel, R., and Buning, P., "Solver and Turbulence Model Upgrades to OVERFLOW 2 for Unsteady and High-Speed Flow Applications," Proceedings of the 25th AIAA Applied Aerodynamics Conference, San Francisco, California, June 5–8, 2006, AIAA Paper 2006-2824.
- ¹⁸Jia, Z. and Lee, S., "Impulsive Loading Noise of a Lift-Offset Coaxial Rotor in High-Speed Forward Flight," *AIAA Journal*, Vol. 58, (2), 2020, pp. 687–701.
- ¹⁹Jia, Z., Lee, S., Sharma, K., and Brentner, K. S., "Aeroacoustic Analysis of a Lift-Offset Coaxial Rotor Using High-Fidelity CFD/CSD Loose Coupling Simulation," *Journal of the American Helicopter Society*, Vol. 65, (012011), 2020, pp. 1–15(15).
- ²⁰Jia, Z. and Lee, S., "Aerodynamically Induced Noise of a Lift-Offset Coaxial Rotor with Pitch Attitude in High-Speed Forward Flight," *Journal of Sound and Vibration*, Vol. 491, 2021, pp. 115737.
- ²¹Jia, Z. and Lee, S., "Computational Study on Noise of Urban Air Mobility Quadrotor Aircraft," *Journal of the American Helicopter Society*, Vol. 67, 2022, pp. 012009.
- ²²Jia, Z. and Lee, S., "High-Fidelity Computational Analysis on the Noise of a Side-by-Side Hybrid VTOL Aircraft," *Journal of the American Helicopter Society*, Vol. 67, 2022, pp. 022005.
- ²³Brès, G. A., Brentner, K. S., Perez, G., and Jones, H. E., "Maneuvering rotorcraft noise prediction," *Journal of Sound and Vibration*, Vol. 275, (3–5), August 2004, pp. 719–738. doi: <https://doi.org/10.1016/j.jsv.2003.07.005>
- ²⁴Brès, G. A., *Modeling the noise of arbitrary maneuvering rotorcraft: Analysis and implementation of the PSU-WOPWOP noise prediction code*, Master's thesis, Department of Aerospace Engineering, The Pennsylvania State University, August 2002.
- ²⁵Perez, G., *Investigation of the influence of maneuver on rotorcraft noise*, Master's thesis, Department of Aerospace Engineering, The Pennsylvania State University, 2002.
- ²⁶Lee, S., Brentner, K., Farassat, F., and Morris, P., "Analytic formulation and numerical implementation of an acoustic pressure gradient prediction," *Journal of Sound and Vibration*, Vol. 319, (3-5), 2009, pp. 1200–1221.
- ²⁷Hennes, C., Lopes, L., Shirey, J., Cheng, R., Erwin, J., Lee, S., Goldman, B., Botre, M., Zachos, D., Gan, Z., Rau, R., Singh, N., Greenwood, E., and Brentner, K., *PSU-WOPWOP 3.5.0 User's Guide*, July 21, 2021.
- ²⁸Farassat, F. and Succi, G., "The Prediction of Helicopter Discrete Frequency Noise," *Vertica*, June 1983, pp. 309–320.
- ²⁹Brentner, K. S. and Farassat, F., "Modeling aerodynamically generated sound of helicopter rotors," *Progress in Aerospace Sciences*, Vol. 39, (2-3), 2003, pp. 83–120.