A PRELIMINARY INVESTIGATION OF PROPELLER-WING INTERACTION NOISE FOR eVTOL AIRCRAFT

A Thesis in
Aerospace Engineering
by
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Submitted in Partial Fulfillment of the Requirements for the Degree of

Master of Science

December 2020
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Abstract

Acoustics of interactional aerodynamic phenomena are expected to play an important role in design and evaluation of eVTOL configurations. The current work is focused on noise from propellers and propeller-wing interactions with the propeller in a tractor configuration. Since usage of high-fidelity aerodynamic methods such as Computational Fluid Dynamics (CFD) remain impractical for rapid preliminary design calculations, this thesis evaluates faster approaches by using classical propeller Blade Element Theory (BET) and the well validated Comprehensive Hierarchical Aeromechanics Rotorcraft Model (CHARM) developed by Continuum Dynamics, Inc. This thesis focuses on investigating complex aerodynamic interaction mechanisms and focusing on how they independently contribute to the vehicle noise.

Noise from an isolated propeller operating in a series of constant shaft angles of attack compared the capabilities of BET-based methods in capturing accurate integrated noise levels to CHARM. Important phase differences in the temporal characteristics of noise results between BET and CHARM were found, and the term involving the time derivative of loading vector from Farasaat’s Formulation 1A was revealed to be the cause. For a propeller operating in axisymmetric flow, the rotor angular velocity was found to be a potentially dominant contributor in the acoustic source term containing the time derivative of loading.

Propeller-wing aerodynamics were studied using CHARM and a comparison was made with aerodynamic data available from wind tunnel experiments. The wing was modeled using three options offered by CHARM, giving insight into their capabilities to account for propwash effects on wing loading. In the absence of analogous acoustic measurements, PSU-WOPWOP was used to computationally predict the acoustics, and the wing was found to be a significant contributor. Current predictions seem to indicate that wings can dominate noise levels when operating in the wake of a low tip-Mach number propeller. Flight test noise measurement data for the acoustically quiet YO-3A airplane is also studied using BET and CHARM. The low tip-speed propeller noise levels were underpredicted by both the tools, revealing the need for further investigation. Definitive experimental data is needed to further validate and improve the low and mid-fidelity aerodynamic tools explored in this thesis.
List of Figures

1.1 Uber’s analysis of UAM integration with existing services in cities of San Francisco, San Jose (US) and Gurgaon (now Gurugram), New Delhi (India) [1] ................................................................. 2
1.2 A typical UAM mission (as envisioned by Uber) [7] ................. 3
1.3 Existing eVTOL prototypes and concepts .................................. 4
1.4 eVTOL aircraft categories ....................................................... 5
1.5 Overview of eVTOL Aircraft noise components, community acceptance factors and mitigation strategies. ......................... 6

2.1 Schematic of complex aeromechanical environment of a helicopter [13] ................................................................. 19
2.2 Schematic of typical noise directivity for rotor noise sources from a conventional helicopter [13] ................................. 21
2.3 CFD simulation of a tiltrotor concept designed and evaluated by Dassault 3DS [30]. ......................................................... 22
2.4 (continued) CFD simulation of a tiltrotor concept designed and evaluated by Dassault 3DS [30]. ......................................... 23
2.5 Simple quasi-steady model ...................................................... 28
2.6 Limitations of the simple quasi-steady model for large $\alpha_P$ or edgewise rotor ................................................................. 28
2.7 Schematic of velocity components for quasi-steady BET. ............ 29
2.8 Schematic blade section definitions for quasi-steady BET. .......... 29
2.9 CVC model representation for rotor in forward flight [42] ........ 33
2.10 Lifting surface representation of wing with 60 spanwise and 1 chordwise element .......................................................... 34
2.11 Lifting surface representation of wing with 30 spanwise and 4 chordwise elements .......................................................... 35
2.12 Lifting panel representation of wing with 20 spanwise and 50 chordwise positions .......................................................... 36
3.1 YO-3A isolated propeller schematic and setup.  
3.2 Comparison of loading noise (OASPL) for propeller operating at angle of attack using XROTOR QS, Quasisteady BEM (QBET), and CHARM.  
3.3 Comparison of acoustic pressure time history for propeller operating at angle of attack \( \alpha_{\text{AOA}} = -7^\circ, 7^\circ, -5^\circ, 5^\circ \) using XROTOR, QBET and CHARM.  
3.4 Comparison of acoustic pressure time history for propeller operating at angle of attack \( \alpha_{\text{AOA}} = -2^\circ, 2^\circ, 0^\circ \) using XROTOR, QBET and CHARM.  
3.5 Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack \( \alpha_{\text{AOA}} = -7^\circ \).  
3.6 Comparison of wake structure as observed in CHARM for propeller operating at different angles of attack \( \alpha_{\text{AOA}} = -7^\circ, 0^\circ, 7^\circ \).  
3.7 Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack \( \alpha_{\text{AOA}} = -5^\circ \).  
3.8 Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack \( \alpha_{\text{AOA}} = -2^\circ \).  
3.9 Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack \( \alpha_{\text{AOA}} = 0^\circ \).  
3.10 Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack \( \alpha_{\text{AOA}} = 2^\circ \).  
3.11 Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack \( \alpha_{\text{AOA}} = 5^\circ \).  
3.12 Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack \( \alpha_{\text{AOA}} = 7^\circ \).  
3.13 Propeller-wing interactions visualized via Q-criterion iso-vorticity surfaces for NASA concept Elytron 4S UAV [31].  
3.14 Dunsby et al. [55] and Currie and Dunsby [56] propeller-wing experimental setup.  
3.15 CHARM’s CVC wake representation for Dunsby propeller-wing system.
<table>
<thead>
<tr>
<th>Section</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>3.15</td>
<td>(continued) CHARM’s CVC wake representation for Dunsby propeller-wing system. 58</td>
</tr>
<tr>
<td>3.16</td>
<td>Variation of $C_{L_{avg}}$ for different methods. 59</td>
</tr>
<tr>
<td>3.17</td>
<td>Variation of steady spanwise distribution of $C_L$ (viewed from the rear) with respect to changing geometric angles of attack ($\alpha_g$) [56]. 61</td>
</tr>
<tr>
<td>3.18</td>
<td>Spanwise distribution of $C_L$ for different methods. 67</td>
</tr>
<tr>
<td>3.19</td>
<td>Spanwise distribution of $C_L$ for different angles of attack (AOA). 68</td>
</tr>
<tr>
<td>3.20</td>
<td>Schematic of the hypothetical observer/microphone placed in PSU-WOPWOP to compute the noise for the Dunsby propeller-wing system. 69</td>
</tr>
<tr>
<td>3.21</td>
<td>Variation of $C_{L_{avg}}$ for different methods. 72</td>
</tr>
<tr>
<td>3.22</td>
<td>Acoustic pressure time history from propeller (Pa) for various angles of attack and different aerodynamic methods (LSM, VLM, LPM). 73</td>
</tr>
<tr>
<td>3.23</td>
<td>Acoustic pressure time history from wing (Pa) for various angles of attack and different aerodynamic methods (LSM, VLM, LPM). 74</td>
</tr>
<tr>
<td>3.24</td>
<td>Frequency domain spectra from wing for various angles of attack and different aerodynamic methods (LSM, VLM, LPM). 75</td>
</tr>
<tr>
<td>3.25</td>
<td>Acoustic pressure time history from wing (Pa): 1st Blade Passage Frequency (BPF = 200 Hz) for various angles of attack and different aerodynamic methods (LSM, VLM, LPM). 76</td>
</tr>
<tr>
<td>3.26</td>
<td>YO-3A surveillance airplane with the 3-bladed propeller. 77</td>
</tr>
<tr>
<td>3.27</td>
<td>YO-3A noise reduction features [51]. 77</td>
</tr>
<tr>
<td>3.28</td>
<td>YO-3A flight test schematic [51]. 79</td>
</tr>
<tr>
<td>3.29</td>
<td>Trends in propeller and aircraft noise as reported by Griffith and Revell [51]. 80</td>
</tr>
<tr>
<td>3.30</td>
<td>CHARM’s CVC wake representation for isolated YO-3A propeller. 81</td>
</tr>
<tr>
<td>3.31</td>
<td>YO-3A isolated propeller schematic and setup. 81</td>
</tr>
<tr>
<td>3.32</td>
<td>Comparison of fundamental rotational SPL (dB) using XROTOR, BET (QBET), and CHARM with Griffith and Revell [51] predictions (axisymmetric case). 82</td>
</tr>
<tr>
<td>3.33</td>
<td>Comparison of fundamental rotational SPL (dB) using XROTOR Quasi-steady, Quasi-steady BET (QBET), and CHARM with propeller operating at 2.88° angle of attack. 83</td>
</tr>
<tr>
<td>3.34</td>
<td>YO-3A propeller and wing. 84</td>
</tr>
<tr>
<td>3.35</td>
<td>Comparison of fundamental rotational SPL (dB) using QBET and CHARM with with Griffith and Revell [51] predictions and experimental data. 85</td>
</tr>
<tr>
<td>3.36</td>
<td>Comparison of fundamental rotational SPL (dB) using CHARM for propeller and wing loading with with Griffith and Revell’s [51] prediction and experiment. 86</td>
</tr>
</tbody>
</table>
3.37 Acoustic pressure time histories from propeller and wing at 480 and 780 RPMs. 

List of Tables

3.1 Number of spanwise and chordwise control points used to model the Dunsby wing using LSM, VLM, and LPM in CHARM. . . . . . . . 57

4.1 Summary of validation with experimental data . . . . . . . . . . 91
4.2 Summary of OASPL (dB) values generated by the wing . . . . . . 92
List of Symbols

Nomenclature

$t$ observer time
$c$ speed of sound in undisturbed medium

$T_{ij}$ Lighthill stress tensor

$u_i$ component of local fluid velocity in direction $x_i$ ($i = 1, 2, 3$)

$P_{ij}$ compressive stress tensor

$g = r - t + r/c$

$r$ distance between observer and source, $r = |\vec{x} - \vec{y}|$

$\vec{x}$ observer position vector

$\vec{y}$ source position vector

$f = 0$ implicit describing the surface of the noise source, e.g., a rotating blade, wing

$\Box^2$ wave operator, $\Box^2 = \frac{1}{c^2} \frac{\partial^2}{\partial t^2} - \nabla^2$

$H(f)$ Heaviside function, $H(f) = 0$ for $f < 0$ and $H(f) = 1$ for $f > 0$

$\hat{n}$ unit outward normal vector to surface, with components $\hat{n}_i$

$u_i$ components of local fluid velocity

$v_n$ local normal velocity of source surface

$v_{n_i} v_i \hat{n}_i$
\( \dot{v}_n, \dot{v}_i \hat{n}_i \)

\( \vec{M} \) local Mach number vector of source with respect to a frame fixed to the undisturbed medium, with components \( M_i \)

\( M_r \) mach number of source in radiation direction, \( M_i \hat{r}_i \)

\( \dot{M}_r \) \( \dot{M}_i \hat{r}_i \)

\( dS \) element of the rotor blade surface

\( l_i \) components of local force intensity that acts on the fluid, \( l_i = P_{ij}n_j \)

\( l_r \) \( l_i \hat{r}_i \)

\( \dot{l}_r \) \( \dot{l}_i \hat{r}_i \)

\( V_{tip} \) velocity of propeller tip

\( R \) propeller radius

\( V_\infty \) freestream operational velocity

\( V_{axial} \) velocity component on blade section in a direction aligned to the propeller axis

\( V_{tangential} \) velocity component on blade section in a direction aligned tangential to the motion of the blade

\( a_{axial} \) axial induction factor

\( a_{tangential} \) tangential induction factor

**Greek letters**

\( \delta_{ij} \) Kronecker delta, \( \delta_{ij} = 1 \) for \( i = j \), otherwise \( \delta_{ij} = 0 \)

\( \nabla \) the gradient operator

\( \tau \) source time

\( \Omega \) rotor angular velocity

\( \alpha_P \) shaft angle of attack
\( \theta, \psi \) propeller blade azimuthal position

\( \epsilon_{\text{induced}} \) induced aerodynamic angle of attack

\( \beta \) local blade twist angle

\( \alpha_{\text{AOA}} \) blade section angle of attack, \( \alpha_{\text{AOA}} = \beta - \epsilon_{\text{induced}} \)

\( \rho_0 \) density of air in quiescent medium

**Subscripts**

- \( \text{ret} \) quantity is evaluated at the retarded time, \( \tau = t - r/c \)
- \( 0 \) denotes fluid variable in quiescent medium
- \( T \) thickness noise component
- \( L \) loading noise component

**Abbreviations**

- UAM Urban Air Mobility
- eVTOL Electric Vertical Take-Off and Landing
- CHARM Comprehensive Hierarchical Aeromechanics Rotorcraft Model
- BET Blade Element Theory
- QBET Quasi-steady implementation of the BET
- LSM Lifting Surface Theory
- VLM Vortex Lattice Method
- LPM Lifting Panel Method
- OASPL Overall sound pressure level
- SPL Sound pressure level
Acknowledgments

I would first like to thank my advisor Dr. Kenneth S. Brentner for giving me the opportunity to work on this and helping me every step of the way. I admire his patience with me while I find another rabbit hole to spend time in and occasionally produce magnificently pointless results. I would also like to thank Dr. Eric Greenwood for reviewing this thesis, helpful discussions regarding the YO-3A aircraft and the intricacies of acoustic flight testing based on his extensive experience.

I would like to thank Terrafugia for their amazing support and exposing me to the fascinating world of flying cars and eVTOL. My experience while working with the team has been one of the high-points in grad school. Special note of thanks to Carl, Venkat, Bryan, Josh for their leadership and support. Patrick and Brian, you guys made my time a lot more informative with our fun aerodynamic discussions over lunch.

To all my amazing friends in State College, who have made the town tolerable (and even likeable on the rare occasions), this thesis wouldn’t be possible without every one of you. Special shout-out to the people from the Arts and Culture outing group.

At last I would like to thank my labmates Mrunali, Jean-Pierre, Jeff, Rajah, Thomas, Ryan, Kalki, Ted, Demi and others for bearing with me. Without them, grad school would be futile, and I would have failed over half of my courses.
Dedication

The thesis is dedicated to my dear mother, father and grandparents, who have supported me throughout my life. Their support has never been more important, especially during the shitshow that 2020 is.

For God’s sake, let us sit upon the ground
And tell sad stories of vortex filaments
How some have been rolled, some slain viscously,
Some haunted by the mathematics they have involved.

All murd’rous.

For within the swirling motion that screws round the air
Keeps trailing the edge
And there the non-linearity sits
Scuffing its state and grinning at its pomp,
Allowing a wake, a little scene,
To linearize, be approximated,
Cometh at last, with a dram ’rr’r
With solemn chaos ,,,
   throw away the costly hope

Farewell ...

Shakespeare, Richard II, Act 3, Scene 2
‘Unashamedly’ “inspired” from Henry Che-Chuen Yuen
Chapter 1  Introduction

Urban Air Mobility (UAM) is an emerging area of transportation [1] intended to serve and facilitate transportation of goods and people over a diverse set of environments and capacities. It’s intended to be a multi-modal point to point method of transporting people and goods within a city utilizing aircraft as the central leg.

The bedrock for UAM lies in the concept of on-demand aviation. This is an extension to the current market of on-demand transportation valued more than $300B [2]. In the next 10 years, this market is estimated to have profits exceeding $700B. However, on-demand transportation faces significant challenges including increasing environmental impact and congestion of existing urban areas. With millions of man-hours wasted on high-density urban commutes worldwide, aerial mobility offers a previously unexplored dimension of fast transportation. Figure 1.1 demonstrates the potential for drastic reduction in travel times by integrating UAM with current On-demand transportation services provided by Uber.

Despite the huge potential, UAM is expected to only have a near-term available market value of $2.5B; a mere 0.5% of its potential market cap [3]. The significantly low market cap can be attributed to a variety of technological and non-technological challenges (such as noise) discussed at greater detail in Section 1.2. The Booz Allen Report [3] however concludes that UAM can reach its potential market cap of $500B by addressing the challenges through strategic partnerships between government agencies at different levels and strong industry commitment.
1.1 eVTOL Aircraft

Several aircraft design solutions capable of performing a typical UAM mission exist. Over 50 companies of varying credibility have announced aircraft intended to serve the UAM market [4]. Uber Technologies Inc, a popular ride-sharing company with 110 million monthly active users worldwide [5], has announced plans to enter the UAM market with the announcement of Uber Elevate [6]. An accompanying white paper [1] revealed Uber’s goal to serve the market as a steward-of-ecosystem by simultaneously developing aircraft and connecting other companies to its huge consumer base via the Elevate network. The paper also describes potential market barriers, Uber’s vision on aircraft designs along with quantitative targets to help overcome these barriers. A basic set of missions and requirements for eVTOL aircraft was also defined by Uber [7]. One of the most common and efficient design solutions to a typical UAM mission was determined to be the Electric Vertical Take-Off and Landing (eVTOL) aircraft. These missions and requirements are intended to act as a basic litmus test for evaluating different eVTOL aircraft designs.
A cursory look at the typical UAM mission (fig. 1.2) reveals the need for the following mission-critical capabilities from any candidate eVTOL aircraft:

1. **VTOL**: Aircraft must have strong Vertical Takeoff and Landing capability including short duration hover.

2. **Noise**: The mission is extremely *noise sensitive* due to a largely dense urban population exposed to the aircraft’s noise.

3. **Carbon footprint**: Considering the high density of missions, it’s essential that the aircraft be powered using energy sources with low CO₂ and other greenhouse gas emissions. However, the long-term vision for the aircraft to be completely electric throughout operation and eliminate large parts of operational greenhouse emissions.

4. **Autonomy**: A major push from Uber and others is towards enabling autonomous operation of eVTOL aircraft. Autonomy is expected to expedite revenue generation, enabling vehicles to perform multiple missions throughout the day, with low turnover times for maximized efficiency. Combined with high safety requirements, autonomy is expected to be a goal for eVTOL vehicles to become a viable commercial reality.

eVTOL aircraft generally involve the use of multiple rotors and propellers to generate lift and propulsive forces. This leads to several possibilities in the configuration design of an eVTOL aircraft. Figure 1.3 shows some of the most prominent prototypes and concepts.
The diverse range of passenger eVTOL vehicles can be classified into four broad categories:

1. **Vectored Thrust**: An eVTOL aircraft that uses any of its thrusters (propellers, rotors, jet, etc.) for lift and cruise.

2. **Lift + Cruise**: An eVTOL aircraft that uses independent thrusters used for cruise and for lift without significant thrust vectoring.

3. **Wingless**: Thrusters are used for lift, and a typical wingless eVTOL aircraft doesn’t deploy stationary lifting surfaces.

4. **Electric Rotocraft**: An eVTOL aircraft that uses the traditional helicopter architecture (typically single main rotor).

While the above mentioned categories of eVTOL vehicles have their own strengths, their ultimate commercial viability is still an open-ended question and different companies have invested significant resources in exploring them (fig. 1.4). Despite the diverse architecture of eVTOL vehicles, all face a similar set of challenges that must be tackled in order to be successfully integrated in an UAM service. The next section goes through the most critical and relevant challenges.
1.2 Challenges Faced by eVTOL Aircraft

As eVTOL aircraft evolve into a viable design solution for UAM, they face several technological and non-technological challenges. Case studies across different cities undertaken by Vascik and Hansman [8, 9] have identified several of such challenges. Three of the most critical challenges include:

1. Aircraft Noise and Community Acceptance

2. Availability of Takeoff and Landing Areas (Vertiports)

3. Scalability of Operations (Traffic control, long-term noise exposure)

It is expected that noise and community acceptance will play a key role in the short and long-term viability of eVTOL aircraft in an UAM network. Incorrect evaluation of immediate and long-term effects of eVTOL aircraft noise could lead to disastrous setbacks in scaling of UAM operations and potential industry-wide revenue losses.

1.3 Noise: Mechanisms and Mitigation

Considering the importance of noise and community acceptance to the successful scaling of UAM operations (section 1.2), it is important to understand the nature of this particular constraint. Unlike most other constraints, the mechanism responsible for the noise impact is a highly correlated function of the objective aircraft noise
characteristics and \textit{subjective} community reaction (\textbf{acceptance}). Figure 1.5 gives a broad overview on these components of eVTOL noise. A proper understanding of these components will help designers and regulators in formulating mitigation strategies.

![Diagram of eVTOL Aircraft noise components, community acceptance factors and mitigation strategies.](image)

\textbf{Figure 1.5: Overview of eVTOL Aircraft noise components, community acceptance factors and mitigation strategies.}

eVTOL aircraft are expected to be complex with several stationary and moving components. From the perspective of aerodynamically generated \textbf{sources} of noise, the following categories are established:

1. \textbf{Rotational noise}: This includes the noise from rotating components. Rotational noise is typically the dominant source of noise in traditional VTOL aircraft [10]. With the presence of multiple propellers (axial flight) and rotors (edgewise flight)\footnote{In the context of this thesis, propellers always represent blades rotating in a plane normal to the flight direction, while rotors always represent blades rotating in a plane parallel to the flight direction}, eVTOL aircraft are also expected to have high levels of rotational noise contribution to the total aircraft noise.
2. **Non-rotating component noise**: eVTOL aircraft can also have several non-rotating components such as wings, jets, fuselage, etc. Jets could be a major source of noise, but it is beyond the scope of consideration in this thesis.

3. **Aerodynamic interaction noise**: Due to the nature of closely spaced rotating and non-rotating components in eVTOL configurations, complex aerodynamic interactions are expected to be an important contributor to overall noise. The most prominent of these interactions include Propeller-Propeller, Propeller-Rotor, Rotor-Rotor and *Propeller-Wing interaction* noise. The contribution from these interactions to total noise is highly dependent on aircraft configuration and maneuver.

Computation of eVTOL noise by inclusion of some or all the aircraft noise components described would ideally lead to an objective, configuration-dependent acoustic data representing the noise from the aircraft. This information would then be used by a community acceptance model. The model, would include re-purposing the representative aircraft acoustic data based on the following factors:

1. **Regional preference**: This is a subjective factor which depends highly on local individuals, past community experience with aircraft noise and a variety of other socioeconomic factors. Currently, active research and surveys are being undertaken across prospective urban markets in order to better understand regional preferences [3]. Traditional short and long-term noise annoyance metrics like SEL (Sound Exposure Level) and DNL (Day-Night Levels) [1, 11] could be re-purposed to help aircraft designers get a better understanding on the potential community reaction to aircraft noise.

2. **Local environment**: The ambient noise and terrain are also expected to play a key role in noise perception and propagation. Hence the local environment is expected to be an important factor in community acceptance to eVTOL aircraft operations [1, 3, 8, 9].

Based on the knowledge of aircraft noise characteristics (sound level, spectral composition, etc.) and community annoyance metrics (SEL, DNL, etc.) and mitigation strategies for aircraft noise could be broadly classified into the following categories:
1. **Acoustically aware design:**

   This is the one of the most effective ways to reduce noise. An acoustically aware design process would rely on source noise reduction and noise abatement strategies. For example modern commercial fixed-wing aircraft have the benefit of source noise reduction (chevron nozzles) and noise-control features such as engine duct liners. But aircraft involving rotating wings typically operate in complex aeromechanical environments, and hence rely heavily on source noise reduction as opposed to noise absorption approaches. Hence, an in-depth physical understanding of the mechanisms of noise generation and resultant characteristics, especially the noise from rotational and aerodynamic interaction noise.

   Little research is currently available on experimentally validated direct noise mitigation design strategies for eVTOL aircraft. However decades of historic research devoted to understanding and mitigating noise from general aviation propellers [12] and helicopter rotors [13] can be leveraged as a starting point for understanding eVTOL aircraft noise.

2. **Operating restrictions:** This is a potential result of poor community acceptance to eVTOL operation noise. Restrictions may include curfews, no-fly zones, noise quotas (basically limiting number of flights over a certain period of time), etc.

3. **Noise abatement procedures:** Noise abatement procedures could play a crucial role in mitigating operational noise. A noise abatement procedure involves utilizing the knowledge of the acoustic characteristics of the aircraft to identify maneuvers that trigger high noise mechanisms and minimize them, or redirecting noise to areas less noise sensitive.

   Historically noise abatement research have proved valuable in enabling helicopters to fly more quietly in high population areas [14, 15]. In order for similar research to be undertaken for eVTOL vehicles, a through understanding of aircraft noise sources and capability for accurate computational noise modeling needs to be gained. Development of such procedures are usually only practical if the aircraft design allows for it.
Thus reducing the noise during the design stage itself is a cornerstone to mitigate eVTOL aircraft noise. Source noise for any aircraft potentially includes a diverse variety of factors, fig. 1.5 covers the components postulated to be particularly significant for eVTOL aircraft.

1.4 Thesis Objective

As discussed above, rotational and aerodynamic interaction noise are expected to be important sources of eVTOL aircraft noise. Mitigation of these require acoustically aware design strategies and a thorough understanding of the physical mechanisms. The objective of this thesis is to conduct a fundamental investigation into propeller-wing interaction noise in the context of eVTOL aircraft. Propeller-wing interactions are expected to be important for several eVTOL configurations, especially the ones including a wing as a primary lifting surface (section 1.1: Vectored Thrust, Lift + Cruise, etc.). It is also expected that propellers in eVTOL aircraft are going to be operating in lower tip-Mach numbers compared to traditional general aviation and turboprop aircraft. Hence, there will be a unique focus on propeller and propeller-wing aeroacoustics, where the propeller helical tip-Mach number will generally be in the range of 0.2 to 0.3.

High-fidelity aerodynamic methods, like computational fluid dynamics (CFD), remain an impractical option as a design and evaluation tool because of the high computational cost. Computation of flow induced noise becomes more time consuming with CFD as this requires calculation of time-varying flowfield and unsteady schemes often require more steps to achieve convergence. Coupled with the vast design space of eVTOL vehicles, a strong need for development and validation of faster physics-based aerodynamics methods exists among the eVTOL design community. These aerodynamic methods need to sufficiently resolve the complex three-dimensional, coupled and unsteady processes that directly affect the source noise characteristics, while keeping the computational costs low.

Accurately resolving the unsteady component of the aerodynamics remains the key challenge for low and mid-fidelity aerodynamic methods. Relevant experimental data will be used to validate the accuracy of predicted aerodynamic loading. Using this, the acoustic data will be computed and when available be compared to experimental measurements.
The goal is to test the capabilities of these tools to capture key aerodynamic and aeroacoustic phenomenon while laying groundwork for future research in improving these tools.

1.5 Contributions

Summarizing main contributions from current work:

1. Analysis of integrated and temporal characteristics of noise from a propeller operating in axial and tilted flow using XROTOR, Blade Element Theory and CHARM.

2. Evaluation of free-wake aerodynamic method in capturing propeller-wing aerodynamics with respect to experimental time-averaged wing $C_L$ and the distribution of wing section $C_L$ across the span of the wing steady loading integrated over sections of wing under propwash

3. Investigation of noise results reported in the comprehensive YO-3A aircraft report using XROTOR, BET, and CHARM.
Chapter 2
Theory

As discussed in the previous chapter (section 1.3), vehicle noise reduction was postulated to be the cornerstone of eVTOL noise mitigation strategy. With the presence of multiple rotating components and complex coupled interactions, the noise mitigation strategy has to be developed based on a thorough identification of dominant acoustic and aerodynamic processes. This chapter describes the general aeroacoustic theory and the underlying acoustic source terms. Specific focus is kept on the acoustic source terms and the underlying aerodynamics of low tip-speed propellers and propeller-wing interactions operating in typical cruise flight conditions.

2.1 Aeroacoustic Theory

Aeroacoustics is the study of noise generated by the flow of fluids. It is a scientific subdiscipline which lies at the boundary between aerodynamics and acoustics. The field was largely initiated in a two-part paper titled “On sound generated aerodynamically” by Sir M. J. Lighthill [16, 17]. The results of this paper allowed for the calculation of sound intensities only when generated by a fluid in free-space (no solid boundaries). In 1955, Curle [18] extended Lighthill’s results to include solid immovable boundaries by making use of the Kirchhoff’s description of the homogeneous wave field in terms of surface boundary conditions. J. E. Ffowcs Williams and D. L. Hawkings extended Kirchhoff’s description of the wave field with stationary surfaces to generalized surfaces in arbitrary motion [19]. This allowed the expansion of aeroacoustic theory to rotating surfaces: with helicopters, general-aviation propellers and turboprops being the historical applications of the
FFowcs Williams-Hawkins (FW-H) equations.

2.1.1 Lighthill-Curle Theory of Aerodynamic Sound

An analogy approach was undertaken by Lighthill [16, 17], where the continuity and momentum equations were rearranged to the form of an analogous wave equation.

\[
\frac{\partial^2 \rho}{\partial t^2} - c^2 \nabla^2 \rho = \frac{\partial^2 T_{ij}}{\partial x_i \partial x_j} \tag{2.1}
\]

where \( T_{ij} \) is known as the Lighthill stress tensor, defined in eq. (2.2).

\[
T_{ij} = \rho u_i u_j + (P_{ij} - c^2 \rho \delta_{ij}) \tag{2.2}
\]

Here \( \rho \) = density, \( P_{ij} \) = compressive stress tensor, \( c \) = velocity of sound in fluid at rest, \( u_i \) = component of velocity in direction \( x_i \) (\( i = 1, 2, 3 \)). Equation (2.1) shows the result obtained by Lighthill’s acoustic analogy. Physically this means that sound is generated by a fluid flow exactly as an uniform medium at rest is acted upon by externally applied fluctuating stresses. According to Lighthill [16] the sound field produced is equivalent to the one produced by a static distribution of acoustic quadrupoles whose instantaneous strength per unit volume can be given by the Lighthill stress tensor (eq. (2.2)).

Using the free-space Green’s function for the wave equation, Lighthill obtained the solution of this equation, which allows the prediction of noise generated from a flow field without the presence of surfaces.

\[
\rho(\vec{x}, t) = \iiint_V \int_\tau \frac{\partial^2 T_{ij}}{\partial x_i \partial x_j} G(\vec{x}, t; \vec{y}, \tau) d\tau d\vec{y} \tag{2.3}
\]

where \( \vec{x} \) is the observer position, and \( \vec{y} \) is the source position, \( \tau \) is the source time and \( t \) is the observer time. \( G(\vec{x}, t; \vec{y}, \tau) \) represents the free-space Green’s function for the wave equation:

\[
G(\vec{x}, t; \vec{y}, \tau) = \frac{\delta(g)}{4\pi r} \tag{2.4}
\]

where \( g = \tau - t + r/c \) and \( r = |\vec{x} - \vec{y}| \). In 1955, Curle [18] was able to extend Lighthill’s acoustic analogy to account for rigid objects in the flow field. Curle stated that solid boundaries make their presence felt in two ways:
1. The sound generated via acoustic quadrupoles predicted by Lighthill’s theory eqs. (2.1) to (2.3) will be reflected and diffracted by the solid boundaries.

2. The quadrupoles will no longer be distributed over the entire free-space, but only up to the region external to the solid boundaries. The distribution of sources at the boundaries was found to correspond to be a resultant distribution of dipoles.

Hence the Lighthill-Curle theory of aerodynamic sound describes the sound generated due to flow of fluid in presence of solid immovable bodies as a result of quadrupoles and dipoles distributed in the flow-field and surface respectively.

### 2.1.2 Ffowcs Williams and Hawkings Equation

In 1969 Ffowcs Williams and Hawkings published “Sound Generation by Turbulence and Surfaces in Arbitrary Motion” [19], in which they generalized Lighthill’s acoustic analogy to include moving surfaces and a third type of noise source was found to be monopole distributions. The Ffowcs Williams and Hawkings (FW-H) equation manages to maintain the advantage in finding the solution via the use of the free-space Green’s function for the wave equation (eq. (2.4)). This is achieved by embedding the exterior flow/acoustics problem into unbounded space by using generalized functions to describe the flowfield.

Considering a moving surface, $f(\vec{x}, t) = 0$ where $\nabla f = \hat{n}$ and $\hat{n}$ is the surface unit vector, a new set of generalized flow variables (indicated by the tilde) are defined throughout the entire space, with the implicit function $f = 0$ defining the acoustic data surface (i.e., surface in arbitrary motion), where $f > 0$ is outside the body in the fluid domain and $f < 0$ is inside the body. The new functions that are valid throughout all space are given as:
Here $\tilde{\rho}$, $\tilde{\rho}_u$ and $\tilde{P}_{ij}$ are the density, momentum and the compressive stress tensor respectively. The Heaviside function $H(f)$ can be used to succinctly describe the terms in eq. (2.5).

$$\tilde{\rho} = \rho_0 + (\rho - \rho_0)H(f)$$

$$\tilde{\rho}_u = \rho u_i H(f)$$

$$\tilde{\rho}_u u_j = \rho u_i u_j H(f)$$

$$\tilde{P}_{ij} = P_{ij}|0 + \Delta P_{ij} H(f)$$

where the Heaviside function is defined as:

$$H(x) = \begin{cases} 1 & x > 0; \\ 0 & x < 0; \end{cases}$$

The conservation laws of mass continuity and momentum remain valid for fluid flows when all derivatives are interpreted as generalized derivatives. The generalized partial derivative $\left( \bar{\partial} / \partial x_i \right)$ of the function $q(x_i)$ (discontinuous across the surface $f(x) = 0$) with respect to variable $x_i$ is:

$$\bar{\partial}q / \partial x_i = \partial q / \partial x_i + \Delta q \partial f / \partial x_i \delta(f)$$

\footnote{The overbar ($\bar{\partial} / \partial x_i$) represents generalized differentiation}
Here $\delta(f)$ represents the Dirac delta function. Hence, we can write the generalized continuity and momentum equations as:

\[
\begin{align*}
\frac{\partial \bar{\rho}}{\partial t} + \frac{\partial \bar{\rho} u_i}{\partial x_i} &= \frac{\partial \bar{\rho}}{\partial t} + (\rho - \rho_0) \frac{\partial f}{\partial t} \delta(f) + \frac{\partial (\rho u_i)}{\partial x_i} \delta(f) \\
&= [\rho_0 v_n + \rho(u_n - v_n)] \delta(f)
\end{align*}
\] (2.7)

\[
\begin{align*}
\frac{\partial \bar{\rho} u_i}{\partial t} + \frac{\partial \bar{\rho} u_i u_j + P_{ij}}{\partial x_j} &= \frac{\partial \bar{\rho} u_i}{\partial t} + \rho u_i \frac{\partial f}{\partial t} \delta(f) \\
&+ \frac{\partial}{\partial x_j} (\rho u_i u_j + P_{ij}) + (\rho u_i u_j + \Delta P_{ij}) \frac{\partial f}{\partial x_j} \delta(f) \\
&= [\rho u_i (u_n - v_n) + \Delta P_{ij} n_j] \delta(f)
\end{align*}
\] (2.8)

All the fluid parameters in eqs. (2.7) and (2.8) are valid only for the exterior of solid surfaces ($f > 0$). Substituting the expressions of the generalized variables (eq. (2.6)) and rearranging the terms in the same manner as Lighthill’s derivation [16] while keeping in mind the properties of generalized derivatives, we arrive at the general expression for Ffowcs Williams-Hawkings equation:

\[
\Box^2 p'(\vec{x}, t) = \frac{\partial}{\partial t} \{ [\rho_0 v_n + \rho(u_n - v_n)] \delta(f) \} - \frac{\partial}{\partial x_i} \{ [\Delta P_{ij} n_j + \rho u_i (u_n - v_n)] \delta(f) \} \\
+ \frac{\partial^2}{\partial x_i \partial x_j} \{ T_{ij} H(f) \}
\] (2.9)

where $\Box^2 = [(1/c^2)(\partial^2/\partial t^2)] - \nabla^2$ is the generalized wave operator and $p'(\vec{x}, t)$ is the acoustic pressure at the observer position $x$ at observer time $t$.

The principle advantage of the FW-H equation lies in the ability to ascribe physical meaning to the three source terms derived in this equation. We must keep in mind that the data surface ($f = 0$) in eq. (2.9) need not be solid. It could be a permeable surface located in the flow field. However, in order to continue the discussion of the source terms in the context of rotorcraft noise, it is common to assume that the surface $f = 0$ is coincident with the actual solid surface (blade
surface), then the normal velocity of the fluid is the same as the normal velocity of the surface \((u_n = v_n)\). For an impermeable surface eq. (2.9) can be rewritten:

\[
\square^2 p'(x,t) = \frac{\overline{\nabla}}{\partial t} \{[\rho_0 v_n] \delta(f)\} - \frac{\overline{\nabla}}{\partial x_i} \{[\Delta P_{ij} \hat{n}_j] \delta(f)\} + \frac{\overline{\nabla}^2}{\partial x_i \partial x_j} [T_{ij} H(f)]
\]

(2.10)

A noteworthy observation in eq. (2.10) is that the surface source terms are identifiable by the presence of the Dirac-delta function, while the Heaviside function is associated with the volume source term. Based on their respective mathematical structures the three terms in eq. (2.10) can be further described as follows:

1. The first term is a monopole source term. The monopole source, often known as the thickness term, accounts for the noise generated due to the displacement of air caused by a moving surface. This term is determined completely by the geometry and kinematics of the moving body.

2. The second term is a dipole source term. The dipole term, also known as the loading source term, models the noise resulting from the acceleration of the force acting on the fluid due to the presence of the body.

3. The third term is a quadrupole source term, and it accounts for nonuniform speed of sound, finite fluid particle velocity, and other aspects of the flow field that differ from the assumptions made in the Lighthill acoustic analogy. The quadrupole term in the FW-H equation is primarily a deterministic noise source (in contrast to jet noise where it is stochastic) and it is often associated with transonic blade speeds and the occurrence of shock waves and high-speed-impulsive noise.

For typical rotocraft applications below transonic Mach numbers, the quadrupole term holds little importance and hence will be neglected from further discussions this point\(^2\). Moving ahead, the FW-H equation for subsonic rotocraft noise applications can be written as:

\[
\square^2 p'(x,t) = \overline{\nabla} \{[\rho_0 v_n] \delta(f)\} - \frac{\overline{\nabla}}{\partial x_i} \{[\Delta P_{ij} \hat{n}_j] \delta(f)\}
\]

(2.11)

\(^2\)For rotocraft noise in the transonic flow regime, the reader is referred to [20–22].
2.1.3 PSU-WOPWOP

While the FW-H equation enables us to calculate noise from a general source surface at any observer point, its true numerical advantage is realized through integral formulations. Several integral formulations of the FW-H equation have been proposed over the years. The focus in this work will be the underlying formulation used by the PSU-WOPWOP noise code [23].

The PSU-WOPWOP noise prediction code was used for all the acoustic computations performed in this thesis. PSU-WOPWOP was developed at Penn State to model noise of maneuvering rotorcraft following arbitrary flight paths together with independent, aperiodic blade motion and loading for multiple rotors [24–26]. The retarded-time integral formulation of the FW–H equation [19], known as Farassat’s Formulation 1A [27], was chosen as the theoretical basis for the code. PSU-WOPWOP uses a “source-time-dominant” approach because it is the most efficient acoustics algorithm for computing the noise from a maneuvering aircraft [24]. Although PSU-WOPWOP was developed to predict rotorcraft maneuver noise, it is a general FW-H solver that is not restricted to predicting rotor noise.

2.1.3.1 Farassat’s Formulation 1A

In section 2.1.2 the two surface source terms from the FW-H equation (eq. (2.11)) relevant to rotocraft acoustics were introduced. Total acoustic pressure $p'(\vec{x}, t)$ at any observer position $\vec{x}$ from a source surface (defined by $f = 0$) can be described as a sum of the two components:

$$p'(\vec{x}, t) = p'_T(\vec{x}, t) + p'_L(\vec{x}, t) \tag{2.12}$$

where $p'_T(\vec{x}, t)$ represents the thickness acoustic pressure while $p'_L(\vec{x}, t)$ represents the loading acoustic pressure. Farassat’s Formulation 1A provides an integral representation of the solution to eq. (2.11) by using the free-space Green’s function, integrating over space and accounting for the sifting properties of the Dirac delta.
function, which results in the integral formulation given as:

\[ 4\pi p_T'(\vec{x}, t) = \int_{f=0} \left[ \frac{\rho_0 (\hat{v}_n + v_n)}{r|1 - M_r|^2} \right]_{ret} dS \]

\[ + \int_{f=0} \left[ \frac{\rho_0 v_n (r\dot{M}_r + c(M_r - M^2))}{r^2|1 - M_r|^3} \right]_{ret} dS \]  

(2.13)

\[ 4\pi p_L'(\vec{x}, t) = \frac{1}{c} \int_{f=0} \left[ \frac{l_r}{r|1 - M_r|^2} \right]_{ret} dS \]

\[ + \int_{f=0} \left[ \frac{l_r - l_M}{r^2|1 - M_r|^2} \right]_{ret} dS \]

\[ + \frac{1}{c} \int_{f=0} \left[ \frac{l_r(r\dot{M}_r + c(M_r - M^2))}{r^2|1 - M_r|^3} \right]_{ret} dS \]  

(2.14)

In eqs. (2.13) and (2.14), \( r \) is the magnitude of the vector from the source \( \vec{y} \) to the observer \( \vec{x} \) represented by \( r = |\vec{x} - \vec{y}| \). The surface velocity vector, outward unit normal vector, and Mach number of the surface element area \( dS \) (source surface) are represented by \( \vec{v}, \hat{n}, M \), respectively. A dot over a variable implies the source time derivative of that variable while the subscript \( n, r \) and \( M \) refer to the dot product of the vector with the unit normal vector, the unit radiation vector and the surface velocity vector normalized by the speed of sound. In eq. (2.14) an additional term, represented by \( \vec{l} \) is the local force acting on fluid due to the surface element \( dS \).

### 2.2 Rotorcraft Noise Sources

A brief overview of development of aeroacoustic theory was conducted in section 2.1. With an understanding of the theoretical sources and the underlying terms, it is important to consider the importance of these sources for different applications in rotorcraft. PSU-WOPWOP is an implementation of Farassat’s Formulation 1A, therefore, the following discussion of rotorcraft noise sources is based on the terms in eqs. (2.13) and (2.14).

A typical rotorcraft vehicle operates in a complex unsteady aeromechanical environment. A combination of these processes contribute to the kinematic terms \( (\vec{v}, \vec{r}, \hat{n}, M) \) responsible for thickness noise and the aerodynamic loading term \( (\vec{l}) \)
responsible for loading noise. Depending on the trajectory, flight conditions and design of the vehicle, thickness and loading noise have directivity characteristics, i.e. these noise components have directions in which they are dominant. Understanding the directivity characteristics and correlating them with the vehicle design and operation is the cornerstone to noise mitigation. Sections 2.2.1 and 2.2.2 briefly discuss the noise sources and typical directivities from conventional helicopters and upcoming eVTOL vehicle configurations based on historical research.

2.2.1 Helicopter Noise Sources

Helicopters are one of the most successful commercial applications in the rotorcraft category. In their most popular form, a typical helicopter has a single main rotor and tail rotor. Decades of research has helped identify the directivity and underlying mechanisms behind the noise generated by helicopter in typical flight conditions, such as forward flight, hover, etc. Helicopters have several noise sources such as engine noise, transmission and gear noise, etc. However, the focus here will be aerodynamically generated noise, in particular: rotor noise.

As shown in fig. 2.1, helicopter rotors tend to operate in a complex kinematic, dynamic and unsteady aerodynamic environment. This leads to several processes through which noise is generated (fig. 2.1) and examining each of these processes and the associated source terms in eqs. (2.13) and (2.14) provides insight into the directivity characteristics of rotor noise. Acoustically, the noise can also

![Figure 2.1: Schematic of complex aeromechanical environment of a helicopter [13]](image-url)
be categorized as discrete frequency and broadband noise components. These components are also types of thickness and loading noise in Farassat’s Formulation 1A. The following is a more detailed explanation of these sources:

1. Thickness noise: Since thickness noise source terms are kinematic and deterministic ($v, r, n, M$), thickness noise only contributes to the discrete frequency noise. In a typical subsonic rotor flight regime, thickness noise is dominant in front of the helicopter (fig. 2.2).

2. Loading noise: Loading noise is largely dependant on the nature of the aerodynamic loading ($l$). The nature of its directivity and contribution to total noise is primarily associated with the highly unsteady aerodynamics determined by the motion of the rotor. Depending on the circumstances, loading noise can be classified into discrete-frequency and broadband noise. Discrete frequency loading noise is a result of deterministic loading noise, including impulsive blade-vortex interaction. Generally the discrete frequencies of loading noise are dominant below the rotor. The impulsive blade-vortex-interaction (BVI) noise, which occurs due to the interaction of the tip vortex with the blade, is highly directional. (see fig. 2.2). The $l$ term in eq. (2.14) is a dominant contributor to loading noise, especially for BVI events.

Broadband noise is generated by random loading on the blade surface. The stochastic nature of the turbulence leads to a wide range of frequencies in the acoustic spectrum. The frequency content is higher than seen in the thickness and harmonic loading noise. Typical helicopter broadband noise lies in mid-high frequency range. For a brief history and recent progress of helicopter broadband noise the reader is refereed to the following papers [28, 29]

It should be kept in mind that the helicopter operational environment leads to several other important sources of noise (such as high-speed impulsive (HSI) noise). However, the goal of this section is to provide a preliminary description of helicopter noise sources that are expected to be relevant and useful to understand the nature of noise generated by the multi-rotor environment of eVTOL aircraft.
2.2.2 eVTOL Noise Sources

Much of the theoretical development for helicopter rotor noise is applicable to eVTOL aircraft as well. Farassat’s formulation 1A is still suited for application as long as the tip Mach number does not exceed 1 for any of the rotors. Unlike decades of R&D devoted to helicopter noise, eVTOL noise is in an extremely nascent stage of understanding within the aeroacoustic research community. Section 1.3 gave a brief introduction to eVTOL noise sources.

A major challenge faced in the identification and categorization of eVTOL noise source terms and mechanisms lies in the huge variety of configurations being proposed. Figures 1.3 and 1.4 gave a general overview on the different categories of commercial eVTOL configurations under active research and development. These configurations represent a diverging range of kinematic and aerodynamic source terms that get populated in eqs. (2.13) and (2.14) resulting in novel characteristics of the acoustic magnitude and directivity.

While rotors were the primary source of aerodynamically generated noise in helicopters, eVTOL aircraft are expected to have other components such as wings with a potential to generate significant aerodynamically generated noise. Figure 2.4 shows an eVTOL aircraft with a tiltrotor configuration designed by Dassault 3DS.
Based on the iso-surface of the $\Lambda_2$ vortex criterion (fig. 2.4a), one can observe significant aerodynamic interactions between the rotors, lifting surface (wing), fuselage and other components. While understanding the noise of eVTOL aircraft, it is important to keep in mind that the rotors are expected to have high variations in rotational speeds or unsteady angular acceleration. Helicopter rotors typically maintain constant rotational speeds for most parts of the flight. The presence of angular acceleration will introduce additional unsteadiness in terms such as $\dot{v}_n$, $v_n$, $M_r$ from eq. (2.13) and $\dot{l}_r$ in eq. (2.14).

_Thickness noise_ while present is expected to have lower significance than loading noise. The angular acceleration term could introduce sub-harmonics of the Blade Passage Frequency (BPF) for deterministic _loading noise_. Equation (2.14) for loading noise shows the importance of _unsteady aerodynamic loading_ ($\dot{l}_r$) term. Considering the highly interactive and coupled aerodynamic environment, unsteady loading is going to be extremely important. With the presence of multiple rotors interacting strongly with the rest of the airframe, _broadband noise_ will be a critical component of eVTOL noise. The high frequencies generated lie in a region of the spectrum where human hearing is sensitive. The primary sources of broadband noise are: self-noise, i.e., noise generated by the interaction of the turbulent boundary layer with trailing edge, vortex shedding due to bluntness of the trailing edge, tip vortex formation noise, laminar-boundary-layer vortex-shedding; inflow turbulent noise due to ingestion of the previously present wake with the blade, etc. With high angular acceleration, modulating broadband noise will also have an important impact on overall perception of noise signal to human hearing. However, for the context of this thesis the acoustic impact of rotors with variable rotational velocity and broadband noise will be _ignored._

![Tiltrotor concept: Side-view](image1)

_Figure 2.3: CFD simulation of a tiltrotor concept designed and evaluated by Dassault 3DS [30]._
As mentioned earlier (section 1.4), the aerodynamic environment of eVTOL aircraft in this thesis is focused to provide fundamental insight into noise generated from a low-speed propeller operating with unsteady loading and aerodynamic interaction between a propeller and a wing in tractor configuration.

1. Low-speed propeller:

   (a) Thickness noise: For low tip-Mach numbers, thickness noise is expected to be mostly negligible when compared to loading noise. This will be described further during the investigation of noise from the YO-3A aircraft propeller (section 3.3).

   (b) Loading noise: Typically the magnitude of the steady loading term ($l_r$) for rotating components like tractor/pusher propellers and lifting rotors depend on the thrust generated. The rate of change ($\dot{l}_r$) of the loading vector ($\vec{l}$) depends on the unsteady/time-varying aerodynamic blade loading. Even if the loading is steady in the blade fixed frame, $\vec{l}$ and $l_r$ are time varying in the observer frame of reference. Furthermore, if the propeller or rotor is in non-axial flow (e.g., propeller at angle of attack, or the interaction caused by passing through the wake of another component of the vehicle), then the loading is unsteady even in the blade frame of reference and the time derivative of loading can be come quite significant.

   Propellers when operating in front a wing tend to operate in a region
of upwash generated by the wing. This upwash results in the propeller operating in a shaft angle of attack with an inflow distribution resulting in the blade sectional loading to change with time, i.e., unsteady loading. Section 3.1.1.1 studies the noise from an isolated propeller operating with a shaft angle of attack, while sections 3.2.2.3 and 3.3 study the noise from the propeller operating with a shaft angle of attack in the presence of a wing upwash field. For low-speed propellers, the tonal noise generated by unsteady blade loading is significant.

2. Wing:

(a) Thickness noise: For a non-rotating component like a wing or fuselage moving in constant, rectilinear motion, \(\dot{v}_n\), \(v_n\), and \(M_r\) reduce to zero. Thus all of the far-field terms (terms with \(1/r\) dependence in the integrands) are eliminated and only near-field terms remain (the last two terms in the second integral in eq. (2.13)). Hence, the thickness noise produced by the wing or fuselage is effectively zero—especially for an observer that moves with the aircraft.

(b) Loading noise: It is important to note that non-rotating components like wings and fuselages could also have relatively significant contributions to loading noise term \(p_L'(x,t)\) through unsteady aerodynamics loads caused by interactions with propeller wakes, etc. In this thesis, examination of the loading generated on a wing operating in the wake of a tractor propeller will be considered. The swirl from the wake of a tractor propeller is expected to induce unsteady loading on the regions of the wing with which it interacts. This unsteady loading \(\dot{l}_r\) is expected to generate significant noise.

These aerodynamic interactions will be studied with low and mid-fidelity aerodynamic methods and tools as described in our upcoming section aerodynamic approach (section 2.3).
2.3 Aerodynamic Approach

The aerodynamic approach in this thesis is aimed to generate time-dependent loading which is exported to the acoustic code PSU-WOPWOP along with the necessary geometry. Computational Fluid Dynamics (CFD) based methods, while being one of the most robust ways to obtain detailed information on the interactional aerodynamics expected from an eVTOL vehicle, remain computationally expensive. Typical eVTOL CFD simulations often requiring huge computational clusters, large data storage and hundreds of CPU hours [31]. Hence CFD loses its appeal in the early design stage where major system level design decisions have to be made in a limited amount of time.

The aerodynamic approach in this thesis was devised with computational speed in mind. None of the aerodynamic calculations in this thesis have taken more than a few minutes of serial processing time. This steep reduction in computational costs compared to CFD methods does come with the price of loss in fidelity. An understanding of the principles and the inner working of the lower fidelity tools allows the comparison and exploration of their respective aeroacoustic prediction capabilities.

The aerodynamic approach begins with XROTOR [32], an open-source propeller design and aerodynamics tool. The aerodynamic loading from XROTOR can be used to predict noise from propellers operating in axial flight conditions. A simple quasi-steady model using XROTOR (XROTOR QS) developed previously at Penn State allowed the evaluation of noise from propellers under angle of attack, with an understanding that the modification was a crude approximation [33]. As an improvement over the XROTOR QS model, the quasi-steady Blade Element Theory (QBET) method was implemented, allowing the capture of fundamental unsteady aerodynamics. Both of these methods leverage the speed provided by classical propeller lifting line theories.

However, in order to capture the effects of interactional aerodynamics, the free-vortex wake model implementation in CHARM developed by Continuum Dynamics Inc was used. CHARM has previously provided good loading data for helicopter noise predictions. These predictions matched noise measurements from acoustic flight tests quite well [34]. Hence CHARM was adopted as the primary tool to study the steady and unsteady features of propeller-wing interactional aerodynamics.
(tractor configuration). Each of these tools will be described in more detail in the following subsections.

2.3.1 XROTOR

XROTOR is an open-source program developed for the design and analysis of free-tip, ducted propellers, and windmills\cite{32}. XROTOR uses a lifting line \cite{35} to represent the blade and uses classical vortex/blade-element methods of Betz \cite{36} and Galuert \cite{37}. It consists of a collection of menu-driven routines which perform various useful functions including:

- Design optimization of minimum induced loss propeller
- Interactive modification of a propeller geometry
- Aerodynamic analysis of a propeller operating in axisymmetric flow conditions

XROTOR is a well validated, robust, and highly efficient computational tool for axisymmetric aerodynamic calculations of a wide variety of propeller blade designs. The menu-driven routines also provide wide control on a host of parameters related to blade design optimization, and aerodynamics calculations. Coupled with its widespread usage in industry, XROTOR was initially chosen to obtain blade loading for noise calculations in PSU-WOPWOP. The lofting feature in CROTOR \cite{38} (an extension of XROTOR) is also used to generate blade surface geometry for thickness noise calculations.

Despite XROTOR’s capabilities with axisymmetric blade load calculations, it does not provide unsteady blade loads. For eVTOL vehicle operations, it is expected that significant aerodynamic interactions between components may exist (see fig. 1.5). These interactions often result in a propeller effectively operating at increased shaft angles of attack, complex velocity inflow fields, etc. In order to understand the impact of unsteady interactions better, a simple quasi-steady model was developed to take advantage of XROTOR’s capabilities and menu-driven routines while adding time-dependence to the blade loading. This quasi-steady model effectively tries to simulate a propeller operating with shaft angle of attack. The next subsection discusses this model in detail.
2.3.1.1 Simple Quasi-steady Model

A simple quasi-steady model was developed at Penn State to leverage XROTOR’s propeller design and aerodynamic modeling capabilities [33] in order to explore the impact of unsteady aerodynamic loading on noise. The simple flow regime of propeller operating with a shaft angle of attack ($\alpha_P$) was considered (fig. 2.5) because it results in time-varying blade loads (i.e., azimuthally varying blade loads). The quasi-steady model calculates the aerodynamic velocity at the tip ($V_{tip}$) of propeller for every discretized azimuthal position ($\psi$) as described by eq. (2.15).

$$V_{tip} = \Omega R + V_\infty \sin(\alpha_P) \sin(\psi) = \Omega_\psi R$$  \hspace{1cm} (2.15)

It is evident that non-zero values of $\alpha_P$ will result in $V_{tip}$ varying around the azimuth $\psi$. The velocity at the tip ($V_{tip}$) can also be represented by an equivalent angular velocity $\Omega_\psi$ for each azimuthal position, which is an appropriate input to XROTOR’s operation menu routine (OPER). As $\psi$ varies from 0 to $2\pi$, a sequence of angular velocities (revolutions per minute or RPMs) is generated ($\Omega_1, \Omega_2, \Omega_3, \ldots \Omega_N$).

It should be noted that XROTOR is “blind” to the presence of shaft angle of attack ($\alpha_P$); therefore, each value of RPM ($\Omega_i$) is treated with the default linear velocity distribution (from blade root to blade tip) expected from a propeller operating in asymmetric flow. The effect of the angle of attack $\alpha_P$ is that the propeller has an edgewise component of velocity $V_\infty \sin(\alpha_P)$, as shown in fig. 2.5. As an example, consider the edgewise rotor shown in fig. 2.6, where $\alpha_P = 90^\circ$ and $V_\infty \sin(\alpha_P) \equiv V_F$ is the forward flight velocity. This example shows the actual distribution of velocity across the span of the blade in fig. 2.6a, while the quasi-steady model has the approximate velocity distribution shown in fig. 2.6b. It can be observed that the simple quasi-steady model won’t capture the reverse-flow region. Section 3.1 explores the impact of this modeling error on noise calculations, with the most significant error for the case of a rotor operating in edgewise flight.

27
Figure 2.5: Simple quasi-steady model.

(a) Velocity for edgewise rotor in forward flight
(b) Simple quasi-steady approximation velocity for edgewise rotor in forward flight

Figure 2.6: Limitations of the simple quasi-steady model for large $\alpha_P$ or edgewise rotor.

### 2.3.2 Quasi-steady Blade Element Theory Code

While the simple quasi-steady model leverages the design capabilities of XROTOR, it fails to further capture the first-order quasisteady effects experienced by a propeller due to presence of shaft angle of attack, unsteady inflow in presence of other rotors, wings, turbulence, etc. Figure 2.7 shows the velocity components of a propeller operating with shaft angle of attack and unsteady inflow, while fig. 2.8 shows the *velocities* and *forces* for a blade section. The Quasi-steady Blade Element Theory (QBET) code implements the same classical vortex/blade-element methods of Betz [36] and Glauert [37] (used in XROTOR), with modifications to blade section velocity terms made in order to account for the effects of shaft angle of attack ($\alpha_P$), and unsteady inflow in presence of wings, fuselages, etc.

Equations (2.16) and (2.17) show the velocity terms on each blade section of a
Figure 2.7: Schematic of velocity components for quasi-steady BET.

Figure 2.8: Schematic blade section definitions for quasi-steady BET.

propeller in ideal axisymmetric flow. The structure of the helical wake system is assumed to not change with sources of unsteadiness, such as shaft angle of attack and upwash from wings, fuselages, etc.

\[ V_{\text{axial}} = V_\infty \left[1 + a_{\text{axial}}(r)\right] \]  
\[ V_{\text{tangential}} = \Omega r \cdot \left[1 - a_{\text{tangential}}(r)\right] \]

Equations (2.18) and (2.19) show the velocity terms on each blade section of a propeller operating in a constant shaft angle of attack \((\alpha_P)\).

\[ V_{\text{axial}} = V_\infty \left[1 + a_{\text{axial}}(r)\right] \cdot \cos(\alpha_P) \]  
\[ V_{\text{tangential}} = \Omega r \cdot \left[1 - a_{\text{tangential}}(r)\right] - \left[v_\infty \cdot \sin \alpha_P\right] \cdot \cos \theta \]
Equations (2.20) and (2.21) show the velocity terms on each blade section of a propeller for constant shaft angle of attack ($\alpha_P$), and unsteady inflow from external sources and turbulence. The external inflow is decomposed into two components: perpendicular to the disk $\mathbf{u}_{inflow}(r, \theta)$ and parallel to the disk $\mathbf{w}_{inflow}(r, \theta)$. The QBET code considers these inflow velocities as varying with span ($r$) and blade azimuth ($\theta$).

\[
V_{axial} = V_\infty \left[ 1 + a_{axial}(r) \right] \cdot \cos(\alpha_P) + u_{inflow}(r, \theta) \quad (2.20)
\]

\[
V_{tangential} = \Omega r \cdot \left[ 1 - a_{tangential}(r) \right] - [v_\infty \cdot \sin \alpha_P + w_{inflow}(r, \theta)] \cdot \cos \theta \quad (2.21)
\]

The axial induction factor ($a_{axial}$) and tangential induction factors ($a_{tangential}$) are nondimensional factors, ranging from 0 to 1 which captures the “effectiveness” of the helical vortex system trailed by a rotating propeller blade in inducing velocities at every blade section. The steps below (based on Adkins et al. [39] and Larrabee [40]) show the method in which these induction factors are calculated:

1. Set initial value for $\epsilon_{induced} = \epsilon_0$.
   $\epsilon_0$ can be a reasonably small value like 10°.

2. Calculate $\alpha_{AOA} = \alpha_0 = \beta - \epsilon_0$.
   Here $\alpha_{AOA}$ is the section angle of attack, $\beta$ is the local twist angle of the blade section.

3. Determine $F_{shaft}$, $F_{tangential}$, $f_{Prandtl}$
   $F_{shaft}$ and $F_{tangential}$ are the shaft and tangential forces acting on each blade section, and the $f_{Prandtl}$ is the Prandtl-tip loss factor for each section. $F_{shaft}$ and $F_{tangential}$ can be given by:

   \[
   F_{shaft}(r, \theta) = [q(r, \theta) \ C_L \ \Delta A] \cdot \cos(\epsilon_{induced}) + [q(r, \theta) \ C_D \ \Delta A] \cdot \sin(\epsilon_{induced})
   \]

   \[
   F_{tangential}(r, \theta) = [q(r, \theta) \ C_L \ \Delta A] \cdot \sin(\epsilon_{induced}) + [q(r, \theta) \ C_D \ \Delta A] \cdot \cos(\epsilon_{induced})
   \]
where

\[ v_b(r, \theta) = \text{Magnitude of velocity at a blade section} = \sqrt{V_{axial}^2 + V_{tangential}^2} \]

\[ q(r, \theta) = 0.5 \rho v_b^2(r, \theta), \quad \Delta A = c(r) \Delta r \]

\[ q(r, \theta) = \text{Dynamic pressure due to the fluid at the blade section, } \Delta A = \text{Area of the blade section, } c(r) = \text{local blade chord, and } \Delta r = \text{length of the blade section.} \]

4. Calculate:

\[ a_{axial} = \frac{\sigma(r) \cdot k}{f_{Prandtl}(r, \theta) - \sigma(r) \cdot k'}, \quad a_{tangential} = \frac{\sigma(r) \cdot k'}{f_{Prandtl}(r, \theta) + \sigma(r) \cdot k'} \]

where

\[ k = \frac{F_{shaft}}{4 \cdot \sin (\epsilon_0)^2}, \quad k' = \frac{F_{tangential}}{4 \cdot \sin (\epsilon_0) \cdot \cos (\epsilon_0)} \]

and \( \sigma(r) \) is local blade section solidity.

5. Calculate \( V_{axial} \) (eq. (2.20)) and \( V_{tangential} \) (eq. (2.21)), and new estimate for \( \epsilon_1 = \tan^{-1}\left( \frac{V_{axial}}{V_{tangential}} \right) \)

Then \( \epsilon_1 \) is used to compute a new value of \( \alpha_{AOA} \) in Step 2, to obtain a new \( \epsilon_2 \). The process is repeated again until the change in value of \( (\epsilon_2 - \epsilon_1) \) is less than a small number determined for the convergence criteria.

Once converged values of \( a_{axial}, a_{tangential} \) and \( \alpha_{AOA} \) are obtained for all the sections along the blade span, the inflow velocity fields \( (u_{inflow}(r, \theta), v_{inflow}(r, \theta)) \) calculated from external sources are combined with the induced velocity terms \( (V_{\infty} [1 + a_{axial} (r)], \Omega r [1 - a_{tangential}(r)]) \) in eqs. (2.20) and (2.21). The converged section \( \alpha_{AOA} \) is used to calculate the corresponding \( C_l \) and \( C_d \) values as obtained by
using the empirical AERODAS model, which gives the lift and drag coefficients for a variety of airfoil families while accounting for thickness of each airfoil [41]. The values of the section lift and drag coefficients are then used to calculate $F_{\text{shaft}}(r, \theta)$ and $F_{\text{tangential}}(r, \theta)$ as shown in Step 5. The propeller shaft thrust and power are calculated, and the process can be repeated again until the blade pitch/RPM is varied to achieve the operating thrust/power levels.

2.3.3 CHARM

The Comprehensive Hierarchical Aeromechanics Rotorcraft Model (CHARM) [42–46] code, developed by Continuum Dynamics, Inc, is a well-validated comprehensive VTOL air vehicle aerodynamics and dynamics analysis computer code applicable to a wide range of propeller-driven, multiple rotor, and ducted fan systems. Extensive validation has shown the model to have unique capabilities in modeling the aerodynamics of multiple propellers, rotors, and airframe interaction aerodynamics and wake modeling [42–45]. CHARM couples a full-span, free-vortex wake model with a vortex lattice lifting surface blade model and a fast doublet panel fuselage model to provide a capability for modeling the complete aircraft aerodynamics involving combinations of props, rotors, wings, fuselages and tail surfaces and potentially their interactions.

The vortex lifting surface for a blade in CHARM is typically modeled with one element along the chord of the blade. Historically, this has proven to be suitable in capturing steady, unsteady and impulsive loads for rotors in a diverse range of flight conditions. CHARM uses curved vortex elements and constant vorticity contours (CVC) to represent the trailed vortex sheet. The loading, constant strength contours and CVC wake are shown in (fig. 2.9). CHARM also offers modeling of lifting and control surfaces such as wings, ailerons, tails as well as nonlifting-surfaces such as nacelles, fuselages, etc.

There are three aerodynamic methods offered in CHARM that can be leveraged for modeling a wing in the wake of a propeller:

1. Lifting Surface Vortex Lattice Method
2. Vortex Lattice Method
3. Lifting Panel Method
2.3.3.1 Lifting Surface Vortex Lattice Method

The lifting surface vortex lattice method is a hybrid scheme developed by Continuum Dynamics Inc. This scheme combines the advantages of lifting line and lifting surface theories[42].

Classical lifting line theory replaces chordwise sections of the aerodynamic
surface with single points along the span (hence forming a line). The 2D angle of attack for each section is calculated and the lift is determined by 2D lookup table.

Lifting surface theory on the other hand is a 3D theory. It replaces chordwise sections with a quadrilateral (or a vortex lattice). The Neumann boundary condition is imposed on every quadrilateral and the velocities and circulation strengths are calculated by taking into account the contributions from incoming wake, freestream and other bodies. It then calculates the lifting force for each quadrilateral using the Joukowsky Law. In order to account for transonic and viscous effects, CHARM is also capable of correcting the lift-coefficient from the 2-D lookup table, thus providing a more accurate treatment of three-dimensional effects.

The lifting surface method is typically used for modeling rotating blade loads in CHARM. While the lifting surface method was originally intended for high aspect ratio wings (like helicopter rotors), its low-computational cost makes it an attractive option for other wings. Throughout this thesis this method is referred to as the Lifting Surface Method (LSM), so as to differentiate between the more general Vortex Lattice Method (VLM), which is merely an extension of this by inclusion of additional control points along the chord of the surface. A lifting surface representation of a wing is shown in fig. 2.10.

Figure 2.10: Lifting surface representation of wing with 60 spanwise and 1 chordwise element.

2.3.3.2 Vortex Lattice Method

The Vortex Lattice Method (VLM) is an extension to the hybrid lifting surface theory, described in the previous section, with additional chordwise control points.
The VLM method involves modeling the lifting surface with a lattice of vortex quadrilaterals whose circulation strengths satisfy the flow tangency boundary condition at selected control points on the surface with wing loads calculated in conjunction with 2-D look-up tables [42]. This is expected to be more suitable for a wing than the lifting surface method with a single chordwise control point. However, there is a noticeable increase in computational cost.

![Figure 2.11: Lifting surface representation of wing with 30 spanwise and 4 chordwise elements.](image)

### 2.3.3.3 Lifting Panel Method

The Lifting Panel Method (LPM) involves discretizing the body with a surface grid representing a set of panels (fig. 2.12b). The flow is then completely represented by surface distributions of source and dipole singularities, and the LPM method solves for the values of the singularity strengths so as to satisfy the zero surface normal velocity condition. CHARM has implemented its own fast LPM method using a fast multipole algorithm as described in Boschitsch et al. [47, 48]. This fast panel method is also *coupled* to the CVC rotorcraft wake model to simulate the unsteady, complicated, and highly vortical flow about bodies.

Hence, the fast panel method is the most computationally expensive option in CHARM, but is expected to capture flow phenomenon like thickness effects not captured by VLM methods. Panel methods will not be able to capture flow-field effects arising due to compressibility and viscosity of the fluid. Even so, when such effects are important, panel methods are still computationally cheaper than volumetric grid-based CFD methods. Hence, the lifting panel methods should remain a reasonable option to be employed in preliminary design and scoping studies before bringing to bear more comprehensive and expensive analyses.
It should be noted that a pre-existing coupling between CHARM and PSU-WOPWOP exists. This coupling is built into CHARM and based upon input flags, CHARM automatically creates files enabling smooth transition of data from the aerodynamic to the acoustic tools. At Penn State, this has been successfully used for LSM and VLM methods. However, issues were faced when Lifting Panel data was being exported from CHARM to PSU-WOPWOP using the pre-existing coupling. Hence, a separate coupling code was written to read the raw aerodynamic data from the lifting panel method and export it to PSU-WOPWOP. The pressure on each panel was extracted and exported to PSU-WOPWOP in a structured grid data format, with the pressure values being node centered.

A series of low and mid-fidelity aerodynamic tools used in the thesis were described in the current chapter. In chapter 3 validation of these tools for the current work is presented. The validation will rely on new experimental data, or it will rely on CHARM’s previous successes in modeling isolated propeller and helicopter noise [34, 49, 50].
Chapter 3
Results

In section 2.3 a series of low and mid-fidelity aerodynamic tools used in the thesis were described. In this chapter we will be looking at validating these tools. The validation will rely on experimental data, or it will rely on CHARM’s previous success in modeling rotocraft aerodynamics and acoustics [34, 49, 50].

3.1 Evaluation of Simple Aerodynamic Tools

A typical eVTOL vehicle is expected to have multiple cruise propellers and lift rotors operating in highly unsteady flowfields due to aerodynamic interactions between different components (section 1.3, fig. 1.5). While simple aerodynamic tools involving quasi-steady blade element methods, cannot capture such flow complexity, they are certainly capable of capturing blade loads operating in simple flowfields such as cruise propeller operating in axisymmetric flow and in flight conditions where the propeller is operating at an angle of attack. A comparison of simple aerodynamic tools with CHARM for isolated rotors is made in shaft angle of attack conditions section 3.1.1. This section explores the effectiveness of these simple and quick tools, as they hold significant potential in rapid design iterations.

3.1.1 Evaluation of Quasi-Steady Model with Respect to CHARM

In absence of relevant experimental data, noise results calculated using airloads determined by QBET (Quasi-steady Blade Element Theory) and XROTOR QS (Simple quasi-steady model using XROTOR) were compared with results from CHARM for a propeller operating with low shaft angles of attack (-7° ≤ α_{AOA} ≤
7°). A comprehensive free-wake code like CHARM has been shown to perform well for rotors and propellers operating in similar conditions with respect to experimental data [34, 49, 50]. Hence, results from CHARM serve as a baseline in this section.

3.1.1.1 Propeller Operating at Low Shaft Angles of Attack

In order to better compare the efficacy of simplified quasisteady model with XROTOR (XROTOR QS) and Quasi-steady BET (QBET) codes, a comparison of the noise generated from an isolated propeller operating at constant shaft angle of attack (\(\alpha_{\text{AOA}}\)) is shown in fig. 3.1. The YO-3A propeller was chosen as it is a low-speed propeller [51] and operates in a flow regime that is acoustically desirable for eVTOL vehicles. Further discussion of the propeller and its design operating conditions with the YO-3A aircraft are given in section 3.3.

The YO-3A propeller was designed to operate in constant thrust at low operational tip-Mach number. The propeller in this case has a rotation speed of 780 RPM, with a resultant helical tip-Mach number of 0.32, and operates with a forward flight speed of 125 ft/s (38.1 m/s). The propeller is trimmed to operate at a constant thrust of 220 lb (978 N), which is achieved by changing the pitch of the propeller blades.

The propeller noise is evaluated for \(\alpha_{\text{AOA}} = [-7°, -5°, -2°, 0°, 2°, 5°, 7°]\) and noise is evaluated at the microphone shown in fig. 3.1 (30 propeller radii below the propeller, moving with the propeller(aircraft)).

![Figure 3.1: YO-3A isolated propeller schematic and setup.](image-url)
The following acoustic results are being analyzed here:

- **Acoustic pressure time history**: Represents the raw pressure signal that an observer/microphone receives. It’s an unsteady pressure plot (units Pa), where the nature of the curve often provides helpful insight into the physical processes behind the source of noise.

- **Overall Sound Pressure Level (OASPL)**: Provides the net representative sound energy level for the range of acoustic frequencies calculated. This is a common metric used for quantification of noise and is expressed in units of decibels (dB), a logarithmic scale, where the reference pressure is $20 \mu \text{Pa}$. The value of the OASPL below the propeller is intended to quickly allow an engineer to evaluate the noise levels with respect to a target value. These target values could be based upon commonly encountered OASPL levels as reported by the Centers for Disease Control and Prevention [52], or weighted metrics such as A-weighted sound pressure level (with units dBA) can be used to establish targets based on human comfort (such as 67 dBA at a point 250 ft away from an eVTOL vehicle [1].

Figure 3.2 shows the variation of loading noise OASPL with respect to different angles of attack ($\alpha_{AOA}$) computed at the microphone as shown in fig. 3.1. The following observations can be made:

- Noise levels from QBET and CHARM match very closely in terms of trend and magnitude. There seems to be a near constant difference of 1 - 2 dB, with QBET results that are slightly higher than CHARM results. This is currently thought to arise from a numerical issue with the implementation of Prandtl’s tip-loss factor in the QBET code. This numerical issue leads to an overestimation of the loss of thrust at the blade tip and hence the code compensates for that by pitching the blade higher to trim for the same thrust value of 220 lb.

- Due to the rather crude approximation used in the XROTOR QS model, it doesn’t perform well for negative angles of attack as compared to QBET. However, it agrees well with the trends and magnitude for positive angle of attack values.
Figure 3.2: Comparison of loading noise (OASPL) for propeller operating at angle of attack using XROTOR QS, Quasisteady BEM (QBET), and CHARM.

The comparison of the loading OASPL between QBET and the more accurate CHARM shows the potential of QBET method to accurately capture the trends and levels of the noise generated from a propeller operating at constant shaft angle of attack. This could potentially serve as the basis for a useful isolated propeller design and aeroacoustic optimization tool.

However, in order to extend this tool for optimization of multiple propellers having simultaneous acoustic interactions for an eVTOL vehicle, the temporal accuracy of the acoustic signals generated by these methods must be verified with respect to CHARM. In Figures 3.3 and 3.4 the acoustic pressure time histories of the loading noise ($p'_L(x,t)$, eq. (2.14)) are shown for all the angles of attack ($\alpha_{AOA}$) in fig. 3.2. This comparison is also expected to provide a greater physical insight into the results because unlike OASPL it is not a time-averaged quantity. The following quick observations can be made:

- For positive angles of attack and axisymmetric flow conditions, the loading noise signals from three methods seem to match closely in phase and peak magnitude.

- For negative angles of attack there seems to be a phase difference between three methods, with the difference being more prominent as the value of $\alpha_{AOA}$ decreases below $-2^\circ$. At $-7^\circ$, the phase difference between acoustic signals
from CHARM and QBET is found to be around $\Delta t_{\text{phase}} = 0.0076$ seconds (which approximately corresponds to $\theta_{\text{phase}} = 35^\circ$ based on $\theta_{\text{phase}} = \omega \Delta t_{\text{phase}}$). This significant difference exists despite the loading OASPL values being the same at $\alpha_{\text{AOA}} = -7^\circ$ for both the methods.

This phase difference observed for negative values of $\alpha_{\text{AOA}}$ could be an important difference when evaluating the aeroacoustic interactions between multiple propellers, and evaluating the impact of an eVTOL vehicle with multiple propellers. In order to better understand the differences, an investigation of individual terms in the expression for loading noise in eq. (2.14) (re-arranged below in eq. (3.1)) was undertaken.

$$p'_L(\vec{x}, t) = \frac{1}{c} \int_{f=0}^{\infty} \left[ \frac{\dot{l}_r}{r |1 - M_r|^2} \right]_{\text{ret}} dS \quad \text{(Term 1)}$$
$$+ \frac{1}{c} \int_{f=0}^{\infty} \left[ \frac{l_r \dot{M}_r}{r |1 - M_r|^3} \right]_{\text{ret}} dS \quad \text{(Term 2)}$$
$$+ \int_{f=0}^{\infty} \left[ \frac{l_r - l_M}{r^2 |1 - M_r|^2} + \frac{l_r (M_r - M^2)}{r^2 |1 - M_r|^3} \right]_{\text{ret}} dS \quad \text{(Term 3)}$$

Here **Term 1** helps us understand the contribution of the noise generated in-part of the time-derivative of the aerodynamic loading (potentially important when unsteady loading is important). **Term 2** helps us understand the contribution of far-field noise generated due to the loading and blade Mach number. **Term 3** includes the near-field acoustic terms. Figure 3.5 shows the contribution of three terms shown in eq. (3.1). The phase difference can be clearly seen to be arising from the unsteady loading term **Term 1**. Hence, phase differences in loading signal between QBET and CHARM seem to be the source of phase differences in the acoustic signal. It is reasonable that unsteady loading effects modeled in CHARM, but not in QBET, are responsible. Further work needs to be done in examining the impact of positive and negative angle of attacks on the phenomenon currently not being captured by QBET. Potential avenues of differences include QBET’s inferior tip-loss estimation and calculation of the spanwise induction factors: $a_{\text{axial}}(r, \theta)$ and $a_{\text{tangential}}(r, \theta)$ (eqs. (2.16) and (2.17)). Both calculations were originally meant for helical wake systems created from propellers in axisymmetric flow. A potential correction to the classical expression for Prandtl’s tip-loss factor with a wake-skew
parameter could improve results [53]. It should also be noted that the values of $a_{axial}$ and $a_{tangential}$ seem to be exceeding empirical limits recommended by Viterna and Janetzke [54]. They placed the limit of 0.7 based on experimental data from horizontal-axis wind turbines. The current study found the values nearing 0.9 (theoretical limit is 1.0). It is not known whether such high values are realistic for the unsteady operating conditions or originating due to deficiencies in the Newton-Rhapson algorithm used to solve for the induction factors.

![Image of acoustic pressure time history](image)

(a) AOA = $-7^\circ$

(b) AOA = $7^\circ$

(c) AOA = $-5^\circ$

(d) AOA = $5^\circ$

Figure 3.3: Comparison of acoustic pressure time history for propeller operating at angle of attack ($\alpha_{AOA} = -7^\circ, 7^\circ, -5^\circ, 5^\circ$) using XROTOR, QBET and CHARM.
Figure 3.4: Comparison of acoustic pressure time history for propeller operating at angle of attack ($\alpha_{AOA} = -2^\circ, 2^\circ, 0^\circ$) using XROTOR, QBET and CHARM.
Figure 3.5: Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack ($\alpha_{AOA} = -7^\circ$).
Continuing the breakdown of different terms of the Total loading noise signal (eq. (3.1)) for the remaining angles of attack (figs. 3.7 to 3.12), a decrease in phase lag for the unsteady loading term Term 1 is apparent. The predicted contribution from the steady loading term Term 2 match for all the three methods. This shows that XROTOR QS and QBET are able to accurately capture the steady aerodynamic effects for a propeller under a series of shaft angles of attack when compared to results from CHARM.

A noteworthy observation from the breakdown of loading noise pressure-time signals for $\alpha_{AOA} = 0^\circ$ (fig. 3.9) is that Term 1 has a higher peak amplitude than Term 2. It should be noted that for $\alpha_{AOA} = 0^\circ$ the magnitude of loading on each blade section is constant. For such flow physics, intuitively the contribution of Term 1, which depends on the time derivative of the blade loading, is expected to be lower than the contribution of Term 2, which depends on the loading and
blade Mach number. In order to understand this better, it is useful to consider the composition of Term 1. The loading vector for any blade element of area \( dS \) can be represented as:

\[
\vec{l} = P(t) \hat{n} = \{ P(t) \, n_x(t) \} \hat{i} + \{ P(t) \, n_y(t) \} \hat{j} + \{ P(t) \, n_z(t) \} \hat{k} \quad (3.2)
\]

Where \( P(t) \) is the pressure exerted by the blade surface on the fluid, \( \hat{n} \) is the surface outward unit normal vector with \( x,y,z \) \((\hat{i}, \hat{j}, \hat{k})\) components in the global inertial coordinate system written as \( n_x(t), n_y(t) \) and \( n_z(t) \). Taking the partial time derivative of the loading vector and expanding via the product rule, we get:

\[
\frac{\partial \vec{l}}{\partial t} = \{ \frac{\partial P(t)}{\partial t} n_x(t) + \frac{\partial n_x(t)}{\partial t} P(t) \} \hat{i} + \{ \frac{\partial P(t)}{\partial t} n_y(t) + \frac{\partial n_y(t)}{\partial t} P(t) \} \hat{j} \quad (3.3)
\]

\[
\quad + \{ \frac{\partial P(t)}{\partial t} n_z(t) + \frac{\partial n_z(t)}{\partial t} P(t) \} \hat{k}
\]

For a propeller operating in axisymmetric flow, the \( P(t) \) is a time-invariant function. Hence \( \frac{\partial P(t)}{\partial t} = 0 \). Substituting this back into the expression for \( \frac{\partial \vec{l}}{\partial t} \), we get:

\[
\frac{\partial \vec{l}}{\partial t} = \{ 0 + \frac{\partial n_x(t)}{\partial t} P \} \hat{i} + \{ 0 + \frac{\partial n_y(t)}{\partial t} P \} \hat{j} + \{ 0 + \frac{\partial n_z(t)}{\partial t} P \} \hat{k} \quad (3.4)
\]

\[
\quad = \{ \frac{\partial n_x(t)}{\partial t} P \} \hat{i} + \{ \frac{\partial n_y(t)}{\partial t} P \} \hat{j} + \{ \frac{\partial n_z(t)}{\partial t} P \} \hat{k}
\]

Based on this, the \( \vec{l}_r \) term in Term 1 for \( a \) can be written as:

\[
\vec{l}_r = \frac{\partial \vec{l}}{\partial t} \cdot \hat{r} = \{ \frac{\partial n_x(t)}{\partial t} P \} \hat{i} + \{ \frac{\partial n_y(t)}{\partial t} P \} \hat{j} + \{ \frac{\partial n_z(t)}{\partial t} P \} \hat{k} \cdot \hat{r} \quad (3.5)
\]

Where \( \hat{r} \) is the unit radiation vector from the source to the observer. We can find the normal vector for the blade element knowing that the angular velocity and the forward speed of the propeller. Based on the coordinate system defined in fig. 2.7, we take a point on the propeller at a distance \( r \) from the hub, and find its position vector \( (\eta(t)) \) as follows:
\[ \hat{\eta}(t) = V_\infty t \hat{i} + (-r \cos \theta) \hat{j} + (-r \sin \theta) \hat{k} \]

Knowing \( \theta = \Omega t \) and \( \frac{d\theta}{dt} = \Omega \), the unit tangent vector can be given by:

\[ \hat{T}(t) = \frac{\hat{\eta}(t)}{|\hat{\eta}(t)|} = \frac{V_\infty \hat{i} + (\Omega r \sin \Omega t) \hat{j} + (-\Omega r \cos \Omega t) \hat{k}}{\sqrt{V_\infty^2 + \Omega^2 r^2}} \]

The unit normal vector can hence be given by:

\[ \hat{n}(t) = \frac{\hat{T}'(t)}{|\hat{T}'(t)|} = \frac{\Omega^2 r \cos \Omega t \hat{j} + \Omega^2 r \sin \Omega t \hat{k}}{\sqrt{V_\infty^2 + \Omega^2 r^2}} = \cos \Omega t \hat{j} + \sin \Omega t \hat{k} \]

We find \( n_x(t) = 0 \), \( n_y(t) = \cos \Omega t \), \( n_z(t) = \sin \Omega t \). Substituting back into the expression for \( \hat{l}_r \), we get:

\[ \hat{l}_r = \Omega P \left\{ \sin \Omega t \hat{j} + \cos \Omega t \hat{k} \right\} \cdot \hat{r} \]  \hspace{1cm} (3.6)

Hence for a single blade surface element on a propeller operating in axisymmetric flow we can see the dependency of \( \hat{l}_r \) on the local value of \( P \) and the shaft RPM \( \Omega \). This shows that even for axisymmetric propeller loading noise, the effect of the kinematic rotational speed exists, and should not be easily discounted.
Figure 3.7: Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack ($\alpha_{AOA} = -5^\circ$).
Figure 3.8: Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack ($\alpha_{AOA} = -2^\circ$).
Figure 3.9: Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack ($\alpha_{AOA} = 0^\circ$).
Figure 3.10: Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack ($\alpha_{AOA} = 2^\circ$).
Figure 3.11: Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack ($\alpha_{AOA} = 5^\circ$).
(a) Total loading acoustic pressure, \( p'_L(\vec{x}, t) \)

(b) Term 1

(c) Term 2

(d) Term 3

Figure 3.12: Comparison of Total, Term 1, Term 2, and Term 3 components of loading noise acoustic pressure time history (eq. (3.1)) for propeller operating at angle of attack \( \alpha_{AoA} = 7^\circ \).
3.2 Propeller-Wing Interactions

As discussed in section 1.4, a key objective of this thesis is to investigate the aeroacoustics of propeller-wing interactions in tractor configuration: i.e, the propeller is in front of the wing. Propeller-wing interactions are expected to be common across several eVTOL configurations. Understanding the aerodynamics will play a key role in noise prediction, especially for Vectored Thrust and Lift + Cruise configurations. In a Vectored Thrust configuration, like A³ Vahana by Airbus (fig. 1.3), propeller-wing interactions during transition will likely involve both the components operating in a shaft angle of attack. This kind of interaction is also commonly observed in tilt-wing rotorcraft. Figure 3.13 shows the propeller-wing interactions visualized using Q-criterion iso-vorticity surfaces for a tilt-wing UAV (Unmanned Aerial Vehicle) concept developed by NASA [31]. Figures 3.13b and 3.13c show that the nature of these interactions vary for different shaft angle of attacks (AOA).

In order to better understand these differences, the mid-fidelity free-wake tool CHARM was used to study propeller-wing interactions and validated using the experimental data reported by Dunsby et al. [55, 56]. The capabilities of the three aerodynamic methods offered in CHARM (section 2.3.3) will be explored and validated against different experimental parameters. It should be noted no acoustic measurements were taken for this setup.

Based on the performance of CHARM to match up with respect to experimental data, aerodynamic flow conditions in which CHARM gives the best predictions will be identified. These flow conditions will then be applied to a slightly modified wing geometry, where the symmetry of the propeller-wing setup will enable understanding of the aerodynamics and noise from different regions of wing influenced by propwash.

During the course of this work an investigation to understand the acoustics of the YO-3A surveillance airplane was also undertaken. The motivation to incorporate the YO-3A study by Griffith et al. [51] was to aid the understanding of the acoustics of low-speed propellers. The Griffith et al. study also demonstrates the importance of propeller-wing interactions to overall noise as the tip-Mach number of the propeller is reduced.
3.2.1 Aerodynamic Validation: Dunsby

To evaluate CHARM and PSU-WOPWOP for propeller-wing interactions, it is important to ensure that CHARM captures the steady and unsteady aerodynamic loads on the propeller and wing accurately, while the data is being fed into PSU-WOPWOP in the correct format. As seen in section 2.3.3, CHARM offers three aerodynamic methods of varying fidelities and computational speeds to evaluate unsteady aerodynamics of an eVTOL vehicle in-flight:

1. Lifting Surface Vortex Lattice Method (LSM)
2. Vortex Lattice Method (VLM)
3. Lifting Panel Method (LPM)

The first comparison with model data explores the wing loading and noise generated from a propeller-wing system operating at different angles of attack. Experimental data is available from Dunsby et al. [55, 56], which offers the time-averaged wing $C_L$ and the distribution of wing section $C_l$ across the span of the wing. Figure 3.14a shows the schematic of the propeller-wing system, while fig. 3.14b shows the wind tunnel setup. The propeller is operating at constant rotational tip-Mach number of 0.33 (3000 RPM, Blade Passage Frequency = 200 Hz), and the wind tunnel velocity was set to 95 ft/s (29.0 m/s). The propeller-wing system is pitched over a range of angles resulting in an effective geometric angle of attack $\alpha_g$ shown in fig. 3.14a. Five values of $\alpha_g$ are reported: $-10^\circ, -5^\circ, 0^\circ, 5^\circ, 10^\circ$. The wing has a symmetric NACA 0015 airfoil cross section. It should be noted that the wing tip was not modeled in CHARM and hence its potential effects may not be fully accounted for in the current calculations. The report by Currie and Dunsby [56] mentions the corrections implemented by the authors to take into account the effect of wind tunnel walls on the propeller-wing system.

![Schematic of computational setup](image1)

![Wind tunnel setup](image2)

Figure 3.14: Dunsby et al. [55] and Currie and Dunsby [56] propeller-wing experimental setup.
3.2.2 Aerodynamic and Acoustic Results from LSM, VLM, LPM

The three aerodynamic methods offered by CHARM: Lifting Surface Method (LSM) (section 2.3.3.1), Vortex Latice Method (VLM) (section 2.3.3.2) and Lifting Panel Method (LPM) (section 2.3.3.3) are implemented for the propeller-wing system in the Dunsby report. Figures 3.15a, 3.15c and 3.15e show the representations of these methods in CHARM. Figures 3.15b, 3.15d and 3.15f show the Constant Vorticity Contour (CVC) wake representation of the wake trailed by the propeller and the wing. As the wake deformation shows, the propeller and the wing influence each other in CHARM in every method.

Table 3.1: Number of spanwise and chordwise control points used to model the Dunsby wing using LSM, VLM, and LPM in CHARM.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>LSM</th>
<th>VLM</th>
<th>LPM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Spanwise control points</td>
<td>60</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>Chordwise control points</td>
<td>1</td>
<td>4</td>
<td>50</td>
</tr>
</tbody>
</table>

(a) Lifting Surface Method (LSM) representation  
(b) CVC wake trailed by propeller on wing (LSM)

Figure 3.15: CHARM’s CVC wake representation for Dunsby propeller-wing system.
The upcoming sections compare the results from CHARM with respect to the experimental data reported by Dunsby et al. This comparison provides further insight into the aerodynamics of the system and the capabilities of CHARM to model them.

### 3.2.2.1 Variation of Wing $C_{L-avg}$ with Respect to $\alpha_g$

Figure 3.16 shows the variation of time-averaged wing lift coefficient ($C_{L-avg}$) of the entire wing operating in the wake of a propeller ahead of the wing for various angles of attacks ($\alpha_g$). The wing in the wake of the propeller was modeled using three aerodynamic methods in CHARM are reported here.
1. **Lifting Surface Method**: Figure 3.16a shows the variation of wing $C_{L_{avg}}$ in presence of the propeller wake (as shown in fig. 3.15a, fig. 3.15b). There is excellent agreement between experimental data ($C_{L_{Dunsby}}$) and values from CHARM ($C_{L_{LSM}}$) across the range of angle of attack values ($\alpha_g$).

2. **Vortex Lattice Method**: The variation of wing $C_L$ in presence of the propeller wake for the vortex lattice method is shown in fig. 3.16b. Again there is very good agreement between experimental data ($C_{L_{Dunsby}}$) and values from CHARM, ($C_{L_{VLM}}$) across the range of angle of attack values ($\alpha_g$), with minor differences at negative angles of attack (especially at $-10^\circ$).

3. **Lifting Panel Method**: Figure 3.16c shows the variation of wing $C_{L_{avg}}$ in presence of the propeller wake (as shown in fig. 3.15e, fig. 3.15f). The LPM method also is in excellent agreement between experimental data, $C_{L_{Dunsby}}$, and values from CHARM, ($C_{L_{LPM}}$) across the range of angle of attack values ($\alpha_g$), with a very minor difference at $-10^\circ$ AOA.

Figure 3.16: Variation of $C_{L_{avg}}$ for different methods.
The three methods have shown excellent agreement throughout the range of \( \alpha_g \) (geometric angles of attack) for which experimental data was evaluated. It should be noted that the original report [56] mentions that the wing cross-section airfoil (NACA-0015) begins to stall as \( \alpha_g \) nears \( \pm 10^\circ \). Previous work conducted at Continuum Dynamics Inc. has demonstrated the capability of lifting surface theory to capture trends in \( C_{L-avg} \) for a similar propeller-wing system [57] at higher angles of attack.

Based on the trends, it can be concluded that modeling the wing as a lifting surface with one chordwise control point for fast preliminary evaluation of propeller-wing systems, would suffice to predict and capture trends in overall time-averaged wing lift coefficient \( (C_{L-avg}) \). This method is the fastest among the three methods depicted here, and should help speed up the design process for early evaluation of configurations with propeller-wing interactions. However, greater accuracy is needed for noise predictions, and the accurate representation of spanwise distribution of loading trends is needed.

### 3.2.2.2 Variation of \( C_L \) Across Wing Span with Respect to \( \alpha_g \)

In addition to reporting the overall time-averaged \( C_L \), Currie and Dunsby [56] also provide an experimental plot (fig. 3.17) of time-averaged lift coefficient for different sections of the wing for different angles of attack \( (\alpha_g) \). This enables better understanding of the effect of propeller swirl and flow angle of attack.

Figure 3.17 also shows the location of the propeller disk with respect to the wing-span. The nondimensional span position of 0.6 represents the center/hub of the propeller. The propeller rotates counterclockwise when viewed from the rear. The following key features can be observed in fig. 3.17:

1. A near constant \( C_L \) is observed from spanwise position of 0 to 0.2 for AOA = 0°. For positive angles of attack, the slope is positive, while the slope is negative for negative angles of attacks.

2. A change in the slope of \( C_L \) can be observed as one moves from the left of the plot to the right. The first inflection point is observed around 0.2 spanwise station as the slope changes to negative. This is near the edge of the propeller disk.
Figure 3.17: Variation of steady spanwise distribution of $C_L$ (viewed from the rear) with respect to changing geometric angles of attack ($\alpha_g$) [56].

3. The slope is seen to be negative from 0.2 (edge of the propeller disk) to 0.4 (midspan of the propeller blade going down). From 0.4 of the wingspan, there is a second inflection point as the slope turns positive.

4. From the second inflection point, $C_L$ increases for all angles of attacks until it reaches the third inflection point around 0.8 spanwise position (midspan of the blade going up).
   
   (a) For $0^\circ$ and positive angles of attack, there is a negative slope, i.e., a decrease in $C_L$.

   (b) For negative angles of attack, no third inflection point is observed and the $C_L$ continues to increase.

Near the tips of the wing, the air is free to move from the region of high pressure into the region of low pressure. This results in the formation of wing tip vortices and subsequent loss of lift from spanwise station 0.8 to 1 (tip of the wing). Hence, the width of this region of lift-loss would be determined by the overall dominance of wing tip pressure loss vs the propeller swirl component.

5. The direction of the swirl created due to the rotational direction of the
propeller is important as it could in turn increase or decrease the angle of attack on a wing section.

6. From position 0.2 to 0.6, the swirl from the propeller wake leads to a decrease in the angle of attack of the wing sections and hence a lower section lift-coefficient. From position 0.6 (the center of the propeller disk) to 1, the swirl leads to an increase in angle of attack of the wing sections and hence a higher section lift-coefficient.

7. It should be noted that the wing begins to operate in stall-like conditions when the section angle of attack reaches a value of ±10° or more. This is a result of using a symmetric NACA-0015 airfoil section [56]. Hence, for values of \( \alpha_g = +10^\circ \), it should be expected that some sections of the wing will operate in stall as the propeller swirl increases the angle of attack. This is evident from the flattening of \( C_L \) between 0.6 to 0.8 spanwise position and absence of the third inflection point, as the increase in section angle of attack with the swirl leads to a drop in performance as the airfoil reaches stall.

It should also be noted that sections of the wing operating in propwash (nondimensional span positions 0.2 - 1), might see velocities higher than the rest of the wing. But the convention for the section lift-coefficient remains the same throughout:

\[
C_L = \frac{L}{q''A} \tag{3.7}
\]

where,

\[
L = \text{lift force}
\]

\[
q'' = \frac{1}{2} \rho v_\infty^2 = \text{freestream dynamic pressure}
\]

\[
v_\infty = \text{freestream velocity}
\]

\[
A = \text{planform area of the wing}
\]

Similar to section 3.2.2.1, the spanwise distribution of time-averaged \( C_L \) is obtained from three aerodynamic methods of CHARM and compared to experimental data [55, 56]) for different angles of attack (\( \alpha_g \)).
1. **Lifting Surface Method**: Figure 3.18a shows the variation of lift coefficient $C_L$ across the span of the wing, modeled via a lifting surface with one control point across the chord of the wing. While the rough trends are captured, there are significant differences in $C_L$ throughout the span. Such differences are expected because the lifting surface theory uses only one chordwise control point, which is normally enough for wings with high aspect ratios (like rotor, propeller blades), but may not be sufficient for low aspect-ratio rectangular wings, such as the Dunsby wing with an aspect ratio of 4.48. The key observations include:

(a) The section $C_L$ is shown to begin at much lower values than observed experimentally. It should be noted that the wind tunnel walls were *not modeled* in CHARM. While the experimental report [56] states that wall corrections were made to the data, it's not yet understood on how these corrections were applied. The general trends in slope from spanwise position of 0 to 0.2 were roughly captured. A future step could be to model the wind tunnel wall in CHARM, observe its impact on the $C_L$ distribution, and compare it with a modified Dunsby plot where the corrections have been removed.

(b) Since the $C_L$ values at the wing left tip were greatly underestimated, the results seem to have missed the first inflection point observed earlier around 0.2 spanwise station, except for $\alpha_g = 5^\circ$. It is currently hypothesized that this is a one-off condition where the pressure loss at the left wing tip was calculated to be less dominant than the swirl for this particular angle of attack.

(c) The second inflection point is seen to be consistently captured by CHARM at 0.4 (midspan of the propeller blade going down). The values are consistently under-predicted for all angles of attacks except for $\alpha_g = 10^\circ$. Because $\alpha_g = 10^\circ$ is considered to be a stall case, this case is considered beyond the capabilities of CHARM because CHARM only deploys 2-D lookup tables for capturing stall effects in performance.

For $\alpha_g = -10^\circ$ there is a significant underprediction of $C_L$, while for $\alpha_g = 10^\circ$ there is a significant overprediction and a failure of the lifting surface method to capture the stall effects between spanwise positions.
0.5 and 0.9.

(d) The observed trend is increasing $C_L$ from the second inflection point to the third inflection point for all angles of attack. This shows that CHARM is able to capture the effect of the swirl for all angles of attack. A general trend of over prediction is observed throughout. This overprediction is severe for the case of $\alpha_g = 10^\circ$, where the wing sections are likely operating in stall.

Approaching the third inflection point, CHARM starts capturing the loss in lift due to the wing tip. Some agreement is observed in this region for $\alpha_g = -5, -10^\circ$.

(e) The reasonable capture of the second and the third inflection points suggests that CHARM, with wing modeled as a lifting surface with one chordwise control point, would be reasonable in capturing the swirl, unless operating in stall.

2. **Vortex Lattice Method (VLM)**: Figure 3.18b shows the variation of lift coefficient $C_L$ across the span of the wing as calculated by VLM. This method comes with an increased computational cost with respect to the lifting surface method. However, there is negligible increase in the time taken to setup as compared to the lifting surface theory. The key observations include:

(a) The underprediction of $C_L$ at the left tip of the wing is also observed here as well.

(b) Similar to lifting surface method, the first inflection point is not captured as the pressure loss at the wing tip is over-predicted. However, the trends between spanwise position 0.2 (first inflection point) and the second inflection point are better matched for $\alpha_g = 5^\circ, 0^\circ, -5^\circ$ than lifting surface theory. The severe underprediction observed in lifting surface theory, was not observed here. This could be attributed to inclusion of 4 chordwise control points, possibly leading to better capture of chordwise loading effects on the wing.

(c) The trends from spanwise position between 0.6 and 0.8 (third inflection point) continue to match closely for most angles of attack except for
\( \alpha_g = 10^\circ, 5^\circ \). However, even in these cases the level of overprediction as compared to lifting surface theory has reduced.

The effect of the wing tip on the \( C_L \) distribution was captured reasonably well for most angles of attack (especially the negative angles of attack). This result provides increased confidence in CHARM’s VLM method for capturing the spanwise variation of wing \( C_L \).

(d) The propeller swirl seems to have been modeled reasonably well with the capture of the second and third inflection points.

3. **Lifting Panel Method (LPM)**: Figure 3.18c shows the variation of lift coefficient \( C_L \) across the span of the wing as computed using the LPM. This method is the most computationally expensive, and requires a completely different style of setup and input files for the wing, unlike the ones used in LSM and VLM. This method does not, however, inherently need 2-D lookup tables, making it the only option in CHARM which could theoretically allow the inclusion of airfoils in a wing for which lookup tables are not available. The key observations include:

(a) The underprediction of \( C_L \) is consistent with the results produced by the previously discussed aerodynamic methods.

(b) Trends similar to the VLM results are observed here as well, except for a slight tendency to overestimate the \( C_L \) in comparison with the VLM. It should be kept in mind that the VLM already had a tendency to overestimate in this region. It also seems to capture the second inflection point better than LSM and VLM for \( \alpha_g = 5^\circ \) (fig. 3.19d).

(c) The spanwise region from 0.4 to 0.8 is not captured as well as it is for the VLM method for most angles of attack (figs. 3.19a to 3.19e). The performance of lifting panel methods deteriorates significantly when operating in potential stall-like conditions such as \( \alpha_g = 10^\circ \). Further investigation needs to be done in order to determine the limits of the LPM to model aerodynamics in such flow regimes.

(d) The agreements with other aerodynamic methods are at best only for \( \alpha_g = 5^\circ, 0^\circ, -5^\circ \), figs. 3.19c to 3.19e.
In conclusion, the VLM method is currently found to be more accurate in predicting spanwise distribution of wing $C_L$ (including stall-like conditions) as compared to other methods evaluated here. This could be important as VLM is also much less computationally expensive than the LPM method. However, without evaluating the LPM method with inclusion of lookup tables, this conclusion could be premature.
3.2.2.3 Potential Impact of LSM, VLM and LPM on Acoustic Results

While no acoustic data was measured during the experiments, acoustic results were computed as a theoretical exercise. The results in this section are completely computational and lacking validation with respect to experimental data. Hence, engineering judgement will be used to evaluate the results obtained in this section.

The propeller in the Dunsby propeller-wing system (fig. 3.14a) operates with a shaft angle of attack and the upwash from the wing. This upwash is expected to introduce unsteadiness in blade loads as it rotates around the azimuth. Figure 3.20 shows the location of the microphone/observer for which the noise calculations have been made. The aerodynamic results from three methods (LSM, VLM, LPM) offered in CHARM were discussed in sections 3.2.2.1 and 3.2.2.2. The propeller and wing loads as calculated were used for acoustic calculations using PSU-WOPWOP. It should be noted that input files for PSU-WOPWOP used in LSM, VLM were obtained from CHARM directly (via the NOISE = -4 input flag in CHARM), while a utility was written to convert the raw geometry and aerodynamic data obtained from the LPM method to a structured grid for PSU-WOPWOP.
(a) $\text{AOA} = -10^\circ$

(b) $\text{AOA} = 10^\circ$

(c) $\text{AOA} = -5^\circ$

(d) $\text{AOA} = 5^\circ$

(e) $\text{AOA} = 0^\circ$

Figure 3.19: Spanwise distribution of $C_L$ for different angles of attack (AOA).
Figure 3.20: Schematic of the hypothetical observer/microphone placed in PSU-WOPWOP to compute the noise for the Dunsby propeller-wing system.

Figure 3.21 shows noise results for the different aerodynamic methods (LSM, VLM, and LPM). Each plot shows OASPL (Overall Sound Pressure Level) on the Y-Axis, and angle of attack ($\alpha_g$) of the Dunsby propeller-wing system on the X-axis. The three images represent OASPL values as computed individually from the propeller, wing, and the propeller+wing system. The following key features can be noted:

1. All three methods predict the noise contribution from the wing as overwhelmingly dominant.

2. A “dip” in OASPL level about $\alpha_g = 0^\circ$ is noticed for the wing noise levels irrespective of the aerodynamic method.

3. The LPM method seems to predict wing noise higher than LSM and VLM.

In order to better understand the OASPL results, an analysis of the acoustic pressure time histories was performed using the loading data from the CHARM runs. Since the propeller and wing noise levels are orders of magnitude different, the analysis of their acoustic pressure time histories was performed separately.
Figure 3.22 looks at the acoustic pressure time history from an isolated propeller as well as a propeller in front of wing modeled by LSM, VLM and Panel methods. It should be noted that the propeller here is always aerodynamically modeled as a lifting surface with one chordwise point, irrespective of the aerodynamic method used to model the wing. The following observations can be made:

1. For $\alpha_g = 0^\circ$ (fig. 3.22e), the signal from all the aerodynamic methods does not differ much from the isolated propeller noise signal. This is expected as the wing is producing negligible lift (fig. 3.16).

2. For $\alpha_g = 5^\circ, 10^\circ$ (figs. 3.22b and 3.22d), increases in the signal peaks is evident. This is expected as the $C_{L-avg}$ is increasing with increasing value of $\alpha_g$. The signal also doesn’t vary much with a change in aerodynamic method used for the wing.

3. For $\alpha_g = -5^\circ, -10^\circ$ (figs. 3.22a and 3.22c), a slight phase shift is observed along with the increase in signal peak. There is noticeable variability with choice of aerodynamic methods of the wing (especially for $\alpha_g = -10^\circ$).

Hence, the potential effects of wing upwash are captured at the propeller plane by all three aerodynamic methods, with differences in phase and magnitude arising for negative values of $\alpha_g$.

In a similar manner, fig. 3.23 looks at the pressure time history from the wing for different values of $\alpha_g$. Based on experience of the flow physics, it is expected that the signals should have some periodicity for every blade passage. The following key observations can be made:

1. The pressure time histories from LSM, VLM methods do not display periodicity for every blade passage. We would consider the signals from these methods to potentially have underlying numerical noise. As of now, not much has been investigated into the reason behind the possible sources numerical noise of these except for rudimentary guesswork on its origins due to the numerical singularities imposed by the usage of control points in LSM and VLM grids used for modeling the wing. There could also be potential issues in the internal PSU-WOPWOP loading file generator of CHARM. However, nothing more can be said without further investigation.
2. The pressure time signal from the LPM method displays consistent periodicity in its signal with every blade passage for all values of $\alpha_g$. This shows that the lifting panel method is able to capture the basic features of the unsteady flow physics.

A spectral plot for the noise signal from the wing could also help provide further insight. Figure 3.24 has frequency domain spectral plots, corresponding to the time-domain plots of fig. 3.23. These spectral plots give a different perspective of the complex features of the signals from the time-domain signals. The Sound Pressure Level (SPL) in decibels (dB) is shown on the Y-axis, while the corresponding Frequency (Hz) is plotted on the X-axis. The X-axis has ticks marked for every Blade Passage Frequency (BPF). For a 4-bladed propeller with a rotational speed of 3000 RPM, the BPF was calculated to be 200 Hz. Hence, the plots in fig. 3.24 are show the SPL from the first 10 blade passage frequencies. The following things were identified from the spectral plots for different aerodynamic methods:

1. For $\alpha_g = 0^\circ$, LSM has significantly under-predicted the SPL for all harmonics as compared to the levels predicted by panel methods. The VLM method is relatively closer to the SPL levels from the panel methods for the 1st and 2nd BPF values. However, it also fails to capture the higher harmonics like LSM.

2. For $\alpha_g = 5^\circ$ and $-5^\circ$, both VLM and LSM fail in predicting the levels for all the important blade passage frequencies.

3. For $\alpha_g = 10^\circ$ and $-10^\circ$, LSM seems to be able to predict the same SPL values for the 1st BPF. VLM seems to be able to also capture 2nd harmonic for $\alpha_g = -10^\circ$.

Figure 3.25 has the pressure time history of the 1st BPF. This was obtained by filtering all the higher harmonics. Overall, VLM seems to be the only method having a signal close enough to LPM method in terms of magnitude. There seems to be noticeable phase differences in signals for all three methods in their harmonics.

As of now, detailed investigation is needed along with experimental validation to comment more on which method is more suitable for accurate aeroacoustic prediction of propeller-wing interaction noise. The lifting panel method from CHARM, however, appears to be the best candidate aerodynamic model available within CHARM.
Figure 3.21: Comparison of Overall Sound Pressure Levels (OASPL).
Figure 3.22: Acoustic pressure time history from propeller (Pa) for various angles of attack and different aerodynamic methods (LSM, VLM, LPM).
(a) $\alpha_g = -10^\circ$

(b) $\alpha_g = 10^\circ$

(c) $\alpha_g = -5^\circ$

(d) $\alpha_g = 5^\circ$

(e) $\alpha_g = 0^\circ$

Figure 3.23: Acoustic pressure time history from wing (Pa) for various angles of attack and different aerodynamic methods (LSM, VLM, LPM).
\( \alpha_g = -10^\circ \)  
\( \alpha_g = 10^\circ \)  
\( \alpha_g = -5^\circ \)  
\( \alpha_g = 5^\circ \)  
\( \alpha_g = 0^\circ \)  

Figure 3.24: Frequency domain spectra from wing for various angles of attack and different aerodynamic methods (LSM, VLM, LPM).
Figure 3.25: Acoustic pressure time history from wing (Pa): 1st Blade Passage Frequency (BPF = 200 Hz) for various angles of attack and different aerodynamic methods (LSM, VLM, LPM).
3.3 Low Speed Propeller Noise: YO-3A Aircraft

The YO-3A aircraft (shown in fig. 3.26) was developed during the late 1960s and early 1970s and was considered acoustically undetectable around an altitude of 1000ft.

Figure 3.26: YO-3A surveillance airplane with the 3-bladed propeller

The noise reduction features and experimental noise measurements from overhead flyovers were discussed in detail in an extensive report written by Griffith and Revell [51]. Figure 3.27 shows the noise reduction features of the YO-3A as outlined by Griffith and Revell [51]. The key takeways from the noise reduction features shown in fig. 3.27 are:

Figure 3.27: YO-3A noise reduction features [51]
• **Low tip speed propeller**

Reducing the propeller tip speed (or the more appropriate nondimensional tip-Mach number $M_{tip}$) has been historically known to directly correlate with a reduction in rotational noise [12]. The designers of the YO-3A airplane used this to their advantage.

• **“Smooth” aerodynamic surface**

Griffith and Revell suggest that the design effort behind YO-3A was dedicated to reduce the likelihood of “strong” unsteady interactions between the wake of the propeller and the wing. Several other design features such as “flush mounted antennae” were also incorporated to reduce the likelihood of further aerodynamic interactions with airframe components and freestream. The aeroacoustic theory (section 2.1) suggests that a reduction of the contribution of the unsteady loading noise component (Term 1 in eq. (3.1)) will result in lower noise.

The incorporation of the results from the YO-3A study in this research effort was to understand and compare the efficacy of our prediction tools to predict acoustics of low-speed propellers. The report also demonstrates the importance of noise levels generated from propeller-wing interactions and their contribution to overall noise as the tip-Mach number of the propeller is reduced to the range of 0.18 - 0.3. Typical tip-Mach numbers for helicopters and general aviation tend to be higher than 0.3. Based on a review of the extensive reports by Hubbard [10] and Metzger [12], a rotational tip-Mach number range of 0.5 - 0.8 is typical.

An extensive series of flight tests were conducted in 1970, where the noise from the aircraft was measured in flyovers as the aircraft was overhead (fig. 3.28). For each acoustic measurement, the RPM was varied while the forward flight speed of the airplane was kept constant and the engine shaft power was measured.
Figure 3.29 shows the rather anomalous trends reported by Griffith and Revell after the processing of results from the YO-3A flight test. An anomalous noise bucket can be observed in the experimental data, which shows the variation of fundamental rotational harmonic noise from the aircraft with respect to the operating propeller RPM. (The data is considered anomalous because it did not agree with predictions or static measurements available at the time and the noise levels increased for the lowest RPM value.)

The bucket was originally investigated by performing axisymmetric calculations, which showed a small bucket in the fundamental rotational harmonic with an increase in RPM from 480 to 780 (corresponding to $M_{tip} = 0.18$ and $M_{tip} = 0.3$). With inclusion of Non-Uniform Loading (NUL) effects (referred to as “unsteady loading” in this thesis), the magnitude of noise and the bucket is shown to increase to within 2 - 5 dB of the experimental measurements, as per the theoretical calculations in the report. Inclusion of interactions between the propeller wake and the wing reduced the differences between the theoretical and experimental values to less than 1 dB.

In this work, calculations were recreated in the same order using three toolchains involving XROTOR, Quasisteady Blade Element Theory (BET or QBET) and CHARM. Figure 3.30a shows the lifting surface representation of the propeller blade in CHARM, while Figure 3.30b shows the CVC wake trailed by the propeller in axisymmetric flow. As described by Griffith and Revell, the propeller was trimmed by varying the pitch to give a constant thrust of 220 lb (978 N) for all RPMs.
Figure 3.29: Trends in propeller and aircraft noise as reported by Griffith and Revell [51]

Figure 3.31 shows the schematic and convention for the isolated propeller cases executed for calculating noise results for the YO-3A propeller. Figure 3.32 shows the results from the axisymmetric noise calcuations of the toolchains described earlier. While the noise bucket from axisymmetric calculations in the original report was not captured, the results from each of the toolchains were found to be within 2 - 3 dB. Griffith and Revell attribute the small bucket to chordwise effects. Such effects were not observed in this work even with inclusion of additional chordwise control points representing the YO-3A blade lifting surface in CHARM.
Figure 3.30: CHARM’s CVC wake representation for isolated YO-3A propeller

Figure 3.31: YO-3A isolated propeller schematic and setup
Figure 3.32: Comparison of fundamental rotational SPL (dB) using XROTOR, BET (QBET), and CHARM with Griffith and Revell [51] predictions (axisymmetric case).

Figure 3.33 shows the noise levels computed by the toolchains when a shaft angle of attack ($\alpha_{AOA} = 2.88^\circ$ in fig. 3.31)—relative to level flight—is included, as reported by Griffith and Revell [51]. It can clearly be seen that the increase in noise levels don’t match the levels marked as NUL (non-uniform loading) in fig. 3.29. In an effort to understand this discrepancy, consideration propeller-wing interference is needed. In this work the CHARM toolchain was validated for propeller-wing interactions by considering experiments of Dunsby et al. [55, 56], which provides loading information on a wing operating in the wake of a propeller. This validation was included in section 3.2.1 and the following subsections.
3.3.1 Propeller-Wing noise: YO-3A Aircraft

Based on the results from the Dunsby case, propeller-wing interaction noise was evaluated for the YO-3A aircraft using CHARM’s Vortex Lattice Method (VLM) for the unsteady aerodynamics calculations.

The VLM grid for the YO-3A wing shown in fig. 3.34 has 120 spanwise and 4 chordwise control points. The propeller-wing system was operated with a shaft angle of attack in a manner similar to fig. 3.31, with $\alpha_{AOA} = 2.88^\circ$.

Figure 3.35 shows the noise from the propeller operating in a shaft angle of attack of 2.88° and presence of upwash from the wing. The Quasi-steady BET
code used upwash inflow information on the propeller plane from CHARM for consistency. While some increase in noise was observed in comparison to fig. 3.33, the levels are still significantly lower than the corresponding levels predicted by Griffith and Revell’s “Theory only including NUL effect”.

The noise from the unsteady loading on the wing and the combined propeller+wing noise is included in fig. 3.36. Two important features from fig. 3.36 merit attention:

- The noise generated by the wing, with the CHARM wing loading, is higher than the propeller noise by 2-7 dB. This is contradictory to Griffith and Revell’s [51] predictions, for which there is a roughly 2 dB increase with the addition of the wing noise.
Figure 3.35: Comparison of fundamental rotational SPL (dB) using QBET and CHARM with Griffith and Revell [51] predictions and experimental data.

- On comparing the noise in [51] including propeller wake/wing interaction the NUL noise results, it can be inferred that the noise from the wing and the propeller are comparable. The present predictions with CHARM/PSU-WOPWOP the wing noise is dominant, and the combined propeller and wing are lower than [51] predictions and the flight test for 480 and 780 RPMs and higher levels than [51] and the experiment for 600 and 720 RPM. This trend seems erratic and suggests that there may be implementation problems with the CHARM/PSU-WOPWOP noise predictions, which should be resolved in future work.
In order to better understand, the erratic trend of noise between 480 and 780 RPM when the wing is included, the acoustic pressure time histories from the propeller and the wing at these RPM’s were examined. The same “noisiness” (numerically erratic signal) as observed in the acoustic pressure time history of Dunsby wing’s VLM method (fig. 3.23) can be seen in fig. 3.37a and fig. 3.37c. The “higher” harmonics were filtered out so that only the contribution from the fundamental rotational harmonic is visible in the acoustic pressure time history, which helps clarify how the propeller and wing acoustic signals interact. In fig. 3.37b, there is apparently destructive interference between the acoustic signals of the propeller and the wing while the propeller rotates at 480 RPM. In fig. 3.37d, constructive interference between the acoustic signals of the propeller and the wing while the propeller rotates at 780 RPM occurs. This may explain why
the propeller+wing signal is higher than the wing alone for the CHARM/PSU-WOPWOP predictions at 780 RPM and lower than the wing along for 480 RPM. Nevertheless, there are still significant questions about the current predictions (and possibly the Griffith and Revell predictions too) and this type of prediction will require more analysis, corrections and better experimental data before it can be considered validated.

Figure 3.37: Acoustic pressure time histories from propeller and wing at 480 and 780 RPMs.
Chapter 4  
Conclusions and Future Work

This thesis provides a broad overview of eVTOL aircraft noise. Based on historical understanding of noise from helicopters and general aviation aircraft, rotating components were identified as one of the major noise sources at the eVTOL aircraft configuration level. Another major noise source was hypothesized to be the aerodynamic interactions between rotating and non-rotating components. The vast design space of eVTOL aircraft offers a plethora of such sources. Hence, the current work chose to narrow its focus on noise from propellers and propeller-wing interactions with the propeller in front of the wing.

Based on the source terms in general aeroacoustic theory, unsteady aerodynamic loading has been hypothesized as the dominant contributor to the total noise generated. Unsteady aerodynamic loading can result in discrete and broadband noise signatures. Broadband noise while potentially important is beyond the scope of this thesis.

A major challenge in resolving the unsteady aerodynamics of eVTOL aircraft lies in the computational costs. Hence, high-fidelity aerodynamic methods like Computational Fluid Dynamics (CFD) are an unattractive option for conceptual and preliminary design and analysis tools. As a result, this thesis explores the capabilities of quick low and mid-fidelity aerodynamic methods to resolve the aerodynamics and noise generated from propellers and propeller-wing interactions.
4.1 Aeroacoustic Prediction Capabilities of Low-Fidelity Propeller Aerodynamics Tools

The choice of low fidelity aerodynamic tools for analyzing propeller air loads in this work is XROTOR and traditional Blade Element Theory (BET) models originally developed by Glauert and Betz. Both of these aerodynamic tools were originally developed for propellers operating in axisymmetric flow conditions. Appropriate modifications were made to enable the use of these codes for propellers operating in non-axial flow conditions such as shaft angle of attack.

Previous work at Penn State led to the development of a simple quasi-steady model which leveraged XROTOR’s capabilities. However this model had ignored major flow phenomenon and hence a quasi-steady adaptation of classical BET model was also implemented. Both of these tools were then used to analyze the noise from an isolated low tip-speed propeller operating in a range of different shaft angle of attacks. Comparison of noise results were made with respect to the mid-fidelity comprehensive free wake tool CHARM. It served as the baseline tool based on previous work demonstrating CHARM’s capabilities to accurately predict noise from isolated rotors and propellers. The following conclusions are noteworthy:

1. The XROTOR toolchain is capable of leveraging XROTOR’s powerful inverse blade design capabilities. The menu-driven routines of XROTOR offer great flexibility in propeller design and integration with multivariable design optimization tools. The coupling of XROTOR with PSU-WOPWOP allows geometry and loading data to be exported for noise computations. However XROTOR can only design and analyze propellers operating in axisymmetric flow conditions, spurring the development of a simple quasi-steady model using XROTOR. The quasi-steady approximation developed for XROTOR has substantial deficiencies including loss in capturing significant reverse flow regions.

For a propeller operating under a constant shaft angle of attack, the XROTOR Quasisteady model (XROTOR-QS) captures the trend in time-averaged metrics such as OASPL, but can have serious errors with increasing dominance of non-uniform inflow characteristics. Hence, it should only be used for qualitative judgement even when used for propeller operating in low shaft
angle of attack values.

2. The Quasi-steady BET (QBET) code was developed to better capture the flow regions for a propeller operating at a constant shaft angle of attack (fig. 3.1). The code accepts blade geometry inputs (distribution of radial station, chord, twist and thickness) in the same format as XROTOR blade files, so as to maintain ease of transitioning of tools from the XROTOR toolchain. The code is also capable of handling more complex inflow, as long as the velocity distribution at the propeller disk plane is provided (eqs. (2.20) and (2.21)). The inflow due to the upwash from a wing was included in the YO-3A calculations, showing relatively good agreement with the results from CHARM (fig. 3.35).

In comparison with CHARM, the code predicted the same OASPL values for a range of shaft angles of attack (fig. 3.1). Hence, the tool can be safely used to evaluate propeller designs in terms of integrated noise metrics. However, upon closer inspection of the acoustic pressure time history, phase differences in signals were encountered for negative values of $\alpha_{AOA}$ (fig. 3.3). The origin of the phase differences were narrowed down to the unsteady loading source term, i.e., Term 1 of eq. (3.1) (see fig. 3.5b). The phase lag in acoustic pressure time history will be an important factor when using the QBET tool to evaluate acoustic interactions between multiple propellers. Further investigation will be needed to understand and resolve the difference in phase between the loading noise signals generated from QBET and CHARM for a propeller operating at a constant shaft angle of attack.

3. A noteworthy acoustic phenomenon was observed in the detailed analysis of the loading noise acoustic signal for a propeller operating in axisymmetric flow conditions. The unsteady loading term (Term 1 of eq. (3.1)) was found to be dominant when compared to the steady loading term (Term 2). Since blade loads are steady for a propeller in axisymmetric flow, a closer examination of Term 1 suggests that the propeller angular velocity (eq. (3.6)) also has a contribution to the acoustic term encompassing unsteady loading. Normally propeller rotational velocity, ergo the tip-Mach number, is considered important for thickness noise, but this analysis shows that it could also be important for loading noise.
4.2 Aerodynamic Validation of CHARM for Propeller-Wing Interactions

Propeller-wing interactions in the tractor configuration were studied using CHARM, where wings were modeled using the Lifting Surface Method (LSM), Vortex Lattice Method (VLM)\(^1\) and the Lifting Panel Method (LPM). All three aerodynamic modeling methods provided by CHARM were used, in order of increasing fidelity and computational cost: LSM, VLM, and LPM, respectively. In order to understand the capabilities of each method to accurately capture the interactional aerodynamics, the Dunsby et al. wind tunnel dataset was used.

The results of the validation can be summarized as follows:

1. The time-averaged overall wing lift coefficient \(C_{L-avg}\) and lift coefficient vs wing span from CHARM were compared with those available in the experiment (figs. 3.16 and 3.18). The comparison of CHARM results with experimental data can be summarized in table 4.1. The “✓” symbol indicates satisfactory performance, while the × indicates serious differences in results.

<table>
<thead>
<tr>
<th>Experimental quantity (time-averaged)</th>
<th>Aerodynamic method</th>
<th>LSM</th>
<th>VLM</th>
<th>LPM</th>
</tr>
</thead>
<tbody>
<tr>
<td>(C_{L-avg})</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>(C_l) vs span</td>
<td>×</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
</tbody>
</table>

Hence, it is concluded that:

- LSM can be used for rapid preliminary design work, where \(C_{L-avg}\) is the focus.
- For detailed design work, where the accuracy of spanwise distribution of wing loading is important, the VLM and LPM perform reasonably well, with deficiencies discussed in section 3.2.2.2.

\(^1\)VLM is just an extension of LSM, by inclusion of more than one chordwise point.
2. Despite the absence of noise measurements from the Dunsby propeller-wing system, noise was computed using PSU-WOPWOP and individual contributions from the propeller and the wing were studied for the three aerodynamic methods implemented (section 3.2.2.3).

(a) All three methods predict the overwhelming dominance of wing noise levels over the propeller. fig. 3.21.

<table>
<thead>
<tr>
<th>Aerodynamic method</th>
<th>LSM</th>
<th>VLM</th>
<th>LPM</th>
</tr>
</thead>
<tbody>
<tr>
<td>$-10^\circ$</td>
<td>76.6</td>
<td>80.9</td>
<td>90</td>
</tr>
<tr>
<td>$-5^\circ$</td>
<td>74.8</td>
<td>80.6</td>
<td>89.7</td>
</tr>
<tr>
<td>$0^\circ$</td>
<td>71.2</td>
<td>79.2</td>
<td>84.5</td>
</tr>
<tr>
<td>$5^\circ$</td>
<td>75.7</td>
<td>82.4</td>
<td>87.8</td>
</tr>
<tr>
<td>$10^\circ$</td>
<td>75.7</td>
<td>82.2</td>
<td>85.5</td>
</tr>
</tbody>
</table>

Table 4.2: Summary of OASPL (dB) values generated by the wing

However the OAPSL levels predicted by each method have significant variation. LSM predicts the lowest wing OASPL, while panel methods predict significantly higher values.

Looking at the acoustic pressure time history (fig. 3.23), only the LPM predicts the expected periodicity with every blade passage. Pressure signals from LSM and VLM were judged to have numerical noise based on the potentially non-physical features in them. The reasons behind the numerical noise is currently unknown and further investigation is required.

The acoustic spectrum (fig. 3.24) also shows significant differences, with the panel methods predicting dominant sound pressure levels (SPL) for the first eight multiples of the blade passage frequency (BPF).

Until validation of the LSM and VLM data and/or data coupling with PSU-WOPWOP, the LPM method from CHARM is currently considered a reasonable starting point for analyzing the acoustics of propeller-wing interactions.

(b) The effect of wing upwash on the acoustics of the propeller in front of the wing was also studied (fig. 3.22). A comparison with noise signals from isolated propellers operating in analogous conditions (same freestream
velocity, geometric angle of attack $\alpha_{g}$) revealed that the effect of upwash was negligible from $\alpha_{g} = 0$ to $10^\circ$. For negative values of shaft AOA however the wing upwash field seems to have more pronounced effects on the noise signals.

4.3 Reexamination of the Aeroacoustic Analysis of the YO-3A Aircraft

In section 3.3, the acoustics of the low noise YO-3A aircraft was studied using the report by Griffith and Revell [51]. Key historical lessons were revisited, and a focus on noise from low tip-Mach number propellers was renewed. The value of the report lies in the unique flight-test noise data from the YO-3A aircraft. Multiple flight test measurements were conducted for the single prop aircraft, with the propeller operating in constant thrust and 4 different RPMs with a tip-Mach number range of 0.18 to 0.3. A noise bucket observed in the experimental flight test results was hypothesized to be a result of “Non-Uniform Loading” \(^2\) effects acting on the low tip-speed YO-3A propeller. Griffith and Revell also present some results from the computational investigation that analyzed the noise bucket observed in experimental data.

(i) For the 3-bladed YO-3A propeller, Griffith and Revell presented theoretical noise calculations, assuming the propeller is operating in axisymmetric flow. XROTOR, QBET and CHARM were used to compute noise for the same flow conditions. The noise results from these were found to be within a 1 - 4 dB range of values reported by Griffith et al. However a noise bucket predicted in the report was not captured by any of the three aerodynamic tools used for this work.

(ii) The report by Griffith and Revell mention that the propeller operates in a constant shaft angle of attack in the presence of a wing upwash field. With the inclusion of aforementioned unsteady aerodynamic effects on the propeller, and the contribution of noise from the wing using CHARM, the

\(^2\)“Non-Uniform Loading” is currently construed to be a result of “Unsteady aerodynamic” effects.
comparison with experimental data was made. Figure 3.36 showed significant under-prediction of propeller noise levels by CHARM in comparison to the experimental levels and theoretical predictions done in the report by Griffith and Revell. It is suspected that these differences could have arisen due to certain low-Reynolds effects potentially not being captured, along with the exclusion of broadband noise in the calculations, but further work is needed to confirm this.

4.4 Future Work

Based on the preliminary exploration of eVTOL noise done in this thesis, several opportunities for future work remain. The analysis of the results generated by the tools used in this thesis, have revealed several avenues for improvements and future steps in validation.

4.4.1 Low-Fidelity Propeller Noise Tools

Over the course of the thesis the aeroacoustic predictions from low-fidelity propeller aerodynamic methods were evaluated and the following paths to build upon remain:

1. Based on the performance of XROTOR and QBET methods in capturing propeller noise trends qualitatively and quantitatively, both of these tools can be potentially used in conceptual and preliminary design of quiet propellers, contribute to toolchains that enable quick evaluation of eVTOL configuration noise.

2. The deficiencies of Quasi-steady BET model can be alleviated by improving the implementation of Prandtl’s tip-loss factor. Another deficiency identified in the QBET code was determined to be the implementation of Newton-Rhapson solver used to numerically solve for the axial and tangential induction factors. The code structure needs to be revised in order to make the solver more robust for non-axisymmetric flow.

3. Another major improvement can be made by inclusion of airfoil unsteady effects. As explained by Sears [58], the response of a two-dimensional airfoil to change in inflow is not instantaneous and a downwash is induced at the
leading edge by the bound vorticity. Taking the temporal response into account can be shown to increase the fidelity of the QBET model. Nando van Arnhem et al. summarize this approach in a recent paper [59].

4. eVTOL aircraft are expected to have rotors and propellers operating with time-varying rotation speeds and an extension to Quasi-steady BET methods are needed to predict propeller loading with variations in angular velocity in order to maintain the advantages in computational cost provided by this method. Some preliminary work has been done to show the impact of time-varying propeller rotation speed on noise [60].

4.4.2 Analysis of Propeller-Wing Interactions

The mid-fidelity free-wake tool CHARM was used to study propeller-wing interactions. Comparison with time-averaged data revealed free-wake methods capable of capturing time-integrated performance metrics such as overall wing lift coefficient and the distribution of lift across the span of the wing leads to the following immediate future paths:

1. In the current work, all free-wake calculations were made for one propeller revolution only. Hence the impact of propeller wake unsteadiness on wing is only from the near-wake region. The impact of far-wake region needs to be evaluated by conducting transient free-wake calculations over several propeller revolutions.

2. Comparison with the Dunsby experiments revealed overprediction of spanwise lift distribution with increasing geometric angle of attacks. The overprediction occurred in the region of propwash. A lack of viscous wake dissipation effects is hypothesized to be an important factor behind the overprediction of lift. Future work with CHARM could involve inclusion of viscous/turbulent diffusion of vortex cores to better capture the effects of propeller-wing wake interaction. For other three dimensional effects like stall, swirl, boundary layer and transition across the wing span, a CFD approach could help further the understanding of the complex aerodynamics.

3. Analysis of the aerodynamic mechanisms behind propeller-wing noise needs
to be done. Identification of the aerodynamic source terms responsible for wing noise is needed.

4. YO-3A Aircraft noise: The YO-3A aircraft noise still remains a topic of active investigation. Current aerodynamic and acoustic tools were shown to not be able to capture the trends presented in the report by Griffith and Revell. Broadband noise and low-Reynolds number aerodynamic phenomenon remain high priorities for future investigations. The aircraft fuselage was also ignored in current aerodynamic and noise calculations. These could play an important role. Griffith and Revell reported the flight test noise levels only for the fundamental rotational harmonic. Analyzing YO-3A flight test data with a larger range of frequency content could also help in understanding its unique noise characteristics.

4.4.3 Future Work in eVTOL Configuration Noise

The complex aeroacoustic environment of a variety of eVTOL aircraft configurations was divided into a combination of rotating and non-rotating source components. The aerodynamic interaction between these source components was also hypothesized to be a major source of noise. While dependent on the design and mode of operation, the contribution from these components to total noise still remains unexplored. This thesis focused only on the deterministic noise components from an isolated propeller and propeller-wing in tractor mode of propulsion. Significant effort needs to be undertaken into further investigation of:

1. Broadband Noise: Current work did not consider broadband noise generated from different eVTOL components. Considering the prevalence of unsteady interactions between wakes of rotating and non-rotating components, broadband noise is expected to have significant importance, although the relevant source mechanisms behind it remains unclear. Future work would potentially involve development of physics based broadband noise models or deployment of higher-fidelity methods to resolve stochastic surface loading.

2. Lack of relevant data: There is dire shortage of appropriate unsteady aerodynamic data and noise measurements for propeller-wing interactions. Similar shortages can also be seen for other kinds of interactions. It is hoped that
better datasets, which would include unsteady aerodynamic and acoustic data, will be available in the future.

3. Several other aerodynamic interactions exist between the different components of eVTOL aircraft, but their magnitude and nature currently remains unknown. Potential future candidates include interactions between lifting rotors and propellers, fuselage and nacelles, etc.
References


