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UNIVERSITY OF CALIFORNIA,
IRVINE

A Systems Methodology for Predicting Community Noise from Propulsor Design and
Flight Procedures of DEP Aerial Vehicles

DISSERTATION

submitted in partial satisfaction of the requirements
for the degree of

DOCTOR OF PHILOSOPHY

in Mechanical and Aerospace Engineering

by

Seraphin Ho-Juin Yeung

Dissertation Committee:
Assistant Professor Jacqueline Huynh, Chair
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V. R. Pellerito, N. H. Yeung, J. L. Huynh, and R. J. Hansman. Impact of Flight Trajectory Design on Performance and Noise for AAM Aircraft. In AIAA AVIATION Forum and AS-

CEND 2024, number 2024-4076 in AIAA Paper, Reston, VA, July 2024. American Institute of Aeronautics and Astronautics.

S. H. Yeung and J. L. Huynh. Impact of Design and Operation of Distributed Electric Propulsors to Aircraft Noise. In AIAA SCITECH 2026 Forum, number 2026-1482 in AIAA Paper, Reston, VA, January 2026. American Institute of Aeronautics and Astronautics.

V. Gonzalez, S. H. Yeung, J. L. Huynh, and R. J. Hansman. Impact of Takeoff Trajectory Design on Performance and Noise for Advanced Air Mobility Aircraft. *Journal of Aircraft*, 63(2):793–808, March 2026.

S. H. Yeung and J. L. Huynh. Impact of Design and Operation of Distributed Propulsors to Aircraft Noise Emission. Manuscript submitted to the *Journal of Aircraft*, 2026.

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ABSTRACT OF THE DISSERTATION

A Systems Methodology for Predicting Community Noise from Propulsor Design
and Flight Procedures of DEP Aerial Vehicles

By

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Doctor of Philosophy in Mechanical and Aerospace Engineering

University of California, Irvine, 2026

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Advances in distributed electric propulsion (DEP) have enabled novel propulsor architectures and operating capabilities for short takeoff and landing (STOL) and vertical takeoff and landing (VTOL) aircraft. The noise produced by these architectures is strongly dependent on propulsor design and operating state, while their expanded operating capabilities enable unconventional flight profiles within relatively small ground footprints. Together, these characteristics cause community noise to depend on both propulsor source noise and the aircraft operating states encountered throughout the flight profile. The potential operation of Advanced Air Mobility (AAM) aircraft near populated areas further motivates an integrated approach to propulsor design, aircraft performance, and source- and community-noise assessment.

The objective of this thesis is to develop a methodology for designing and evaluating propulsors and DEP architectures and to examine how propulsor geometry, arrangement, operation, and flight path affect aircraft performance, source noise, and community noise.

Propulsors are designed from limited top-level vehicle geometry and performance requirements using blade element momentum theory. Propulsor performance is coupled with physics-based vehicle models to generate representative flight profiles for DEP STOL and VTOL aircraft. Tonal and broadband source noise are modeled using Ffowcs Williams–Hawkings and Brooks, Pope, and Marcolini methods, respectively. The resulting source-noise hemispheres are propagated along the flight profiles to evaluate the coupled effects of

propulsor design and aircraft operation on community noise. The methodology is further integrated with motor and nacelle sizing and aircraft performance models to evaluate DEP arrangements for a representative regional turboprop aircraft.

The results demonstrate strong coupling between propulsor design, performance, and noise. Propulsor geometry and operation significantly affect source noise and required power, with lower tip speeds generally reducing noise at the expense of increased torque or power for some designs. Changes in DEP arrangement further alter lift augmentation, required thrust, power, and noise, demonstrating that propulsor count cannot be considered independently of propulsor design and operation. Application to representative STOL and VTOL aircraft demonstrates that community noise depends on both source-noise magnitude and flight trajectory, as changes in propulsor operation also affect aircraft altitude and the duration of high-noise flight segments. Consequently, reductions in source noise do not necessarily produce equivalent reductions in community noise.

At the aircraft-system level, integration of motor and nacelle sizing, aircraft performance, and operating constraints shifts the preferred DEP design space, demonstrating that propulsor performance alone is insufficient for aircraft-level design selection. For the regional turboprop application, low-noise DEP arrangements overlap with designs having low fuel consumption, while lower propulsor tip speeds enable higher-speed operation than conventional turboprop propulsors. Engine core noise also becomes increasingly important as DEP propulsor noise is reduced at lower tip speeds. Overall, the methodology provides a framework for evaluating the coupled effects of propulsor design and arrangement, aircraft performance and operation, source noise, and community noise during preliminary aircraft design.

Chapter 1

Introduction

1.1 Electric Multi-Propulsor Aviation

Short takeoff and landing (STOL) and vertical takeoff and landing (VTOL) aircraft present a rapidly developing sector in the general aviation market [8]. Though not a new concept, advances in electrification and distributed propulsion, along with the development of infrastructure and demand, have made electric multi-propulsor aircraft a popular focus [87]. Figure 1.1 illustrates three examples of AAM vehicle architectures. Concepts such as AAM air taxis are often promoted as ways to ease congestion in population centers. Other applications include delivery services to urban and suburban areas [91]. Nearly all these scenarios involve operating propellers or rotors in close proximity to areas of human habitation [91].

Source noise is local to the aircraft. It is represented as a set of discrete noise observer points around the vehicle, often in a spherical shape centered about an origin point. This source noise hemisphere can be propagated to represent noise at a different location, such as an undertrack point below the aircraft. Community noise is produced when the source noise hemisphere moves and noise is propagated to a series of points over time. Community noise is evaluated over a larger area on the ground, represents the noise characteristics of a vehicle during dynamic operation, and is a function of distance and time. Figure 1.2 shows a hypothetical aircraft source hemisphere being flown on a flight path over a grid of community noise observers. Flight path parameters, such as altitude, attitude, and speed, play a major role in the noise characteristics produced.

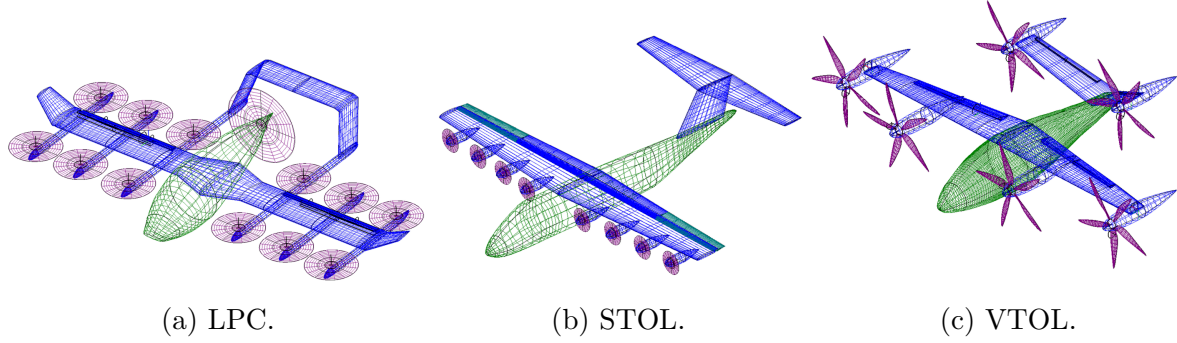


Figure 1.1: Examples of original AAM multi-propulsor vehicle architectures.

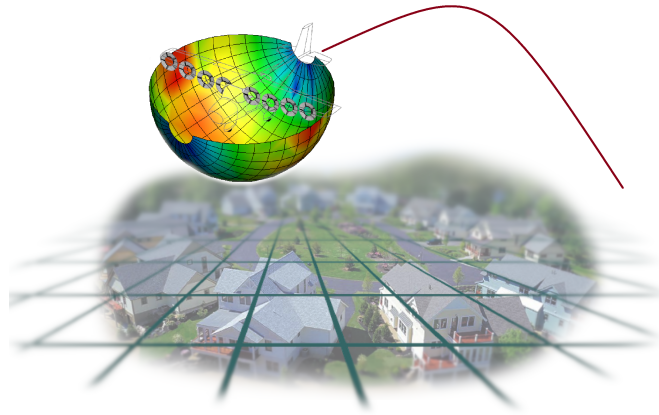


Figure 1.2: Example aircraft source-noise hemisphere propagated along a flight path over a grid of community observers.

The unique architectures and expanded operating envelopes of multi-propulsor aircraft create challenges for noise prediction using conventional tools developed for fixed-wing aircraft and helicopters. Propulsor geometry and operation affect not only source noise but also aircraft performance and flight conditions. For distributed-propulsion aircraft, these effects are further coupled through propulsor arrangement, which can alter aerodynamic performance and the operating requirements of individual propulsors. Propulsors and flight profiles must therefore be analyzed in tandem to accurately represent community noise, since propulsive characteristics influence how an aircraft is flown. A robust noise analysis framework must reflect the vehicle’s operational environment in order to capture these component- and

system-level changes [25].

There is significant interest in designing and operating both propulsors and aircraft in ways that reduce undesirable noise frequencies and amplitudes [1], [2], [111]. Designers require methods that can rapidly evaluate the sensitivities of propulsor geometry, arrangement, and operation, while aircraft operation and flight profiles must also be considered when evaluating the resulting community noise. Community noise remains an important factor in the social acceptance of AAM aircraft [64]. However, as discussed in Chapter 2, many existing methodologies are limited to specific vehicle architectures, existing propulsor geometries, or pre-generated noise data and therefore have limited capability to evaluate new combinations of propulsor design, operation, and arrangement. Other approaches focus primarily on either source noise or community noise without fully coupling changes in propulsor operation to the resulting aircraft performance and flight trajectory.

These limitations motivate the development of a methodology that couples propulsor design and performance with aircraft operation, source-noise modeling, and community-noise propagation. The framework developed in this thesis generates and evaluates propulsor designs from top-level aircraft and performance requirements and determines their tonal and broadband source-noise characteristics over representative operating conditions. Propulsor performance is coupled with aircraft characteristics to generate representative flight profiles, allowing source noise to be propagated according to the operating state and position of the aircraft to evaluate community noise. Additional models account for system-level effects including propulsor arrangement, lift augmentation, motor and nacelle sizing, aircraft weight, and drag, allowing changes in these parameters to modify propulsor performance requirements and the resulting aircraft operation. Together, these capabilities allow the methodology to evaluate component-level sensitivities in propulsor geometry and operation, vehicle-level interactions among propulsor design, aircraft performance, flight operation, and community noise, and system-level tradeoffs among propulsor efficiency, fuel consumption,

noise emissions, and aircraft operating capabilities.

1.2 Thesis Objectives

The objective of this thesis is to:

1. Develop a methodology capable of designing and evaluating propulsors and distributed-propulsion architectures, modeling tonal and broadband source noise, generating representative flight profiles, and evaluating the resulting community noise.
2. Analyze how parametric changes in propulsor geometry, operation, arrangement, and flight path affect propulsor performance, source noise, and community noise for representative DEP STOL and VTOL aircraft architectures.
3. Assess how propulsor design and aircraft operation interact to influence community noise by coupling source-noise modeling with vehicle performance, flight-profile generation, and noise propagation.
4. Demonstrate how the methodology can be integrated into a broader multidisciplinary aircraft design framework by evaluating how propulsor arrangement, motor and nacelle sizing, aircraft performance constraints, and operating requirements affect efficiency, fuel burn, noise emissions, and expanded operating capabilities for a representative regional turboprop aircraft.

1.3 Thesis Outline

Chapter 2 presents a literature survey of existing tools, methodologies, and studies for designing and predicting multi-propulsor source noise for STOL/VTOL vehicles and evaluating their overall community noise. Additional sources explore the performance evaluation and design of distributed propulsion arrangements for a blown-flap lift-augmented STOL vehicle.

Chapter 3 presents the methodology, its components, and cases used to validate its various capabilities against experimental results.

Chapter 4 applies the methodology to evaluate how propulsor geometry, operation, and distributed-propulsion arrangement affect source noise and performance. A single-propulsor sensitivity study examines variations in blade count, propulsor radius, tip speed, and operating condition for propulsors designed to meet common thrust and airspeed requirements. The chapter then extends the analysis to a representative blown-flap STOL vehicle to evaluate how changes in propulsor count and arrangement affect propulsor noise and vehicle performance while maintaining baseline takeoff requirements.

Chapter 5 applies the methodology to evaluate how aircraft operation and flight profile design affect source and community noise for representative lift-plus-cruise VTOL and blown-flap STOL vehicles. Propulsor designs are developed from published vehicle and operating characteristics, representative departure profiles are generated to identify feasible operating states, and source noise hemispheres are created and propagated along these trajectories to obtain community noise contours. The resulting analyses demonstrate how changes in propulsor operation and flight profile interact to affect community noise for vehicles with distinct operating characteristics.

Chapter 6 applies the methodology to the design and evaluation of distributed electric propulsion arrangements for a representative regional turboprop aircraft. Propulsor geometry, operation, and count are varied while accounting for changes in motor and nacelle weight, drag, and resulting aircraft performance requirements. The resulting DEP configurations are evaluated with respect to propulsor efficiency, power requirements, fuel burn, source and community noise, and high-speed cruise performance to demonstrate how the methodology can be integrated with additional aircraft subsystems in a multidisciplinary design framework.

Chapter 7 concludes the thesis, summarizes key contributions, and discusses potential further applications of the methodology.

Chapter 2

Literature Survey

Aircraft noise, in the context of this thesis, is described as unwanted sound that causes nuisance and disturbance. Noise can be produced from flow about the airframe and control surfaces or by propulsion in the form of rotor, propeller, and engine noise [95]. The amplitude of noise is commonly measured as a Sound Pressure Level (SPL) and is expressed on a logarithmic decibel scale (dB) [35]. One metric used in this study is Sound Exposure Level (SEL), which measures sound exposure relative to a reference value [4]. The human perception of noise, however, depends on frequency, with certain frequency ranges perceived as louder than others. When modeling noise, it is often useful to apply a weighted scale that rolls off or boosts amplitude at certain frequencies. This study uses a common metric called A-weighted Overall Sound Pressure Level (OASPL) to allow results to be readily compared with other works [56].

In this work, propulsor describes a mechanical device that generates force and includes both propellers and rotors [33]. Propellers primarily produce thrust for the forward motion of a vehicle, typically operate with their disk planes oriented approximately vertically, and are associated with fixed-wing aircraft. This is distinct from rotors, which are usually operated horizontally, are associated with helicopters or rotorcraft, and directly produce both lift and thrust for a vehicle. Rotors are often larger and have more disk area than propellers for the same vehicle weight. Both types of propulsors control thrust through a combination of RPM variation and changes in blade pitch angle during operation.

2.1 Noise Characteristics of Multi-Propulsor Vehicles

The characteristics of propulsor noise depend strongly on vehicle type. Helicopters and multi-rotors differ in rotor scales, with helicopters having higher chord Reynolds numbers and higher tip speeds that approach sonic velocities [50]. Helicopters and conventional fixed-wing aircraft are typically characterized by more dominant thickness and loading noise components and greater blade-vortex interaction noise than multi-rotors [30]. Helicopter blades also tend to be larger than multi-rotor blades, resulting in greater blade span and increased trailing-edge noise [69]. These qualities result in multi-propulsor vehicles producing broadband noise over a more perceptible range of audible frequencies, in contrast to helicopter and conventional aircraft noise, which is usually dominated by tonal noise at lower frequencies [102]. Though the majority of noise from rotorcraft comes from propulsors, certain AAM designs, such as blown-flap STOL vehicles, have significant amounts of airframe noise from interactions between propeller wakes and flaps. During flight conditions, such as approach and departure, blown-flap STOL vehicles can produce lift coefficients many times larger than those of conventional fixed-wing aircraft due to the increased flow velocity over high-lift devices [29]. In these scenarios, airframe noise can be equal to or greater in magnitude than propulsor noise [108]. This necessitates the consideration of airframe components during the modeling of STOL vehicles.

2.1.1 Components of Propulsor Noise

When performing noise modeling and analysis, it is often useful to break aerodynamic noise into its constituent broadband and tonal components. A better understanding of noise components can aid in determining their mechanisms and sources [16]. Figure 2.1 describes various types of noise created by propulsors. Tonal noise is periodic and is associated with harmonics of the blade passage frequency (blade number $\times$ rotational frequency). Tonal noise is composed primarily of loading and thickness noise. Loading noise is a function of torque and thrust and is caused by the chordwise pressure distribution along the propulsor blades.

Thickness noise is caused by mass displacement of blade sections as they rotate through the air [42].

Broadband noise consists mainly of airfoil self-noise and blade wake/vortex interaction noise [69]. Considerable focus has been devoted to studying and predicting the former. Blade self-noise is composed of the following five components: turbulent boundary layer trailing-edge noise, laminar boundary layer vortex-shedding noise, separation-stall noise, trailing-edge bluntness vortex-shedding noise, and tip-vortex formation noise. For airfoil sections operating at high Reynolds numbers, turbulent boundary layer trailing-edge noise is created by boundary layer interaction along the trailing edge. Conversely, at low Reynolds numbers, laminar boundary layer shedding noise is created by unstable vortices flowing over the trailing edge. At non-zero angles of attack, flow can separate from the suction side of the airfoil section and cause separation stall noise from shed turbulent vortices. For airfoils with blunt trailing edges, trailing-edge bluntness vortex shedding noise is created from the separated flow in this small aft region. Finally, broadband self-noise is created from highly turbulent flow during the formation of tip vortices [18].

Broadband noise can also occur from blade wake/vortex interaction if propellers are operated in disturbed flow and experience turbulent fluctuations [77]. For VTOL vehicles, this is a common occurrence in transitional flight where rotor blades experience significant off-axis flow [42]. In rotorcraft, broadband and tonal noise manifest as propeller-airframe interaction and propeller-propeller interaction noise. These can include blade wake interaction from aft rotors experiencing the vortices of forward propellers or destructive interference from phased tonal noise of opposite propellers [65].

2.1.2 Inflow Modeling and Propulsor Load Calculation

Blade element momentum theory (BEMT) is a method of determining aerodynamic loads on a propulsor that combines actuator disk theory [47] with blade element theory [15], [48],

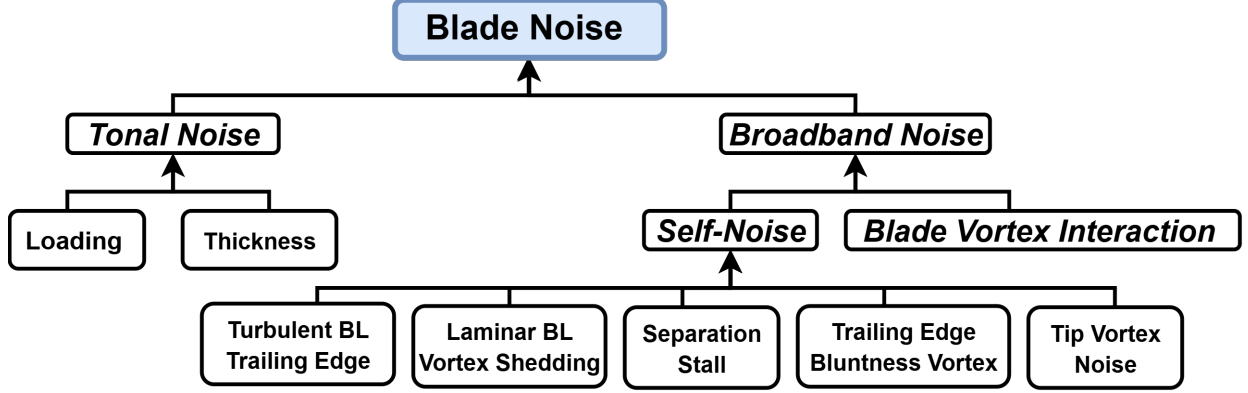


Figure 2.1: Components of tonal and broadband noise.

[101], [33]. Actuator disk theory uses conservation of mass and momentum to calculate the performance of a propulsor disk based on changes in uniform flow across the disk plane [47]. Blade element theory is based on the flow about two-dimensional blade sections to calculate lift and drag using thin airfoil theory at sectional divisions of the propulsor blade. The use of actuator disk theory simplifies the calculation of induced velocities at each blade section and accounts for angular momentum [101].

The following section documents various inflow models which can be implemented in the source noise module. The basic solution of actuator disk momentum theory, as presented in Leishman's *Principles of Helicopter Aerodynamics*, works well for static cases and hover [68]:

$$\lambda = \mu \tan \alpha + \frac{C_\tau}{2\sqrt{\mu^2 + \lambda^2}} \quad (2.1)$$

This theory assumes an azimuthally uniform loading, with induced velocities assumed normal to the disk plane. A more accurate inflow model, however, was proposed by Glauert and used a simple harmonic inflow model to create a linear variation of induced velocity across the propulsor disk [47]:

$$\lambda = \lambda_0(1 + K_c r \cos(\phi) + K_s r \sin(\phi)) \quad (2.2)$$

Here, K_c and K_s are undetermined weights that represent deviations from the uniform inflow of momentum theory and can be solved using experimental and flight-test data. Various

works have since developed alternate expressions for the gradient K_c by incorporating blade element theory to allow unsteady inflow [85]. Harmonic inflow models based on Glauert's original proposal are still widely used due to their modest computational time and ease of implementation [22]. Coleman et al. developed an analytical model for the inflow gradient that expressed the wake using a cylindrical vortex sheet about the blade tips, with K_c defined as follows [26]:

$$K_c = \tan\left(\frac{x}{2}\right) \quad (2.3)$$

These momentum-theory inflow methods show good correlation for cases of mostly axial inflow, where the apparent velocity is normal to the disk plane. Other conditions of flight, however, involve cross flow, which is defined as the component of velocity parallel to the disk plane. This is represented by a cross-flow angle, illustrated in Figure 2.2, which describes the magnitude of propulsor tilt with respect to normal inflow.

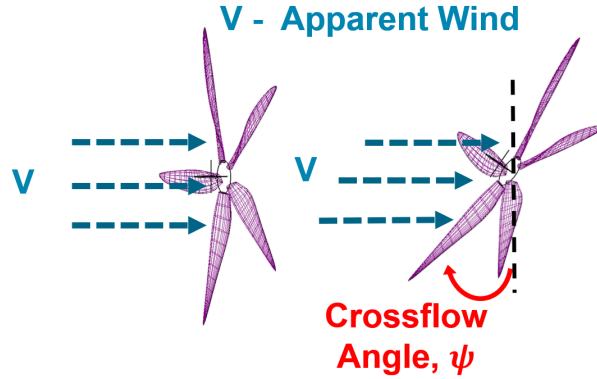


Figure 2.2: Illustration of uniform inflow. The propulsor to the left operates in purely axial flow, while the propulsor to the right experiences cross flow at a cross-flow angle of ψ .

Pitt and Peters developed an unsteady actuator-disk-theory inflow model that provided closed-form expressions for the integration of propulsor loads and induced velocities. The resulting Pitt-Peters inflow model has shown good comparison against experimental data for rotorcraft and other propulsors operating under unsteady and non-uniform inflow conditions [86]. Using the desired inflow model, the radial blade lift, drag, and thrust coefficients

are calculated. The resulting forces are decomposed into their respective perpendicular and parallel components. These are also used to model other parameters such as pressure distribution on the blade surface. This uniform inflow method is ideal for vehicles operating under steady-state conditions in undisturbed airflow.

2.1.3 Propulsor Noise Theory and Modeling Approaches

Most of the noise tools used in this methodology and in other studies are based on the Ffowcs Williams–Hawkings equation [43]. This is an equation that is formulated around Lighthill’s acoustic analogy, which is an expression that relates a turbulence stress tensor to the fluctuation of density with respect to length and time [71]. The Ffowcs Williams–Hawkings equation considers Lighthill’s acoustic analogy on a solid boundary layer in a moving flow field. The result is a wave equation composed of a series of monopole, dipole, and quadrupole source terms representing forces on a solid body, its displacement and acceleration, and its motion [80]. Farassat developed a solution to the Ffowcs Williams–Hawkings equation for a permeable data surface known as the 1A Formulation. This solution was a major advancement because it located the data surface such that only non-negligible noise-producing quadrupoles needed to be integrated, eliminating the need for a volume integral outside of this surface [39]. Using this theory, the Ffowcs Williams–Hawkings equation was solved using a free space Green’s function with respect to a moving reference frame [40]. The resulting solution is broken down into two expressions that represent the thickness and loading noise components.

Formulation A1 is implemented in many rotor and propeller aeroacoustic codes including Penn State University’s expansion and re-development of NASA’s WOPWOP [73] (PSU-WOPWOP) [17]. This code places the Ffowcs Williams–Hawkings permeable surface around the propulsor blades, in the form of a long cylinder, or around the entire propulsor, as a stout cylinder. An additional way PSU-WOPWOP furthers the implementation of Formulation A1 is through the use of tensor algebra which avoids the traditional matrix algebra inte-

gration of transformation coordinates to calculate velocity and acceleration [17]. Another aeroacoustic code based on Formulation A1 is NASA’s Aircraft NOise Prediction Program 2 (ANOPP2) [74]. The fundamental concept behind the many different aeroacoustic tools found in ANOPP2 is the use of nested Ffowcs Williams–Hawkings surfaces to allow the analysis of an entire vehicle with varying levels of fidelity. For example, specific aircraft components, such as propulsors or flaps, can each be surrounded by source surfaces. Spheres or hemispheres are common shapes, though Ffowcs Williams–Hawkings surfaces can be arbitrarily shaped, allowing parameters such as exhaust to be included [75]. These surfaces can be summed into a mid-surface which surrounds the entire vehicle. Mid-surfaces are then propagated, using methods that can implement atmospheric and terrain effects, onto far surfaces or far-field observers. ANOPP2’s basic surface framework is implemented into a suite of modules which can predict a variety of aeroacoustic effects such as airframe and propulsor interaction effects [74].

Brooks, Pope, and Marcolini developed a widely used broadband self-noise prediction scheme (BPM) [18] which augmented theory with empirical datasets from anechoic wind tunnel testing of various two-dimensional and three-dimensional airfoil sections and blades. Analysis focused on determining boundary layer behavior and dependencies on parameters such as Reynolds number, airfoil geometry and Mach number in order to determine curve fit scaling laws. The result is a series of parametric functions which express 1/3 octave sound pressure levels for each of the 5 broadband self-noise mechanisms based primarily around fluid properties and sectional airfoil angles of attack. The BPM method has been validated for Mach numbers below 0.5 and Reynolds numbers below 4.6×10^6 [44]. Aeroacoustic tools such as PSU-WOPWOP and ABEAT have implemented BPM methods to include broadband noise in their modeling capabilities [17], [75].

2.2 Multi-Propulsor Noise Modeling Methodologies

This literature survey focuses on noise prediction and modeling works which contained elements of distributed propulsion propulsor design or analysis, flight procedure analysis of multi-propulsor eS/VTOL vehicles, or community noise analysis of UAM/AAM vehicles. Some studies focused on detailed refinement, such as improved broadband noise prediction [69] or noise abatement during landing [64], while others pursued a more holistic study by expanding existing noise and performance tools to create an empirically based framework for UAM vehicles similar to the FAA’s Aviation Environmental Design Tool (AEDT) [90]. These studies target different applications. Therefore, the scope of propulsor design and noise analysis capabilities varies depending on the purpose of each study. This section surveys past and contemporary methodologies, breaks down their goals and capabilities, and identifies potential gaps or areas for improvement.

2.2.1 Scope of Noise Prediction Frameworks

The methodologies considered in this literature survey can generally be divided into the following categories: those that focus on noise abatement through propulsor design and operation, those that focus on noise prediction through flight operation and trajectory design, and those that create a holistic flight-test-based empirical model for multi-propulsor vehicles to allow the creation of broad noise sensitivities for design and policy making.

Clarke et al. (2021) present one such study that focused on a high-level framework incorporating vehicle design, operation, performance, and noise evaluation of UAM vehicles to allow a holistic assessment [25]. This work emphasized building a robust and versatile design methodology to create models which accurately represented the unique characteristics of eS/VTOL vehicles. Another vehicle design approach can be found in Lima et al.’s 2025 preliminary design framework for UAM vehicles based around performance and vehicle emissions [72]. This methodology was formed around top-level aircraft requirements, such as

range, speed, propulsor, and wing area. These serve as user inputs to size vehicle components and estimate their weight, lift, and drag contributions in a series of first and second level design iterations. The final converged designs are then flown in representative flight profiles and evaluated based on their acoustic noise patterns as well as energy and cost efficiency during operation [72].

Many other methodologies focused their noise modeling efforts on vehicle operation rather than design. Ko, German, and Rauleder (2023) presented one such work that focuses on noise abatement of multi-propulsor vehicles through takeoff and landing procedures without modifying their baseline component design. While multi-rotors yield large potential for increased noise control compared to helicopters, Ko et al. identified the importance of having a flight framework that reflects the unique operational environment of UAM and AAM vehicles in order to accurately assess noise [64]. Habing et al. (2024) provided another example, developing a noise prediction methodology to allow the rapid evaluation and design of trajectories for AAM and IAM (Innovative Air Mobility) vehicles, which are characterized by electric DEP rotor and propeller arrangements [50]. Habing et al. explored gaps in conventional helicopter and fixed wing trajectory frameworks which often have under-developed broadband prediction capabilities and are unable to capture urban effects of noise reflection and navigation between structures [50].

Other works focused on developing methodologies and tools which better capture the unique characteristics of multi-rotor vehicles. Li and Lee (2019) developed one such framework, which focused on broadband noise prediction [69]. Through their UCD-QuietFly methodology, Li and Lee augmented existing trailing edge broadband noise models with coordinate transformations that better account for sectional variations along the propulsor blade and overall blade geometry. This allows UCD-QuietFly to account for rotor twist, blade collective, flap angle, azimuthal angles, and rotor tilt. Li and Lee’s frequency-based approach was used to explore variations in rotor geometry and operating conditions, including tip Mach

number, collective, blade twist, sectional chord, and overall radius [70].

Ko et al. also considered additional avenues of noise evaluation by considering psychological effects of community noise in their study. Psychoacoustic annoyance is a metric defined as a function of loudness, sharpness, fluctuation, and roughness and offers additional insight into the human perception of nuisance noise. A unique aspect of this study concluded that even though their surrogate UAM had lower physical noise, it had a higher psychological impact due to tonal noise occurring in less favorably weighted bands [64].

2.2.2 Propulsor Design

The studies discussed also had varying levels of capability and approaches to propulsor design and evaluation. Methodologies which emphasized realistic predictions or the ability to capture the noise effects of vehicle components generally had the ability to size vehicles with varying architecture types based on a combination of empirical data and first-principles physics.

Clarke et al. found that contemporary AAM works were lacking in component-level design, especially those built around fixed-wing aircraft and helicopters. Clarke et al. used a mix of semi-empirical (SUAVE) and physics-based modeling to allow ground-up design of surrogate propulsors, batteries, vehicles, and airframes for unique vehicle architectures, including lift-plus-cruise (LPC), STOL, and VTOL. This resulted in the capability to size both rotors and propellers based on a vehicle’s performance requirements, such as thrust and power, and operational characteristics, such as hover or climb angle. This methodology is also one which used high-level operational constraints to size the preliminary aircraft. Propellers were sized to balance efficiency (best minimum power loss condition) while meeting both transition and cruise requirements, whereas rotors were sized mainly around hover [25]. Lima et al. present another component-level study which uses first-order tools and models for propulsor design, including actuator disk theory and lifting-line models in conjunction with semi-empirical

methods [72]. This allows a relatively detailed design scheme from top-level inputs that can also encompass other vehicle components such as power-plant mass and drag from non-lifting bodies [72].

Other works utilize existing conceptual design tools to determine propulsor properties such as NASA’s Design and Analysis of Rotorcraft (NDARC) [96]. This program allows rapid and consistent trade studies on technology and component arrangement for a broad range of unconventional vehicle designs. The result is a set of performance parameters, such as drag and weight, based on conceptual combinations of individual components which include power-plant, rotors, wings, tails, and fuselage [57]. Other works, such as Li and Lee’s vehicle blade geometry, arrangement, and operation, were based on available data and parameters in literature [70].

2.2.3 Flight Path and Operation Design

A common method for generating flight profiles was through first-order physics-based models based on a vehicle’s flight capabilities. Ko et al. derived their vehicle parameters such as vehicle weight, drag, and propulsor performance from other studies [96] which used low-fidelity rapid design tools. Using these specifications, flight profiles were constructed using acceleration and velocity profiles representative of UAM vehicles [64]. In a similar manner, Lima et al. created feasible mission profiles based on previously calculated vehicle lift and drag coefficients with propulsor performance. Additional constraints are sometimes implemented to augment physics-based models to better reflect regulations, passenger comfort, or energy efficiency. Ko et al. created vehicle-specific approach and departure profiles intended to reflect more realistic requirements, such as avoiding vortex ring state and imposing pitch-attitude and acceleration limits between 0.1–0.3g based on studies of safety and passenger comfort. Flight profiles were controlled through propeller trim by changing the tip Mach number (RPM) and advance ratio of each motor [64].

Other studies used propulsor geometries and performance parameters from existing representative vehicles to design flight path trajectories. One such example can be found in the work of Czech et al. (2023), who based their community noise analysis on existing commercial helicopter routes [30]. This study’s source noise hemispheres were provided by NASA through its AAM Community Integration Planning tool, which provided noise data at a range of climb angles, speeds, and power settings. This information was used to design realistic climb and descent trajectories [30]. Habing et al. presented another study which used geometry from commercially available multi-rotor UAM vehicles [50]. Li and Lee’s prediction approach emphasized a more robust coordinate transform that better accounted for multi-rotor vehicles and could incorporate parameters such as amplitude modulation. Li and Lee’s prediction method demonstrated tradeoffs between single- and multi-rotor broadband noise, in which decreasing blade radius decreased low-frequency noise while increasing rotor count increased high-frequency noise through the addition of more trailing-edge noise [69].

2.2.4 DEP Augmented Lift Vehicle Design

This section discusses works which explore the detailed conceptual design of distributed propulsion and blown-lift augmentation, as well as the scope of noise analysis for these systems. Most of these sources were used to formulate the physics behind the STOL evaluation study in Chapters 4, 5, and 6. Courtin (2024) developed a study that examined the advantages of DEP blown-lift aircraft over conventional takeoff and landing (cTOL) and VTOL vehicles and explored approaches for sizing and analyzing the performance and controls of DEP vehicles [29]. Courtin designed a component-level methodology that sizes the propulsion system, power plant, aerodynamic characteristics, and operational envelope of a surrogate DEP eSTOL vehicle. Conventional flow-wing interaction theory tends to overpredict lift augmentation when operating under large differences between blown-flow and freestream velocities [66]. Significant variations between modeled and experimental performance also occur when rotor size approaches the wing chord dimension [103].

Courtin incorporated and validated a theory based on effective jet deflection [78] to more closely predict the overall effect on blown-flap STOL vehicles. Courtin took a component-based approach, combining analytical models with empirically derived weights to size propulsion and motors, energy storage and power systems, wings and lift augmentation, and tails and control schemes. This resulted in a robust coupling of rotor variation and blown-flap performance with a propulsion and battery sizing scheme which allowed the comparison of unconventional eSTOL DEP arrangements in takeoff performance and cruise economics [29].

2.2.5 DEP Applications in Regional Turboprop Aircraft

Many studies have been conducted on turboprop aircraft and their potential to offer solutions for sustainable aviation. Rising fuel costs and environmental concerns have recently directed commercial and research efforts into expanding the use of turboprops in current aviation markets [13]. Routes under 1000 nautical miles make up nearly half of US domestic flights and are dominated by regional jets and narrow-bodies [53]. Turboprops operate with substantially less fuel burn than turbofans and are especially suited for short flights due to their ability to operate at low cruise altitudes at lower speeds. Existing turboprop aircraft have capacities of over 60 seats and many manufacturers are proposing 90-plus stretched variants [20]. Turboprop aircraft are well poised to replace many existing aircraft and market segments due to their lower operating costs and fuel economy [32]. These existing capabilities, infrastructures, and commercial demand make turboprop aircraft an ideal platform to explore the implementation of advanced technologies such as electrification.

Distributed electric propulsion decouples an aircraft’s energy source from its propulsors and allows the use of electric and hybrid-electric propulsion systems. Electric motors allow much more freedom in the layout and operation of propulsors, which allows the design of propulsors with lower rotational speeds and lower disk loading. This results in potentially quieter and more efficient propulsors. Several studies have explored the impacts of distributed electric propulsion on performance and noise in the context of advanced/urban air mobility

(A/UAM) [25, 64, 11]. Others explored implementations of DEP for applications such as short takeoff and landing (STOL) [27, 29], as well as the conceptual [51] and experimental design of small-scale advanced aircraft [98] and the integration of subsystems [84].

Many works have also been conducted on the use of DEP on regional aircraft; however, propulsor design, operation, and noise emissions are often explored in a more limited scope. Studies which applied DEP propulsor design and analysis to larger regional aircraft often focused on a narrow scope of propulsor design parameters [60, 76, 21] or a fixed propulsor design [81]. Propulsor designs often do not vary broadly in geometry, operation, and propulsor count. Several works conducted systems-level conceptual designs of regional aircraft that focused on the impacts of DEP on efficiency and performance but were limited to ducted-fan propulsors [61, 100, 99] or placed less emphasis on noise emissions [32]. Lastly, while many studies have been conducted on high-speed turboprop design [34, 79], they rarely explored the use of DEP in this application. These points motivate the exploration of propulsor design and operation for a regional aircraft with a focus on efficiency, noise emissions, and expanded operational capabilities.

2.2.6 Noise Modeling Capabilities of Methodologies

Methodologies which created their own blade geometry usually also had the ability to perform performance evaluation and aerodynamic load calculations using methods similar to those described in Section 2.1.3. Clarke et al. used a BEMT method for propulsor design and a vortex lattice method for aerodynamic load evaluation, identifying blade disk loading and tip Mach number as key factors affecting vehicle noise [25]. Ko, German, and Rauleder utilized a methodology based on aerodynamic calculations augmented with a Beddoes-wake model to predict vortex interactions through a free-wake vortex lattice solver, yielding a low- to medium-resolution model [64]. The framework of Habing et al. computed rotor loads from available software such as XFOIL, URAMS, and FLIGHTLAB and used a rotor lifting-line acoustic (ROLLAC) framework and a rotor lifting-line non-axial (ROLLNAX) framework to

model and propagate rotor noise [50].

Other methodologies employed a simpler method of noise evaluation which sometimes ignored broadband noise or unsteady noise mechanisms. The approach of Czech et al. incorporated broadband noise but was limited to a set of pre-created noise hemispheres adapted from NASA helicopter noise data [30]. The implementation of AEDT by Rizzi et al. for AAM noise modeling was based on data that did not include broadband noise [89]. Habing et al. presented another framework which intends to explore noise caused by wake-ingestion and non-axial flow. The noise modeling of Clarke et al. was performed using a time-domain Hanson formulation to solve the Ffowcs Williams–Hawkings equation. The scope of their noise modeling was limited to harmonic noise (tonal) since broadband, airframe, and wake interaction noises were ignored. Additionally, community-noise propagation area was limited to static, fixed-event contours at three altitudes (50 ft–50 x 50 ft, 250 ft–400 x 400 ft, and 1500 ft–2000 x 2000 ft). Dynamic flight-profile contours were limited to undertrack SEL noise [25].

Most methodologies which did model broadband noise used semi-empirical methods similar to those described in Section 2.1.3. In addition to using Ffowcs Williams–Hawkings to solve for tonal noise, the ROLLAC and ROLLNAX suites utilized by Habing et al. possess broadband capabilities through the use of multiple semi-empirical noise models such as BPM [18], Pegg, GL, or TI methods [50]. Ko et al. considered both tonal and broadband noise, solved by using Ffowcs Williams–Hawkings and the BPM method, and considered unsteady flow effects. They found that noise increased during periods of unsteady loading: their surrogate UAV had lower tip speed but higher intensity due to unsteady loading in bands with less A-weighting attenuation [64].

Li and Lee’s work is an example of a methodology which focused on the development of a broadband trailing-edge noise prediction and propagation framework combining [9], [62], and their previous Quiet-fly framework [69] to perform an evaluation using VTOL surrogate

models. Their study performed a narrow, frequency-based analysis of noise, breaking down broadband noise into Blade Wake Interactions and Turbulent Ingestion Noise at low to medium frequencies, airfoil self-noise at medium to high frequencies, and trailing edge and trailing edge bluntness noise at high frequencies (predicted by BPM). Li and Lee’s study concluded that while multi-rotor VTOL vehicles had the potential to reduce overall noise, a large amount of broadband noise occurred at frequencies not masked by urban environmental noise and that these factors are important to consider in vehicle design [70]. Their work found that increases in tip Mach number, collective, sectional blade twist, and chord length increased overall SPL by 2 dB, while changes in rotor radius increased SPL by 5 dB. Li and Lee’s frequency-based analysis, however, found that these changes in broadband noise were mostly confined to frequencies below 1–5 kHz, while those above remained largely unchanged. These changes were largely due to increases in boundary layer displacement thickness at higher angles of attack and increased rotor chord, while factors such as rotor radius increased noise by increasing the overall source noise region. Finally, Li and Lee found that tip Mach number noise scaled to the 5.5^{th} power, while frequency scaled as $f * 0.8 / M_{Tip}$, which matched well with values theorized by Hutchenson and Brooks [54] [69].

2.3 Summary and Scope of Existing Work

This literature survey identified gaps in vehicle, propulsor, and flight profile design, as well as in the integration of aircraft operation with source- and community-noise prediction. Although most of the methodologies coupled design, operation, and noise analysis, many did not have methods robust enough to consider a wide range of vehicle architectures or propulsor arrangements [30]. Those that could were usually unable to create designs from only a sparse set of top-level requirements and instead required existing models or data from other studies to size vehicle and propulsor models [30] [106].

Similar limitations were identified in studies of DEP regional aircraft. Although several

works evaluated the effects of distributed propulsion on aircraft efficiency and performance, propulsor design was often limited to fixed configurations or a narrow range of geometric and operational parameters [60, 76, 21, 81]. Systems-level regional-aircraft studies also tended to focus on ducted-fan configurations [61, 100, 99] or placed less emphasis on noise emissions [32]. Consequently, relatively few studies explored the coupled effects of propulsor geometry, operation, and count on efficiency and noise for regional turboprop aircraft. High-speed turboprop studies demonstrated the potential for efficient operation at elevated cruise speeds [34, 79], but these studies generally predated contemporary DEP technologies and did not investigate high-speed operation in the context of distributed electric propulsion.

Gaps were also found in the overall noise modeling capabilities of many methodologies. Works that focused on vehicle design and performance analysis often did not consider noise when evaluating overall environmental impacts [29] [103]. This limitation is especially relevant to blown-flap STOL vehicles, for which propulsor layout and operation strongly affect both lift augmentation and acoustic behavior, yet much of the existing work focuses primarily on aerodynamic performance rather than noise [27, 29, 66, 103]. Others only considered tonal noise and did not have the capability to model broadband noise [25] [89] or noise during unsteady, transitional periods of flight. Similarly, works that focused on the development of noise modeling for highly specific component noise mechanisms [70] [69] usually had limited capability to consider propagation effects on community noise during dynamic flight. Even works that did focus on the effects of flight profiles had limited community noise resolution, with some works only generating under-track noise instead of area contours or only calculating contours for static flight [25]. More generally, limited integration between source-noise prediction, aircraft performance, and dynamic flight operation makes it difficult to capture tradeoffs in which changes in propulsor operation alter both source noise and the resulting flight trajectory. These effects are important because community noise depends not only on source noise but also on the aircraft operating states encountered along the flight profile. In addition, methodologies that relied on pre-generated noise hemispheres or exist-

ing propulsor geometries were limited in their ability to evaluate how new combinations of propulsor geometry, operating conditions, and arrangement influence source and community noise [30, 106, 70, 50].

The methodology discussed in this thesis contributes to the current field by coupling a robust top-level propulsor design tool with flight-performance, source-noise, and community-noise models capable of evaluating the combined effects of propulsor design, aircraft operation, and noise propagation. This framework links propulsor geometry, operation, arrangement, and flight path to create a holistic analysis tool capable of evaluating a wide range of vehicle architectures and identifying tradeoffs among source noise, flight performance, and community-noise propagation. The framework is capable of computing tonal and broadband source noise while also modeling community noise that reflects the unique operating capabilities and flight profiles of different vehicle architectures. These capabilities enable the methodology to evaluate component-level propulsor sensitivities, including geometry, RPM, blade pitch, and arrangement, as well as system-level tradeoffs among aircraft performance, propulsor efficiency, noise emissions, and flight operation.

Chapter 3

Methodology

This methodology consists of five primary modules: Propulsor Sizing and Evaluation, DEP Arrangement Design, Flight Profile Design, Source Noise Modeling, and Noise Propagation, as illustrated in Figure 3.1. Propulsor Sizing and Evaluation and Source Noise Modeling form the core of the methodology and are used in all applications presented in this dissertation. Additional supporting modules, including Lift-Augmentation Evaluation, Nacelle Sizing, and Motor Sizing, are used when required for specific applications such as the evaluation of DEP or STOL architectures. Finally, Flight Profile Design and Noise Propagation are used when aircraft operation analysis and community noise contours are desired.

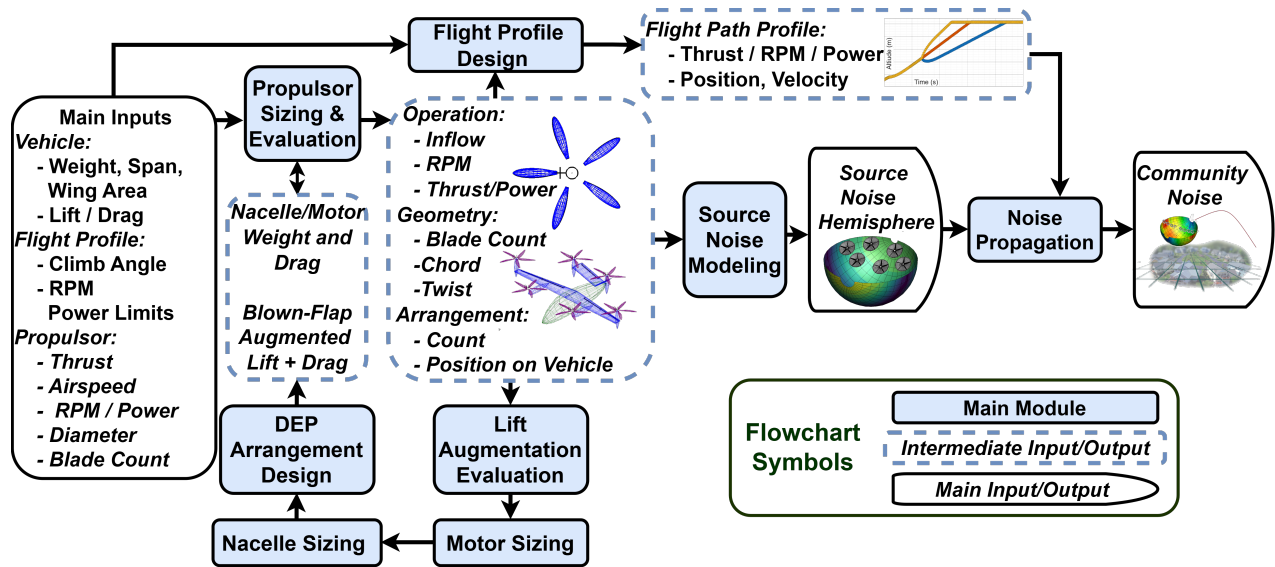


Figure 3.1: Overview of the methodology and primary inputs and outputs.

The main inputs for this methodology are a set of top-level aircraft requirements (TLARs) which specify aircraft characteristics, flight profile requirements, and propulsor characteris-

tics. Vehicle specifications are used to calculate flight physics and include wing geometry, aircraft weight, and aerodynamic parameters such as lift and drag coefficients. Flight profile requirements are used to determine points of propulsor operation and include details such as flight speed, climb rate, and required thrust or power. Propulsor characteristics include known geometry, such as blade count and diameter, and design conditions, such as thrust, RPM, or power.

These inputs are used to generate detailed blade geometry during Propulsor Sizing and Evaluation using BEMT. Detailed designs specify blade count, chord and twist distributions, and performance parameters such as torque and power. This process also calculates parameters such as the coefficient of thrust, C_T , propulsor efficiency, η , and propulsor solidity ratio, which are used to normalize and compare the performance of various propulsor designs.

DEP arrangements are generated using the resulting propulsor outputs and specific design criteria, such as maintaining a constant tip speed or coefficient of thrust or minimizing disk loading. When implemented as DEP, variations in propulsor architecture have the potential to alter aircraft performance. These potential impacts include changes in aerodynamic performance when lift augmentation is used and changes in aircraft weight due to alterations in nacelle and motor design. The DEP Arrangement Design Module is used to account for the impacts of propulsor arrangement on other aircraft subsystems and to iterate propulsor design until all requirements converge.

The Propulsor Sizing and Evaluation and DEP Arrangement Design Modules are based on a limited set of specified design operating conditions. However, the analysis of aircraft operation requires performance at off-design conditions in order to develop representative flight procedures. These points are determined using the Flight Profile Design Module, which generates representative flight profiles from propulsor capabilities and aircraft physics derived from vehicle specifications.

Once propulsor geometries and operating conditions are known, the Source Noise Modeling Module generates broadband and tonal noise to create a local source-noise hemisphere representing the instantaneous noise during a specified operating condition. This source noise hemisphere is based on local properties such as inflow velocity, RPM, blade pitch, and phase angle. Community noise is generated by propagating these source hemispheres along a corresponding flight profile to a grid of ground observers. In addition to operating condition, community noise is affected by aircraft attitude and altitude, as dictated by the flight profile. These capabilities allow this methodology to evaluate both local changes in propulsor geometry, operation, and arrangement and large-scale variations in how the aircraft is flown. These procedures are presented in detail below.

3.1 Propulsor Sizing and Evaluation

The Propulsor Sizing and Evaluation Module is used to design and evaluate the performance of propulsors. This module uses known geometries, such as blade count and diameter, and known operating conditions, such as RPM, inflow velocity, and desired thrust or power, to create representative propulsors. Existing or designed propulsors can be evaluated at off-design conditions to determine thrust, power, and performance coefficients, such as propulsor efficiency. Additional specifications such as sectional lift coefficients or torque and power limits, if known, can be used to further refine propulsor design and constrain operation. Figure 3.2 depicts the procedure of propulsor design and evaluation.

Propeller performance analysis and design are performed using classical blade-element and vortex theory established by Betz, Goldstein, and Theodorsen [15] [48] [101]. These models are implemented in this methodology through interactive codes which perform calculations from user text inputs such as XROTOR and QPROP [33]. Though any blade-element program can be used, XROTOR is preferred because it implements an augmented blade-element theory that provides more accurate analysis at high disk loading and more consistent

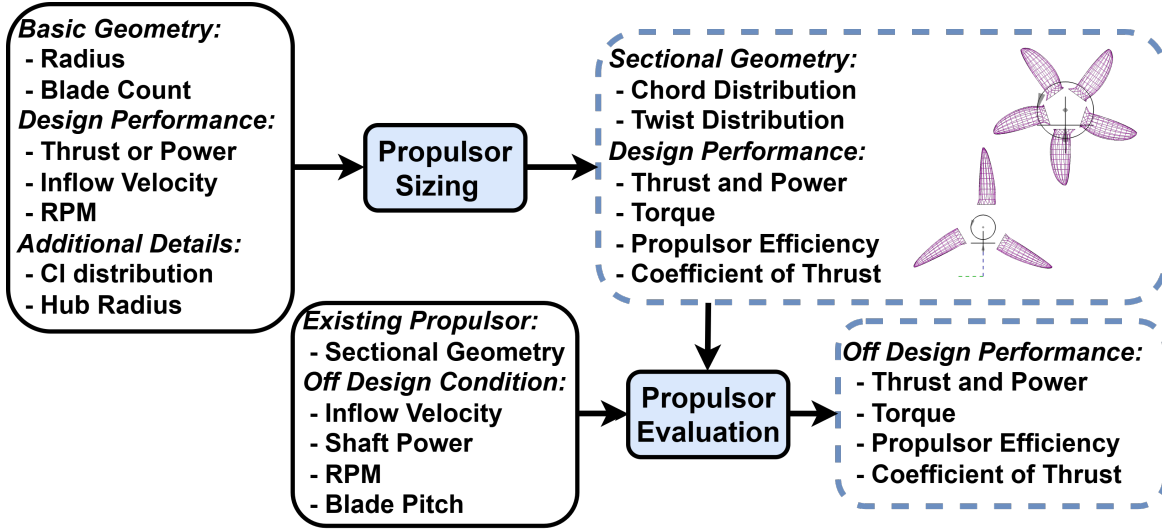


Figure 3.2: Propulsor Sizing and Evaluation Module and its primary inputs and outputs.

design and analysis results [14]. This extension improves classical blade-element momentum theory by allowing induced velocity to vary radially, which better aligns the results with empirical data when propulsors are operated under heavy load. XROTOR also incorporates iteration schemes that allow the design of propulsors that maximize efficiency for a given span and operating condition using BEMT [33].

3.1.1 Propulsor Design

For a given set of performance requirements and known geometry, a propeller capable of meeting desired aircraft performance can be created. Blade chord and twist distributions are generated for a given propeller diameter, blade count, RPM, power, and airspeed. Other details, if known, can also be specified, such as c_l distribution, advance ratio, torque, and desired thrust. This portion of the framework also has the capability to modify existing geometry. Blade radius can be increased or decreased, blades can be retwisted, their chords can be reshaped, and the number of blades can be reduced or increased. These changes can all be done while preserving either power, thrust, or RPM.

During propulsor design, thrust, torque, or an RPM/torque relation must be specified. $\beta(r)$ and/or c_r will be treated as unknowns and Newton's residual will be parameterized as a

function of V , Ω , β , c , and a final variable representing propeller loading. This is set by specifying either thrust or torque. Ultimately, the main propeller design objective is to create geometries that result in minimum induced loss (MIL), constant radial efficiency, and maximum induced efficiency for the specified operating condition [33]. For the Source Noise Module, the relevant result is a distribution of $c(r)$ and $\beta(r)$ that is used to generate a 3-D propeller. The use of XROTOR allows the creation and noise analysis of propulsor designs that vary in blade count, solidity, thrust, RPM, and motor power.

The specific procedure used to develop inputs for propulsor sizing is dependent on the design goals and requirements. An example design criterion would be to maximize thrust or power for a given diameter and torque. Chapters 4.2.1, 4.3.1, 5.2.1, and 6.1 present detailed design procedures for four unique applications.

3.1.2 Propulsor Analysis

For a known propulsor geometry, the framework models performance properties using operating points such as inflow velocity, RPM, power, or thrust. The propeller geometry input includes chord distribution $c(r)$, blade twist distribution $\beta(r)$, and overall parameters such as blade count and propeller radius. Performance outputs include thrust, power, and torque. This analysis ensures the required vehicle thrust is achievable with a given propeller design. Additionally, off-design operating conditions can be analyzed, such as varying RPM and air-speed for a given design. These operating points are used to pair flight path properties, such as glide slope, with their corresponding propeller power levels. The resulting parameters are used to create a flight profile that later serves as input to both the Source Noise Modeling Module and the Noise Propagation Module.

For most of the studies presented in this work, XROTOR was selected because of its ability to vary desired parameters and its consistency between propeller design and performance analysis. The following description comes from theory documentation describing the formu-

lation of XROTOR [33]. The program starts by decomposing relative velocity and adding a tangential "swirl" velocity caused by the torque of the propeller blade. Prandtl's tip-loss factor is modified by using the blade's local wake advance ratio, λ_w , rather than the propulsor's average ratio because this provides a more accurate result for heavy disk loading. The local angle of attack is based on the geometric twist angle, β , and the local decomposed velocity. This is also used to obtain the sectional blade circulation, Γ . Finally, the radial circulation distribution, $\Gamma(r)$, is calculated independently at each section using blade rotation, velocity, $c(r)$, airfoil section information, c_d , c_l , and $\beta(r)$. Newton's method is used to iterate the residual as a function of c_l and Γ . Propeller thrust and torque are analyzed by integrating the radial circulation distribution, $\Gamma(r)$.

3.2 DEP Arrangement Design

The results of the Propulsor Sizing and Evaluation Module are based on a fixed set of design and performance requirements. However, propulsor arrangement has the potential to affect the overall performance of a DEP aircraft. Propulsor count, geometry, and placement directly impact aerodynamics when used with wing-based lift-augmentation systems, such as those employed by blown-flap STOL vehicles. Even in cases with minimal effects on lift and drag, DEP arrangements have the potential to alter aircraft weight and drag through the sizing of motors and nacelles.

The DEP Arrangement Design Module determines changes in performance requirements resulting from variations in the overall propulsor architecture, such as propulsor count. This module uses individual propulsor parameters to design DEP arrangements, determine their impacts on aircraft performance, update requirements used to resize designs during Propulsor Sizing and Evaluation, and iterate the DEP designs until the results converge. An example application would be to design DEP arrangements for a blown-flap STOL vehicle without changing ground-roll distance or climb angle. To that end, the DEP Arrangement Design

Module determines how propulsor count affects the effectiveness of lift augmentation, how resized nacelles and motors alter aircraft weight and aerodynamics, how these changes affect aircraft performance at a specified flight condition, and how required thrust must be adjusted to maintain the original flight characteristics.

3.2.1 DEP Lift-Augmentation Evaluation

Distributed propulsion affects the performance of vehicles employing lift-augmentation devices, such as blown-flaps, which alter the effective thrust required for approach and departure conditions. For these vehicles, DEP arrangements must be sized to account for changes in thrust and power from variations in propulsor count while maintaining desired vehicle performance. This procedure is demonstrated in an application in Section 4.3.

3.2.1.1 Effects of Lift Augmentation

The performance of conventional aircraft with propulsors having diameters greater than one wing chord length is well predicted using the assumption of increased local dynamic pressure over blown portions of the wing [29]. However, as propulsor diameter decreases relative to the wing chord length, classical theory has been experimentally shown to overestimate lift due to unconventionally high lift coefficients and dynamic pressures [103, 66]. A more accurate approach is to model a deflected jet sheet blown over and past the wing. This sheet interacts with the freestream flow and wing to form a chord extension capable of maintaining a pressure difference past the trailing edge of the wing [103, 78]. One such solution is an augmented, semi-empirical 3-D lift coefficient, C_L , developed by Courtin, which expresses lift as a function of flap geometry and vehicle thrust [29]. This formulation is implemented in the present framework as:

$$C_L = f(\alpha, \delta_F, \Delta C_J) \quad (3.1)$$

The net streamwise force coefficient, C_x , is similarly represented as [29]:

$$C_x = f(C_L, \Delta C_J, C_{D0}, T_C) \quad (3.2)$$

where T_C is the overall propulsor thrust coefficient:

$$T_C = \frac{T}{q_\infty A_d} \quad (3.3)$$

and A_d is the propulsor disk area. The augmented lift coefficients, derived from the deflected jet-sheet blown-flap model, depend on two parameters: the jet deflection angle, δ_J , and the jet momentum excess coefficient, which represents the momentum difference between the jet stream and the freestream, represented by ΔC_J [29]:

$$\Delta C_J = 2 \frac{h_d}{c} \left(\frac{V_j^2}{V_\infty^2} - 1 \right) \left(\frac{V_\infty}{V_j} + 1 \right) \quad (3.4)$$

where V_j is the average jet velocity, c is the wing chord length, and h_d is the disk height, defined as [29]:

$$h_d = \frac{A_d N_{Prop}}{b} \quad (3.5)$$

For a uniformly blown wing, δ_J can be represented using an empirically derived equation based on numerical solutions [29]:

$$\delta_J = \delta_F \left(f_1 e^{f_2 \left(\frac{h_d}{c} \right)} + f_3 e^{f_4 \left(\frac{h_d}{c} \right)} \right) \quad (3.6)$$

where δ_F is the flap angle and f_i are coefficients of the exponential fit described in [29]. Increasing propulsor count through the variable h_d increases ΔC_J , while potentially reducing δ_J , depending on the signs and magnitudes of the f_i coefficients. An example blown-flap propulsor architecture is depicted in Fig. 3.3, illustrating key components including flap deflection angle, δ_F , and jet velocity, V_j [108].

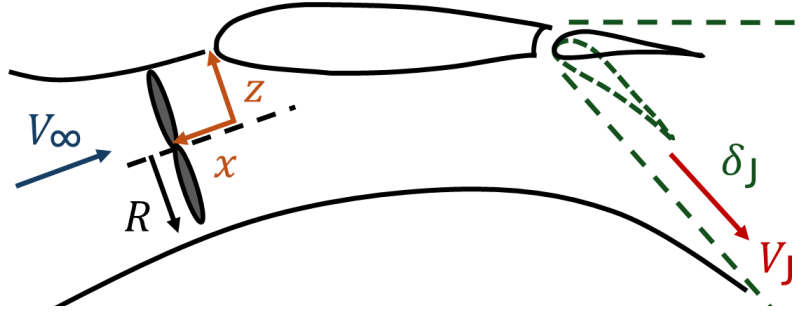


Figure 3.3: Blown-flap propulsor configuration illustrating the deflected momentum jet .

The resulting lift and net-horizontal force coefficients, given by Eqs. (3.1) and (3.2), respectively, can be combined with aircraft weight to determine the instantaneous force balance of the aircraft. These equations are applied in a time-discrete analysis to calculate aircraft kinematics during operation.

3.2.1.2 DEP Performance Evaluation

The effects of lift augmentation are particularly important during high-lift flight segments and complicate the estimation of performance during takeoff and landing. Conventional assumptions, such as constant values of α_{max} or $C_{L_{max}}$, are no longer valid, as these parameters must be expressed as functions of thrust, power, and airspeed using the previously described augmented C_L and C_x coefficients. This methodology adopts the approach derived in [29] to calculate augmented lift and drag characteristics from the blown-flap parameters described in Section 3.2.1.1. The resulting lift and drag forces are then used in the following kinematic equations to describe vehicle forces during ground roll and rotation, Eq. (3.7), and climb, Eq. (3.8).

$$ma = -X - \mu(W_{TO} - L) \quad (3.7)$$

$$ma = -X - W_{TO} \sin(\gamma) \quad (3.8)$$

W_{TO} is the takeoff gross weight, γ is the flight path angle, and X is the net forward force (thrust minus total drag). Equation (3.8) is used when the vehicle's lift matches or exceeds

its weight. A simplified takeoff profile can then be generated based on available thrust and desired performance parameters such as ground-roll or obstacle clearance distance.

3.2.2 Motor and Nacelle Sizing and Iteration

DEP arrangements have the potential to affect aircraft noise and performance beyond variations in propulsor design. Propulsor count affects aircraft performance by altering weight and drag through the integration of additional nacelles and motors. Nacelle size affects parasitic drag through surface area and characteristic Reynolds number, which are functions of geometry. Changes in nacelle volume also affect total nacelle weight, which, along with motor weight, alters induced drag and the aircraft weight component during climb. To account for these effects, changes in motor and nacelle weight and drag are added to the design conditions, and the required thrust is recalculated. This procedure is demonstrated for an application in Section 6.1.4.

3.2.2.1 Nacelle Sizing

Nacelle diameter is sized as a function of individual propulsor power using the equation derived in [99]. A nacelle length-to-diameter ratio of 3 is used to represent the stouter size of electric motors compared to gas turbines and is held constant for all propulsor configurations [88]. Surface area was calculated by approximating each nacelle as a blunt-nosed semi-ellipsoid. Finally, nacelle weight was calculated using a nominal thickness of 1 mm and the material density of duraluminum, based on the procedure used in [99].

3.2.2.2 Motor Sizing

Motor weight and size were determined using the motor sizing tool described in [59]. This module was created through an industry collaboration and represents an advanced motor design using contemporary non-cryogenically cooled technology. The two main inputs are propulsor diameter and individual power. Motor dimensions and weight are determined by the amount of insulation and copper needed to dissipate a specified amount of heat. This

information comes from a surrogate motor model based on existing and proposed electric motor architectures, motor efficiency, and capabilities. Using this motor feasibility design tool, a representative motor was created for each arrangement based on its propulsor RPM and power requirements.

3.3 Source Noise Modeling

The Source Noise Modeling Module consists of two primary parts: Inflow Calculation and Load Generation and Source Noise Calculation, as illustrated in Fig. 3.4. Many existing tools described in Section 2.1.2 can be implemented in the methodology to perform these functions. The Blade Element Acoustic Tool (ABEAT) within NASA’s ANOPP2 is one such program which uses lifting-line theory to derive sectional loads along a propeller blade [74, 17]. Most of the work presented below models noise by solving the Ffowcs Williams–Hawkings equation through programs such as Penn State University’s PSU-WOPWOP or NASA’s Aircraft NOise Prediction Program (ANOPP2) [74] [43] [17]. Functional modules exist within these programs to couple propulsor-load calculations with broadband and tonal noise calculations for propeller operation, as shown in Figure 3.4. For a specific vehicle model, modules are selected to allow unique configurations of propeller geometries and operating conditions, which permit a wide range of multi-propulsor and propeller vehicles to be considered.

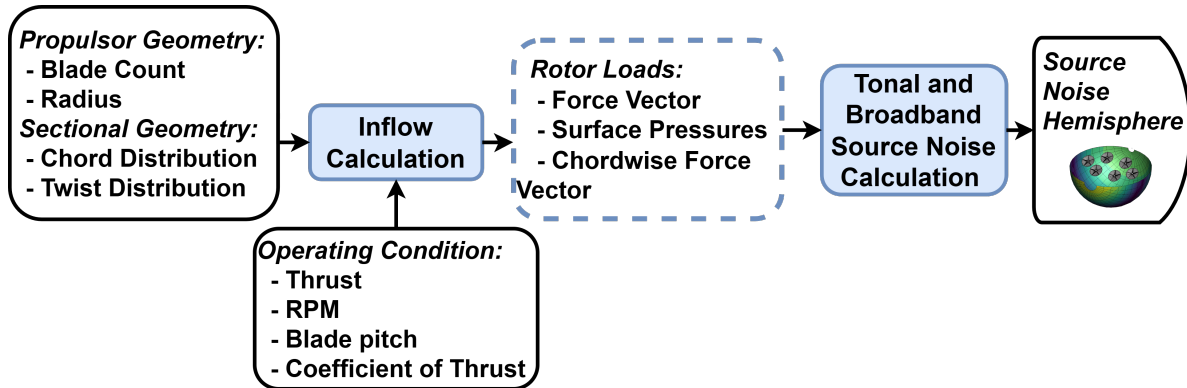


Figure 3.4: Source Noise Modeling Module and its primary inputs and outputs.

3.3.1 Propulsor Surface Modeling

Specified propeller geometry is used to create a lifting-line model of each blade. This is done by dividing each blade into radial sections, r/R . Each radial division then specifies $c(r)$, local airfoil data, $\beta(r)$, leading and trailing edge radii, trailing edge thickness, and leading edge ordinate and abscissa points. The last parameter defines each section's position in space. Other geometry, such as rake and skew, can be specified by shifting each section's ordinate position. Hub diameter is also specified by omitting data within the innermost radial station. Higher model density is desired around areas of greater geometric change such as the propeller tip [112]. The result is a surface mesh model of each propeller blade. An example is illustrated below in Fig. 3.5.

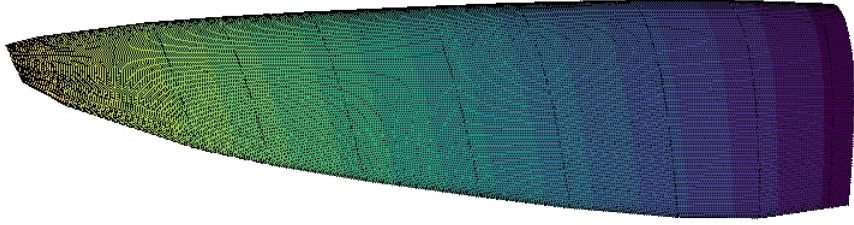


Figure 3.5: Example propeller-blade surface mesh used for source-noise modeling.

The single blade is then patterned symmetrically about the e_θ rotation axis in the propeller coordinate system, (e_r, e_θ, e_ϕ) as defined in Figure 3.6. This results in a single, complete propeller. Rotation direction is then specified, and blade geometry is inverted as necessary to produce either left- or right-handed propellers. Phase offset is also specified at this point, which allows the model to capture in-phase and out-of-phase propeller operation. Lastly, the propeller tilt angle is specified for each propulsor with respect to the local onset velocity. Defined about the e_ϕ axis, this parameter allows the framework to model configurations ranging from conventional fixed-wing aircraft to VTOL tilt-rotor applications. The previous modeling steps are repeated for each subsequent propeller.

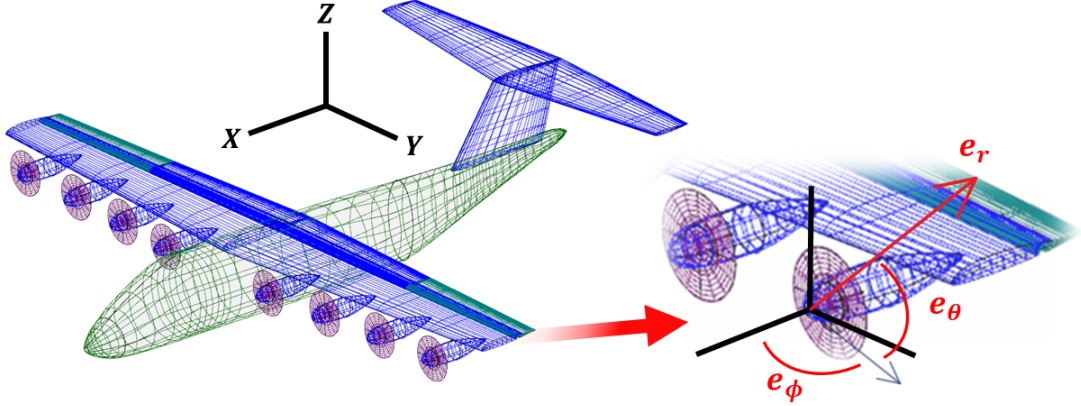


Figure 3.6: Aircraft coordinate system (X, Y, Z) and local propulsor coordinate system (e_r, e_θ, e_ϕ) .

Once propellers of correct geometry, phase, and local orientation are defined, they are combined into a single vehicle using the aircraft geometry input. The propeller locations are specified with respect to the vehicle coordinate system, (x, y, z) . An example aircraft with different propeller diameters, rotation direction, and phase angle is shown in Fig. 3.7.

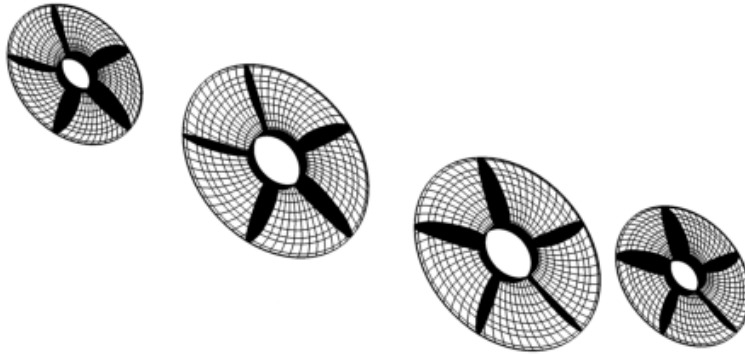


Figure 3.7: Modeled distributed-propulsion configuration with counter-rotating, multi-diameter propellers.

3.3.2 Inflow and Propulsor Loading Calculation

Once a vehicle has been defined, propulsor loads are calculated using the inflow models described in Section 2.1.2. Most of the studies presented employed a harmonic inflow model based on BEMT [47]. Cases requiring non-uniform inflow modeling used an unsteady inflow model [85]. Velocity is a function of thrust and aircraft lift, and is specified with respect to the

ground coordinate system, (X, Y, Z) , described in Figure 3.6. Propeller loads are calculated in an iterative process based on the inflow model, section c_l and c_d , thrust coefficients, and propulsor thrust. Propeller loading calculations are largely based on classical blade-element theory inflow, which iterates c_l , c_d , and thrust until the results converge [22].

3.3.3 Tonal and Broadband Noise Calculation

Propeller noise is modeled based on the resulting propulsor load forces. Tonal noise is modeled by solving the Ffowcs Williams–Hawkings equation (FW-H), which is a time-dependent acoustic analogy that is a function of density, surface velocity, and pressure distribution [43]. Formulation 1A of Farassat (F1A) is one solution found to have excellent utility for helicopters and propeller-based vehicles operating at subsonic speeds [111]. This formulation is based on solving the thickness- and loading-noise terms of FW-H imposed on a moving surface [40]. By ignoring the quadrupole source terms, F1A is able to represent FW-H as an integral function to avoid the need for computationally complex CFD methods [111]. The use of isometric translations also makes Formulation 1A particularly convenient for aeroacoustic applications because distances to both moving and stationary observers are preserved [39]. For this step, the framework employs an ANOPP2 internal module which implements 1A over the moving surfaces of the modeled propeller blades.

Broadband noise is calculated semi-empirically due to the unsteady fluctuations resulting from turbulent flow. The methods used in this framework are based on the Brooks, Pope, and Marcolini model (BPM) [18] for calculating airfoil self-noise. This model identifies and accounts for self-noise due to trailing-edge boundary-layer turbulence, laminar boundary-layer instabilities, trailing-edge vortex shedding, and turbulent vortex flow about the blade tips [18]. Originally developed for airfoils in steady flow, the BPM model has been adapted for use in propulsors using a quasi-steady-state assumption at each blade section [58]. Noise predictions are then calculated using the mean-square pressure along the propulsor radius [44]. For this, an ANOPP2 internal functional module was used to implement the BPM

method.

The broadband and tonal noise results are used to create the aircraft source-noise output through the use of local observers. Depending on the application, observer geometry can range from a two-dimensional ring to a full 3-D hemisphere, as shown in Figure 3.8.

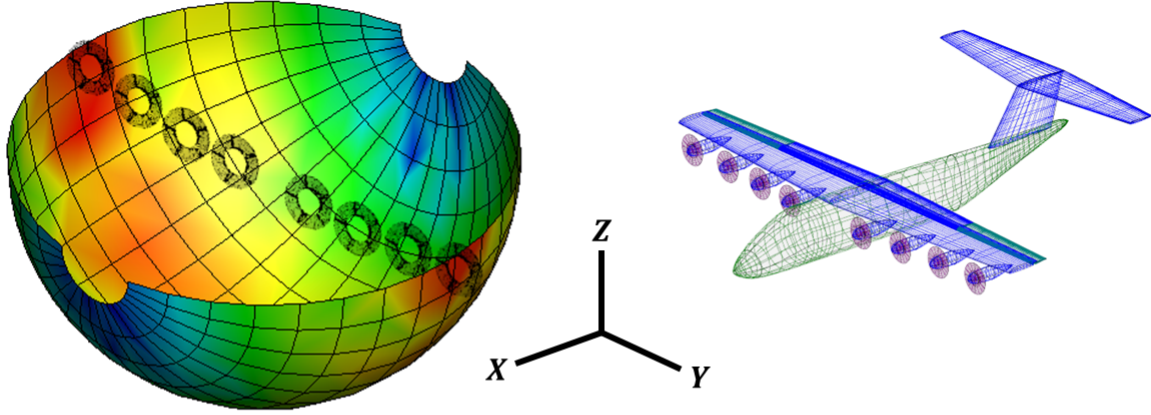


Figure 3.8: Vehicle source-noise hemisphere showing observer locations, propulsor positions, and orientation relative to the aircraft.

3.4 Flight Profile Design

The flight profile of a vehicle is generated from a physics-based model using aerodynamics to determine lift and drag, propulsor performance to determine thrust, and vehicle sizing to determine weight and inertia. At each segment, from climb to steady flight, the vehicle is operated such that it remains in a force-balanced kinematic state defined by the following governing equations [107]:

$$T_x - Drag_x - Lift_x = ma_x \quad (3.9)$$

$$T_y - Drag_y + Lift_y - Weight = ma_y \quad (3.10)$$

An operating condition is defined by parameters such as airspeed or flight path angle, which

are themselves used to determine lift and drag vectors. Drag coefficients are calculated by means of a drag buildup for the vehicle type and lift coefficients depend on whether a vehicle platform has lifting bodies or wings. The Propulsor Analysis subsection, described in Section 3.1.2, is used to determine the RPM or blade pitch necessary to meet thrust and maintain either steady or accelerated flight [83]. This procedure is performed at each segment of a given flight path. The number of segment divisions dictates the resolution of the noise model. Smaller segments will result in a smoother, more realistic flight path but at the cost of computational resources because each segment requires the generation of a specific source noise hemisphere [108]. To balance speed and resolution, steady segments of flight, such as cruise, are modeled with fewer segments than dynamic segments of flight, such as departure or approach. Ground-roll and rotation are specifically included for fixed-wing cTOL and STOL vehicles and are modeled using Equations 3.7 and 3.8 described in Section 3.2.1.2.

3.5 Noise Propagation

Once the vehicle source hemisphere is created, it can then be propagated to noise observers over a specified area. This procedure is based on straight ray modeling, which assumes noise from a source is primarily transmitted along a direct path and a reflected path to an observer [23]. Atmospheric conditions that would curve a noise path, such as wind and temperature variations, are not considered within the scope of this study. Noise attenuation and diffusion effects are modeled using specified atmospheric properties [92].

Noise propagation requires the vehicle’s local flight path, global position, angles of attack, and attitude relative to local wind. Noise levels in the source hemisphere are also allowed to vary with position. This is specified through a flight path, which allows forward velocity, altitude, and RPM to be changed as a function of time. This enables the framework to associate propulsor performance with flight angles for use during noise propagation. Possible noise propagation results include proportional-band spectrum (PBS), narrowband spectrum

(NBS), power spectral density (PSD), and overall sound pressure level (OASPL).

The propagated source noise is then recorded by community-noise observers. These observers are placed along the ground trajectory of the aircraft and are used to generate ground-noise contours as the aircraft follows its specified flight profile. The flight profile dictates altitude and attitude as a function of distance. The Source Noise Modeling Module described in Section 3.3 generates aircraft noise under constant operating conditions. These steps, however, can be repeated as the aircraft propulsors operate in different regimes. The subsequently generated source noise hemispheres are propagated according to the flight profile. These flight profiles include forward velocity, RPM, blade pitch, and even propeller tilt angle.

The preceding modules define the complete methodology from propulsor design through community-noise propagation. The following sections validate the propulsor performance and source-noise modeling capabilities that form the foundation of these analyses.

3.6 Propulsor Source Noise Modeling Validation

The propulsor evaluation, load generation, and noise modeling capabilities of Sections 3.1.2, 3.3.1, and 3.3 were validated against available test data and known propeller geometries. This validation focuses on noise modeling rather than propulsor design and evaluation; therefore, pre-existing propulsor geometries and operating conditions are used. In this section, propulsors with known overall and sectional blade geometry are imported using the Propulsor Sizing and Evaluation Module outlined in Section 3.1 and analyzed using the Source Noise Modeling Module. Geometric parameters in the evaluated propulsors include variations in blade count and blade radius, while operating conditions vary inflow velocity, cross-flow orientation, and RPM. These modeled results are compared against experimental measurements of the actual propulsors at the same operating conditions. The results reveal the accuracy and limitations of the source noise modeling capabilities of the methodology.

3.6.1 Static Propulsor Noise Experiment

In this validation study, noise test data were made available through an industry partnership. These data were obtained from an experiment that measured the noise and static performance of two propellers at various angular velocities. Two five-bladed propellers with small ($Radius_A$) and large ($Radius_B$) radii were operated statically at discrete intervals ranging from 2000 to 4500 RPM. Each propeller was operated in open space at a height of 10 feet above the ground. Nine sound level meter data loggers with integrated microphones were positioned in a half-ring at the same height as the centerline of the propeller depicted in Figure 3.9. This arrangement was intended to take advantage of the setup’s axial symmetry to maximize the density of the noise monitors while adequately capturing directivity.

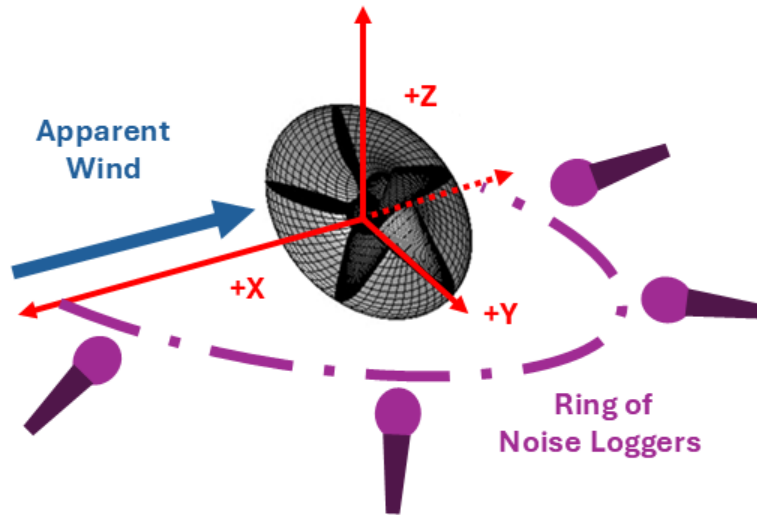


Figure 3.9: Static propulsor noise test setup with nine sound level meters positioned on a 4.5-m half-circle in the propeller plane.

Previous results have demonstrated that modeled propulsor-noise directivity becomes independent of observer-hemisphere size when the observer distance is at least five times the propulsor radius. For better comparison with the methodology’s results, a radius of 4.5 m was specified for both the experimental and modeled setups. Both propellers were then operated at a series of discrete angular velocities so that average noise levels could be recorded

once the noise readings stabilized. The system without propellers was also run and recorded in the same setup so that motor and other electronic noise could be subtracted from the test results.

3.6.2 Static Propulsor Modeling

This setup was re-created using the Propulsor Sizing and Evaluation Module described in Section 3.1. In addition to noise test data, Electra also provided sectional propeller geometry for reproduction in the methodology’s model. In this case, chord, twist, and other sectional blade geometry were available so the propeller could be directly processed as a surface model as described in Section 3.3.1. Two propulsor load methods were used when modeling the propulsor blade. Since the inflow was uniform and normal to the propulsor disk, the first method used a lifting-line model which resulted in sectional perpendicular and parallel forces along the local chord line. The second load model was a fast-vortex, fast-panel method. Despite the apparent simplicity of a statically operated propeller, the high back pressure generated behind the propulsor has the potential to influence the inflow ahead of the propeller. To explore these effects, a transient analysis was implemented in addition to the steady-state results. Figure 3.10 illustrates the steady and transient filament lines and 9 observer locations mirroring the test setup.

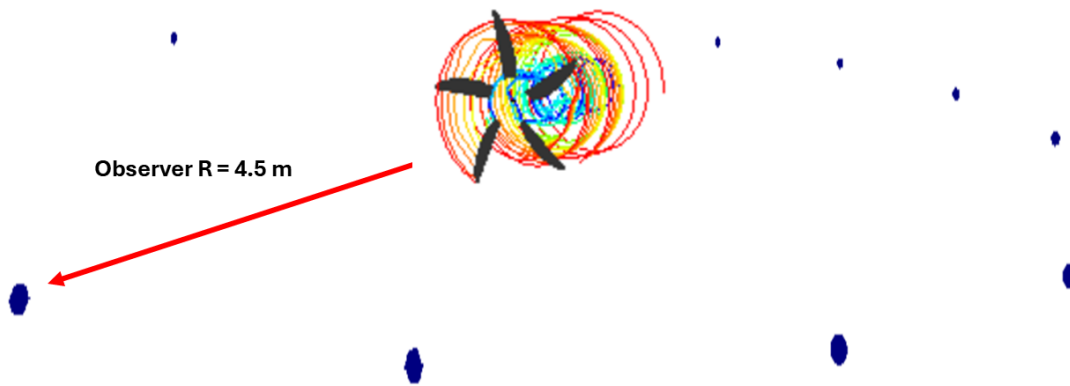


Figure 3.10: Vortex-filament model of the static propulsor test setup.

3.6.3 Static Propeller Modeled Noise Results

The measured noise was processed by removing 1 dB from all observers to account for the effects of ground proximity. An additional 1 dB was removed from microphones found at 180 and 90 degrees to account for noise reflections from two blast plates placed around the propeller.

Figure 3.11 depicts the modeled and measured noise results for the propulsor of radius A. Both $Radius_A$ and $Radius_B$ propellers show better agreement between modeled and measured noise at higher RPM values. Towards the front and rear observer locations of propeller $Radius_A$, the predicted noise was within 1–2 dB of the measured data at 4500 RPM and within 5 dB at 2000 RPM.

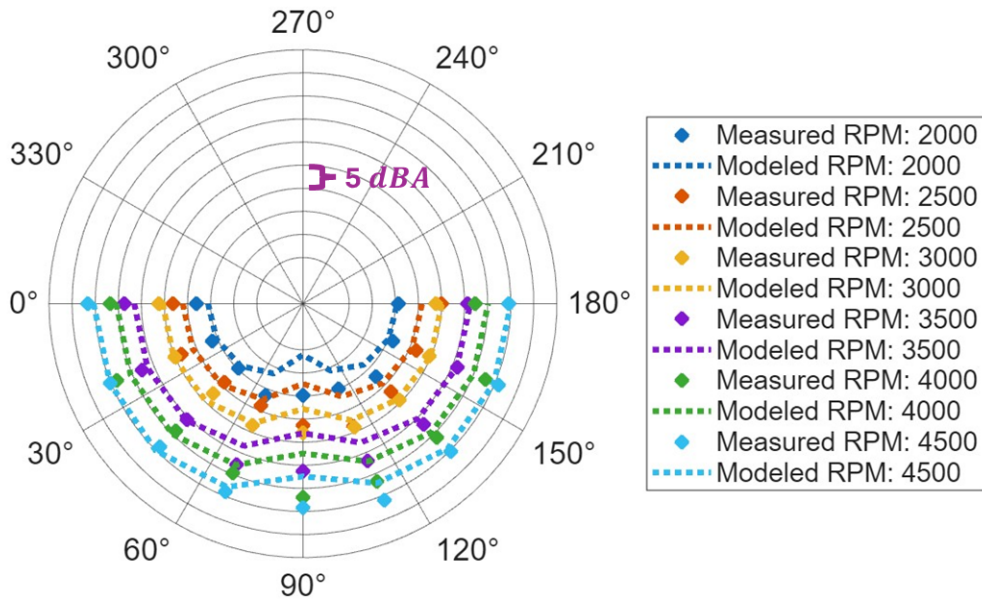


Figure 3.11: Modeled and measured noise directivity for propulsor $Radius_A$ at varying RPM. Radial increments represent 5 dB.

Figure 3.12 depicts the modeled and measured noise results for the propulsor of radius B. The propeller of $Radius_B$ varied within 2–4 dB at 3500 RPM and within 6 dB at 2000 RPM. For both propellers, the largest variation occurred at the 90deg location.

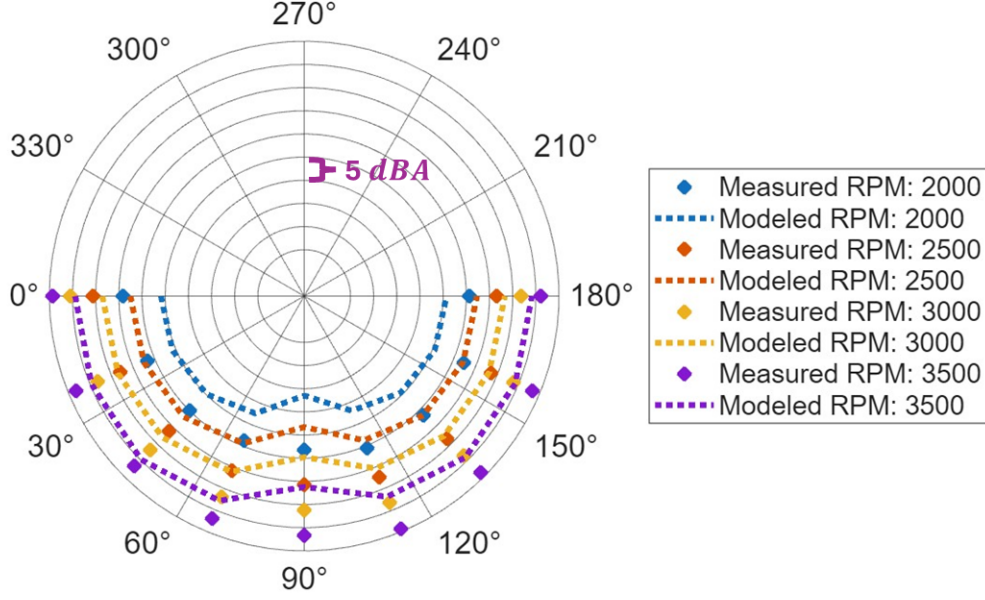


Figure 3.12: Modeled and measured noise directivity for propulsor $Radius_B$ at varying RPM. Radial increments represent 5 dB.

3.6.4 Dynamic Propulsor Noise Experiment

Additional validation was accomplished using propeller data taken from an anechoic wind tunnel at different inflow speeds, RPM values, and cross-flow angles, as described in Section 2.1.2. This condition is represented by a cross-flow angle that describes the magnitude of propulsor tilt with respect to normal inflow. The anechoic wind tunnel experiments included variations in both propulsor radius and blade count, with propulsors having either five or seven blades. Additional variations in blade geometry include the implementation of skew for the smaller radius propulsor. Skew is defined as a sweep of the propulsor blade centerline parallel to the propulsor disk plane. Instead of all blades extending radially from the hub, skew introduces a gradual sweep toward the blade tip, as defined by a skew angle. Wind tunnel velocity varied from 0–50 m/s, while RPM varied from 1000–4000 RPM in discrete increments. Cross-flow angles varied from 0–40 degrees, with 0 degrees corresponding to pure axial flow. Propulsor geometries and test parameters are summarized in Table 3.1.

Table 3.1: Anechoic wind tunnel test conditions and propulsor characteristics.

Testing Parameters:		
Inflow Speed (m/s)	0–50	
RPM Range	1000–4000	
Cross Flow Angle (deg)	0–40	
Propulsor Characteristics:	Propulsor A	Propulsor B
Radius	R_A	R_B
Blade Count	5	7
Skew	None	Tip

Measured noise was recorded using an array of six microphones placed above the propulsor along the X-axis on an approximate 6-m arc centered about the propulsor hub and sweeping from the front to the rear of the propulsor, as depicted in Fig. 3.13.

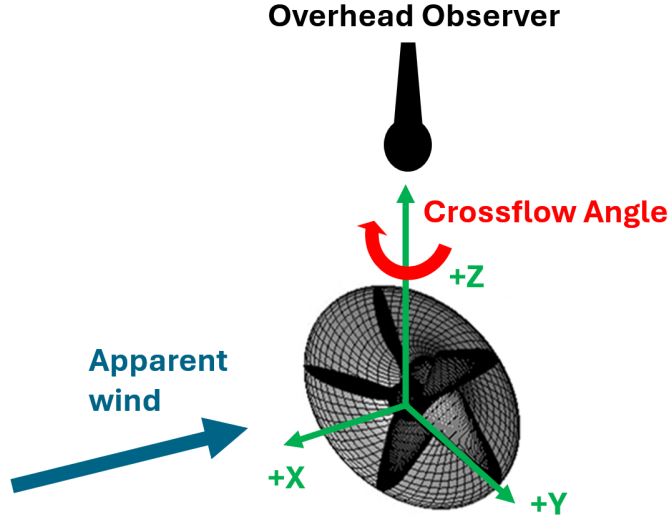


Figure 3.13: Anechoic wind tunnel test setup and microphone locations used for propulsor noise validation.

In this setup, cross-flow angle was varied by rotating the propulsor motor assembly clockwise about the positive Z-axis, with airspeed parallel to the wind tunnel walls referenced as apparent wind along the X-axis. For both propulsors, thrust was varied by changing RPM for a fixed blade pitch. Data loggers recorded real-time experimental thrust and torque. At each condition point of velocity, RPM, and cross-flow angle, steady-state noise was recorded

once the thrust and power readings stabilized. At each value of constant RPM, propulsor thrust and power were allowed to fluctuate as outputs dictated by the inflow. The results of the anechoic experiment were combinations of discrete velocity, RPM, and cross-flow-angle conditions and their corresponding noise, power, and thrust measurements.

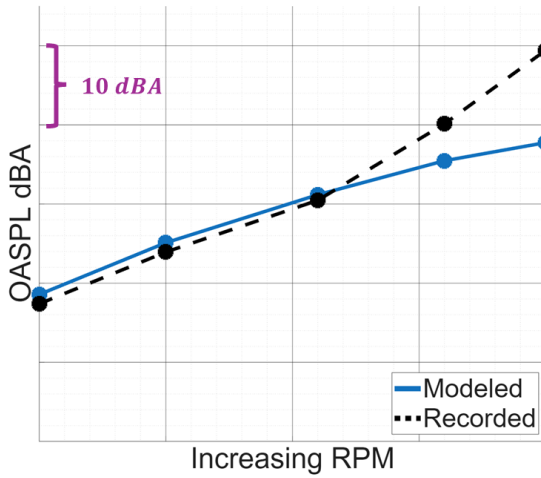
3.6.5 Dynamic and Cross-flow Propulsor Modeling

Both propulsors were modeled using the procedure described in Section 3.3.1. As in Section 3.6.2, radial chord, twist, and, in the case of the skewed prop, tangential offset distributions were provided for both propulsors. Cross-flow was accounted for by using an inflow model based on the Pitt-Peters unsteady actuator-disk theory, as outlined in Section 3.3.2. A narrow strip of observers was placed in a 6-m half-ring about the side of the propulsor to mirror the experimental microphone locations in the anechoic wind tunnel setup while maximizing observer-location density. The resulting propulsors were then modeled over the condition ranges summarized in Table 3.1.

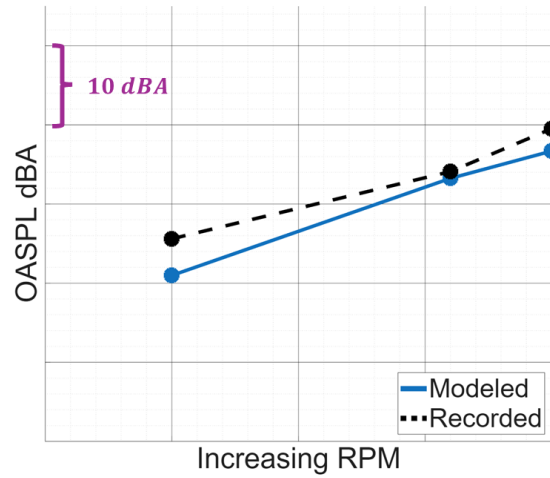
3.6.6 Dynamic Propulsor Modeled Noise Results

3.6.6.1 Noise Effects from Variation in RPM

Figure 3.14 compares modeled A-weighted OASPL against measured results for discrete, increasing RPM values. The two subfigures represent example low (Figure 3.14a) and high (Figure 3.14b) speed climbs at constant velocities. At all airspeeds, both modeled and measured noise levels increase linearly with RPM at a rate of about 5 dBA per 500 RPM. Modeled noise follows the measured values closely but diverges at higher RPM. For the low-speed climb example, noise matches within 2 dBA for the first three RPM levels but diverges at the fourth and fifth levels to more than 5 dBA. In a similar comparison, Figure 3.15 plots RPM and noise at low and high climb speeds for the seven-bladed propulsor. The seven-bladed propulsor features a greater offset of 5–10 dBA but otherwise follows the same trend of increasing noise with higher RPM.

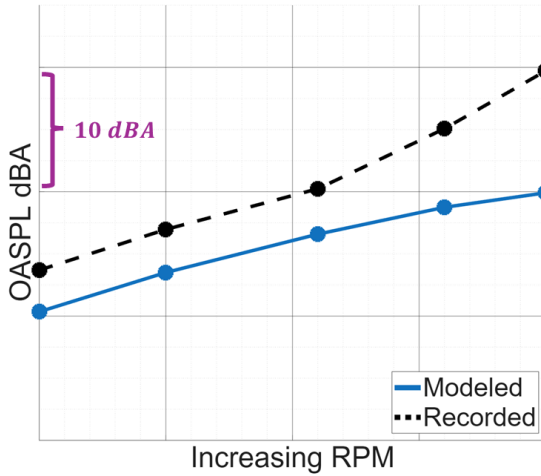


(a) Low Climb Speed

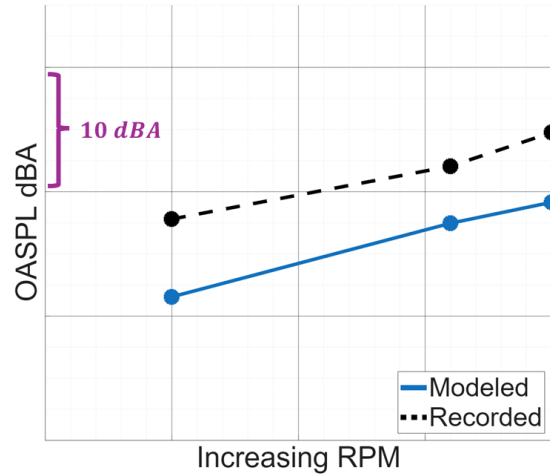


(b) High Climb Speed

Figure 3.14: Modeled and measured noise as a function of RPM for the five-bladed propulsor at low and high climb speeds.



(a) Low Climb Speed



(b) High Climb Speed

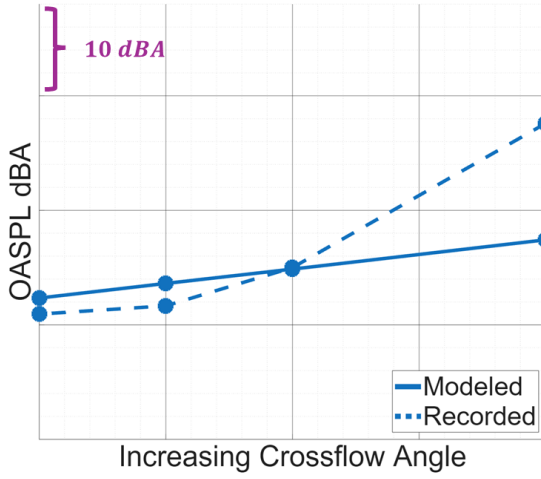
Figure 3.15: Modeled and measured noise as a function of RPM for the seven-bladed propulsor at low and high climb speeds.

Both modeled and experimental results are consistent with the previous static propulsor tests in Section 3.6.2. Furthermore, the anechoic tests expanded on the static findings by establishing that this effect applies at both low and high climb speeds. This establishes that

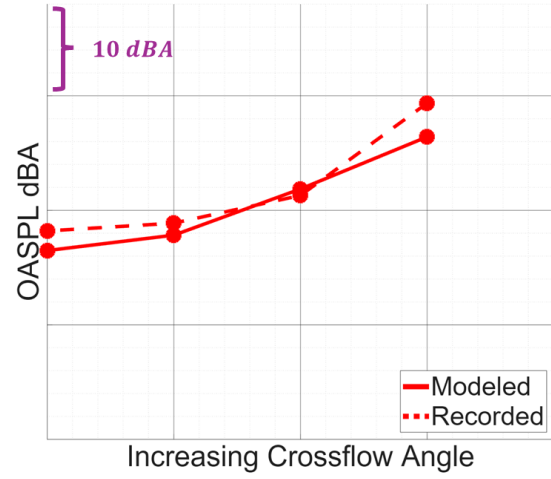
the noise-model framework in Section 3.3 shows sufficient resolution and accuracy to model noise during early flight segments, where community noise is most significant, and to capture operational changes in RPM and airspeed.

3.6.6.2 Noise Effects from Variation in Cross-flow Angle

Figures 3.16 and 3.17 show noise results from variations in cross-flow angle at low and high climb speeds for five- and seven-bladed propulsors, respectively. Both propulsors follow the trend of increasing noise with cross-flow angle. The five-bladed case shows slightly closer agreement, within 3 dBA at lower cross-flow angles, but eventually diverges to almost 10 dB at the highest angle. At the higher climb speed, the five-bladed case shows much closer agreement with the measured results, with all points within 3 dBA. These results establish that the model is capable of accounting for the effects of cross-flow, although most of the vehicles considered in this thesis operate in either mostly axial or highly edgewise flow and are not likely to be in these conditions of gradual cross-flow. Finally, the increased variation between modeled and experimental noise may result from the more complex seven-bladed geometry, which features skew that is not accounted for by the actuator-disk- and BEMT-based tool used in this methodology.

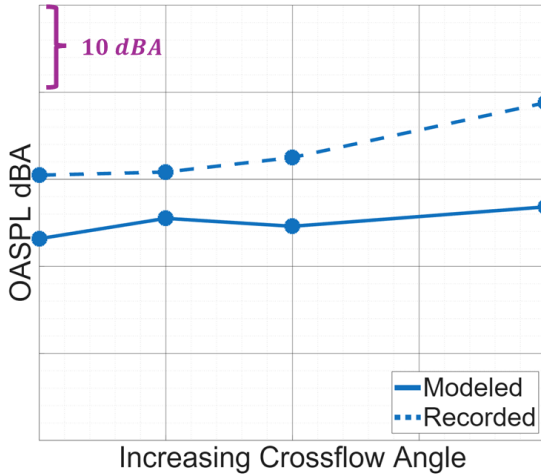


(a) Low Climb Speed

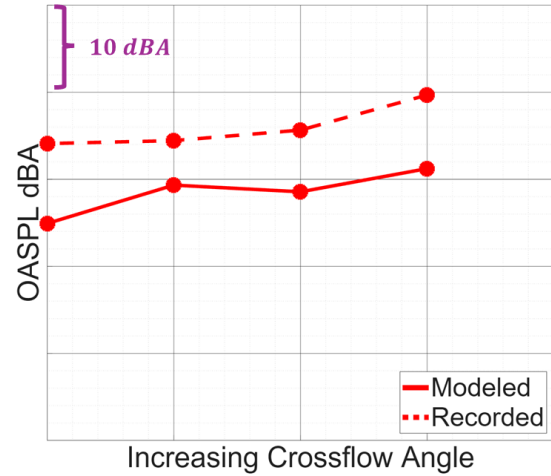


(b) High Climb Speed

Figure 3.16: Modeled and measured noise as a function of cross-flow angle for the five-bladed propulsor at low and high climb speeds.



(a) Low Climb Speed



(b) High Climb Speed

Figure 3.17: Modeled and measured noise as a function of cross-flow angle for the seven-bladed propulsor at low and high climb speeds.

3.7 Propulsor Design Validation

The methodology's propulsor design and performance evaluation capabilities, described in Sections 3.1.1 and 3.1.2, are validated against experimental flight test data from an existing

vehicle. While the Source Noise Modeling Validation in Section 3.6 uses known propulsor geometry and operating states, this validation focuses on propulsor design. Representative propulsors based on overall vehicle performance requirements and characteristics, as outlined in Section 3.1, are created for an existing lift-plus-cruise vehicle. Performance targets such as climb angle, airspeed, and RPM are known, while vehicle characteristics such as weight, wingspan, and propulsor arrangement are obtained from publicly available sources and studies. Recorded noise values from flight tests were available through industry collaboration. The following section describes how a surrogate propulsor was generated based on vehicle geometry and propulsion layout to match the overall performance capabilities of the vehicle. The model was then analyzed using the Source Noise Modeling Module, and the results were compared against test data.

3.7.1 Experimental Flight Test

The lift-plus-cruise vehicle was flown above an array of ground-based microphones in two flight conditions. The first was a stationary hover using only the lift propulsors, with no vertical airspeed. The second condition was a horizontal flight transition with both lift and cruise propulsors operating and the wings generating lift from forward airspeed. Operating conditions such as airspeed, power, and RPM were recorded onboard throughout the flight test. The ground noise was back-propagated to a 1-m source-noise hemisphere. Two angles (39° and 45°) of elevation are depicted, representing the vertical angle below the vehicle plane.

3.7.2 Representative Propulsor Design

For this validation, the propulsor count, blade count, and diameter were known. This vehicle was configured with 12 horizontal twin-bladed lift propulsors and one vertical three-bladed cruise propulsor. The lift propulsors were positioned at the leading and trailing edges of the wing in two rows of six. All 12 lift propulsors had radii of 0.65 meters, while the single cruise propulsor had a radius of 0.99 meters. Other known vehicle characteristics include

the estimated weight of 1270 kg and its wingspan of 10.97 meters, as sourced from [11].

The vehicle’s operating states were known for two flight configurations: hover and transition. For hover, the vehicle was assumed level, with the cruise propulsor inactive, zero horizontal velocity, and no lift generated by the wing. Transition flight was conducted at a level, forward velocity with both lift and cruise propulsors operating and lift produced by both the lift propulsors and the wing. Propulsor RPM values during these two conditions were provided through industry collaboration along with the noise data. Using this information, the propulsor design procedures described in Sections 3.1 and 3.3.1 were used to create propeller blade geometry that matched the required thrust and power levels of the available data at the corresponding RPM and operating states. The results were propulsors with known radial chord and twist distributions that matched the available performance requirements of the vehicle. The lift-plus-cruise propulsor design procedure is discussed in more detail in Section 5.1.1. Propulsor characteristics and operating conditions are summarized in Table 3.2.

Table 3.2: Lift-plus-cruise vehicle, propulsor, and operating characteristics used for validation.

Vehicle Characteristics	
Lift Propulsor Configuration	12 Wing-Mounted
Cruise Propulsor Configuration	1 Empennage-Mounted
Vehicle Weight (kg)	1270
Span (m)	10.97
Propulsor Characteristics	
Lift Propulsor Blades	2
Lift Propulsor Radius (m)	0.65
Cruise Propulsor Blades	3
Cruise Propulsor Radius (m)	0.99
Operation Stages	
<i>Hover</i>	
Lift Propulsor RPM	2700
Cruise Propulsor RPM	0
<i>Transition</i>	
Lift Propulsor RPM (Stage 1)	1370
Lift Propulsor RPM (Stage 2)	1760
Cruise Propulsor RPM	1750

3.7.3 Propulsor Design Modeled Noise Results

Figure 3.18 is a polar plot overlaying the modeled ABEAT results and measured noise as A-weighted OASPL. The vehicle is in a hover state with zero vertical or horizontal motion and only the lift propulsors operating. The LPC vehicle is centered as depicted, and all noise observer points correspond to a 10-meter radial distance. Two elevation angles (39° and 45°) are depicted, representing the vertical angle below the vehicle plane. In both modeled and measured results, noise is relatively omnidirectional, varying less than 5 dBA in magnitude azimuthally. Modeled results generally underpredict noise by 2–7 dBA.

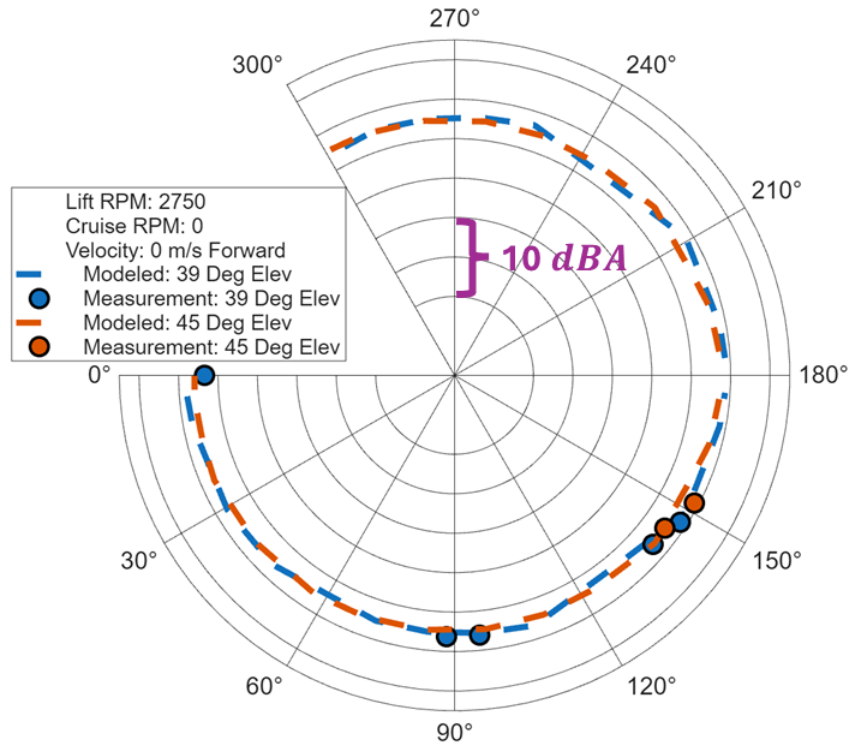


Figure 3.18: Modeled and measured hover noise at two elevation angles and a 10-m radial distance from the vehicle.

Noise results at a transition state are shown in Figure 3.19. In this condition, the vehicle is moving at a 29 m/s horizontal speed. Thrust is provided by the cruise propulsor, while lift is provided by the wing and lift propulsors, with the lift propulsors operating at 1370 and 1760 RPM for the two transition stages. Similar to the hover state, noise varies little

azimuthally. Modeled noise agrees more closely with the measured results, with less than 3 dBA of variation in magnitude.

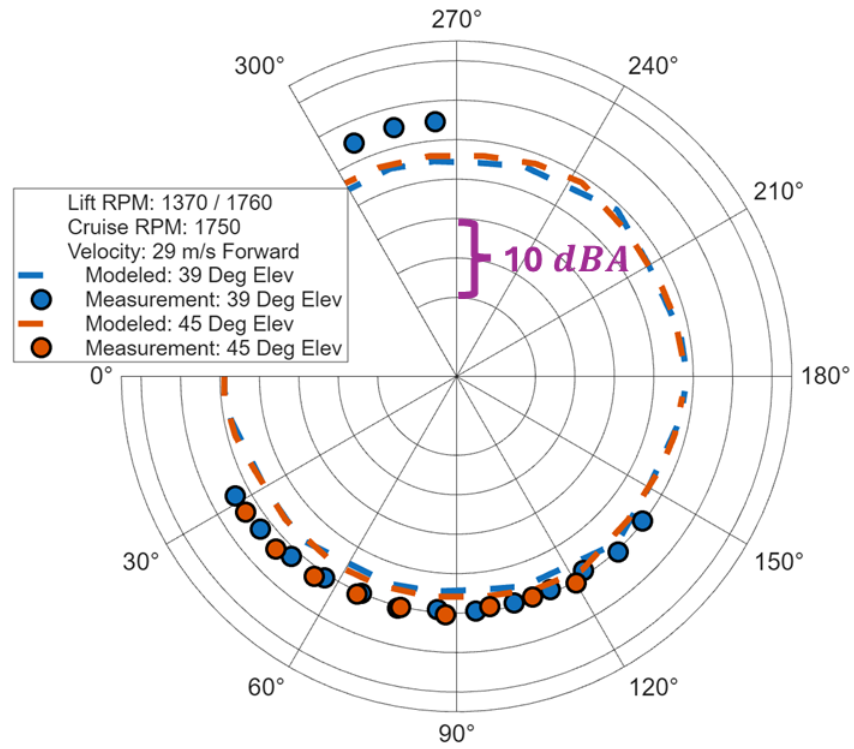


Figure 3.19: Modeled and measured transition-flight noise at two elevation angles and a 10-m radial distance from the vehicle.

Chapter 4

Impact of Design and Operation of Distributed Propulsors to Aircraft Noise Emission

Advanced Air Mobility (AAM) is a popular and rapidly developing concept in the field of urban air transportation. Applications such as air taxis are promoted as ways to ease congestion in population centers, whereas other implementations include delivery services to urban and suburban areas [91]. Advances in electrification have allowed the implementation of distributed electric propulsion (DEP) while avoiding weight limitations of conventional engines, which would otherwise require gearing and drivetrains to achieve comparable propulsor architectures. DEP layouts have allowed the creation of many different designs for AAM vehicles [96, 11]. One such configuration is the blown-flap STOL aircraft, which can take off and land in significantly shorter distances than those of conventional aircraft. STOL vehicles are often proposed as a way to transport passengers and cargo efficiently over longer distances than lifting-rotor vehicles [28, 29]. These scenarios, however, involve operating propulsors in close proximity to areas of human habitation, which can potentially expose people to increased levels of nuisance noise [91, 93].

Noise is a significant factor in the social and regulatory acceptance of urban vehicles and their applications. For many aircraft, propulsors are the dominant source of noise during flight [91]. To support these efforts, propulsor noise modeling is an essential step in evaluating the potential noise impact of a vehicle design. However, preliminary propulsor noise modeling is challenging due to the lack of detailed propulsor geometry required by most existing noise tools and methodologies. Furthermore, DEP has allowed propulsors to be smaller and

operate at different tip speeds than those of conventional vehicles, which limits the utility of existing noise databases and models developed for airplanes and helicopters. Blown-flap STOL vehicles pose additional challenges because lift augmentation depends on propulsor layout, and modifications to the propulsor arrangement potentially alter vehicle performance.

This chapter utilizes the methodology presented in Chapter 3 to design propulsor architectures for a distributed electric propulsion (DEP) vehicle and model propulsor noise across various operating conditions and propulsor geometries. The methodology was applied in a parametric study to investigate how variables such as tip speed, blade count, and blade radius affect the source noise of a single propulsor operating at a fixed thrust and flight speed. The rapid propulsor design capability enables parametric sensitivity studies of the effects of geometric and operational variables on noise. The DEP architecture design capabilities presented in Section 3.2 were then used to assess the effects of propulsor count on noise for a representative blown-flap short takeoff and landing (STOL) vehicle. This approach couples vehicle performance and noise with both individual propulsor design and overall propulsor layout and explores the noise emitted by propulsors designed for vehicles with performance augmented by blown lift.

Effective noise modeling for DEP vehicles requires the ability to account for their unique propulsor geometries, operating conditions, and the impacts of propulsor layout on performance and noise. DEP introduces additional complexity because propulsor geometry and arrangement influence not only acoustic behavior but also overall aircraft performance. This further emphasizes the need to study propulsors both as individual components and as integrated elements within a vehicle configuration. Although numerous experimental and analytical studies have investigated the effects of propulsor geometry and operating conditions on noise and performance, these efforts are often limited to existing propulsors. To serve as an effective preliminary design tool, noise modeling methodologies must be capable of generating and evaluating novel propulsor arrangements with sufficient case density and

detail to support trade studies and sensitivity analyses.

This chapter presents two studies that utilize the methodology to explore the effects of propulsor design, operation, and layout on noise and performance, with application to a representative blown-flap STOL vehicle. Since both studies focus on propulsor design, only propulsor source noise is considered, which excludes airframe or propagated community noise. The findings are valid for applications in which propulsor noise is dominant, such as during departure. Section 4.2 presents the first single-propulsor study, which uses the Propulsor Sizing and Evaluation Module in Section 3.1 to conduct a sensitivity analysis of noise with variations in propulsor geometry and operating conditions. Here, propulsors are designed to meet common thrust and airspeed requirements while varying geometric and operational parameters. This results in propulsor designs optimized for their respective design conditions and ensures that noise variations are due to the design variables rather than the evaluation of propulsors at off-design or extreme conditions. The relatively low computational cost of propulsor load generation and noise modeling described in Sections 3.3.2 and 3.3 allows the creation and evaluation of a large number of propulsor designs. This resulted in the evaluation of a wide design matrix comparing many combinations of blade count, RPM, and propulsor radius. The resulting contours of geometric and operational parameters show the sensitivity of noise and power to changes in these variables. Finally, Section 4.3 presents the second study, in which the methodology is used to design variations in propulsor architectures for a DEP blown-flap STOL vehicle and evaluate the resulting modeled propulsor noise.

4.1 Propulsor Design and Noise Modeling

4.1.1 Overview

This chapter utilizes three methodology modules described in Section 3: Propulsor Sizing and Evaluation, DEP Arrangement Design, and Source Noise Modeling, as illustrated in Fig. 4.1. The Propulsor Sizing and Evaluation Module takes as inputs a set of vehicle characteristics,

propulsor performance requirements, and known propulsor geometry. Vehicle characteristics include parameters such as aircraft weight, lift and drag characteristics, and wing geometry, which are used to model aircraft kinematics and define its operational envelope. Additional information, when available, such as motor RPM or power limits, can be incorporated to impose further design restrictions.

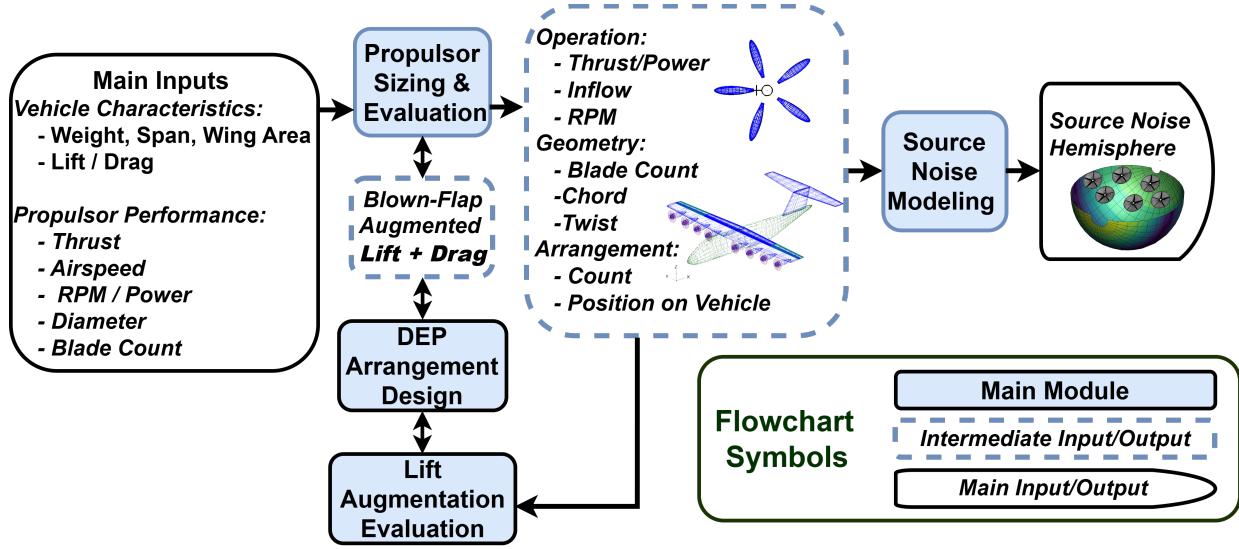


Figure 4.1: Methodology modules used for propulsor design, DEP arrangement design, and source-noise modeling.

Propulsor performance is defined by parameters such as nominal RPM, thrust and/or power, and airspeed, which are used to size and design the propulsor. Known or existing propulsor geometry and operating conditions may also be provided as direct inputs for propulsor evaluation and off-design performance analysis. For lift-augmented applications, such as blown-flap STOL aircraft, the DEP Arrangement Design Module modifies propulsor and aircraft performance to account for the effects of propulsor count and arrangement. The outputs of these modules include detailed propulsor geometries and, for DEP vehicle analyses, their respective installations and arrangements on an aircraft.

The Source Noise Modeling Module computes propulsor loads using the propulsor blade surface geometries and inflow conditions, then models broadband and tonal noise using

Farassat Formulation 1A and BPM methods. The output of this module is a local source noise hemisphere. Although not the focus of the current study, the methodology presented in this chapter has been used to generate time-discrete kinematics-based flight profiles, model corresponding noise hemispheres, and propagate total operational noise into community noise contours [49].

4.1.2 Distributed Propulsion Blown-Flap Propulsor Evaluation

Distributed propulsion affects propulsor design by altering the aerodynamic performance of blown-flap STOL vehicles. Changes in propulsor count alter the resulting lift augmentation; therefore, the thrust and power required to maintain prescribed performance requirements depend on the DEP arrangement. The following analysis uses the procedure and formulation described in Section 3.2.1 to account for the effects of DEP on lift-augmented blown-flap STOL vehicles.

4.1.2.1 Effects of Lift Augmentation

Conventional dynamic-pressure-based lift augmentation models become less accurate as propulsor diameter decreases relative to the wing chord. Instead, this analysis employs the semi-empirical deflected jet-sheet formulation developed by Courtin [29], which better represents the aerodynamic interaction between the flap, freestream, and deflected propulsor jet for distributed propulsion configurations.

As detailed in Section 3.2.1.1, the augmented lift coefficient, C_L , and net streamwise force coefficient, C_x , are determined using Eqs. (3.1) and (3.2). These equations are functions of vehicle angle of attack, flap deflection, propulsor thrust coefficient, T_C , and jet momentum excess coefficient, ΔC_J , defined by Eqs. (3.3) and (3.4), respectively. The jet momentum excess coefficient depends on the ratio of jet velocity to freestream velocity and the effective disk height, h_d , given by Eq. (3.5). Disk height is a function of propulsor count and total blown disk area and is, therefore, affected by the DEP arrangement design. The jet

deflection angle, δ_J , is determined from the flap deflection and disk height using Eq. (3.6) [29]. Together, these relationships capture how changes in the DEP architecture modify the aerodynamic forces generated by the blown wing.

The resulting C_L and C_x are combined with aircraft weight to determine lift and drag forces on the vehicle during flight. This force balance is evaluated using the time-discrete flight analysis described in Section 3.2.1.1, allowing changes in propulsor arrangement, thrust, and lift augmentation to be reflected in the aircraft kinematics and required propulsor operating conditions.

4.1.2.2 Takeoff Performance Evaluation

Lift augmentation has the greatest influence during high-lift flight segments, where conventional assumptions of constant α_{max} or $C_{L_{max}}$ are not applicable because the available lift and drag depend on propulsor thrust, power, and airspeed. The takeoff performance methodology therefore uses the augmented C_L and C_x formulation described in Section 3.2.1.1 to couple propulsor operation with blown-flap aerodynamic performance [29].

The resulting aerodynamic forces are used when calculating ground roll and rotation using Eq. (3.7) and during climb using Eq. (3.8). The climb formulation is applied once vehicle lift equals or exceeds weight. Kinematic equations are used to determine an aircraft’s departure profile based on the available propulsor thrust and prescribed performance requirements, including ground-roll and obstacle-clearance distances, as described in Section 3.2.1.2. This allows changes in DEP configuration and propulsor operation to be reflected directly in both lift augmentation and takeoff performance.

4.1.3 Propulsor Interaction Effects

DEP arrangements present additional complexity due to interactions between adjacent propulsors and the wing. Care must be taken to model these factors or mitigate their effects by positioning and operating propulsors in ways that minimize potential interaction noise. One

such source is propulsor-wing interaction, which refers to the influence of a downstream wing on the inflow of upstream propulsors. This interaction is primarily caused by viscous-wake and potential field effects. Experimental measurements in the existing literature indicated that arrangements with propulsor-wing horizontal separation beyond 0.25 propulsor radii and vertical separation greater than 1.25 propulsor radii (depicted as x and z in Fig. 3.3) exhibited minimal interaction noise, with total arrangement noise comparable to isolated propulsor measurements [110]. A second interaction mechanism is propulsor-propulsor interaction, caused by the influence of induced velocity fields from adjacent propulsors. This interaction is sensitive to propulsor spacing, as illustrated in Fig. 4.2. Experimental data have shown that for separation-to-diameter ratios, S/D , beyond 1.04, propulsor noise is invariant to further increases in spacing [104].

Additional interactions were considered but were found to have minimal impact for the DEP arrangements considered in this chapter. Propulsor-wake interactions occur when the wake and vortices of forward propulsors impinge on the inflow of downstream propulsors [110]. However, the DEP arrangements considered in this study do not include downstream propulsors, and all evaluated conditions correspond to forward flight with minimal non-axial inflow.

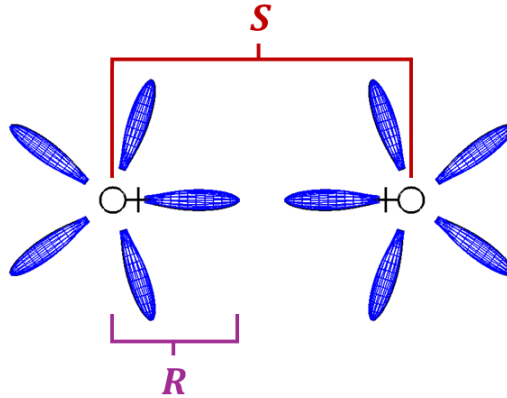


Figure 4.2: Definition of center-to-center propulsor spacing, S , and propulsor radius, R .

Propulsor-only noise is the primary focus of this chapter. Therefore, the results are most

applicable to flight conditions where propulsor noise dominates over airframe noise and for propulsor arrangements that minimize the magnitude of interaction noise.

4.1.4 Propulsor Sizing and Evaluation Module

Blade chord and twist distributions are generated for a specified propulsor diameter, blade count, RPM, power, and airspeed to minimize induced losses. Additional details, when available, may also be specified, including c_l distribution, advance ratio, torque, and desired thrust. The propulsor design condition depends on the vehicle architecture. VTOL propulsors are typically sized to satisfy hover requirements, whereas STOL propulsors are sized for takeoff or cruise conditions. This portion of the framework is also capable of modifying existing geometries. At the design condition, circulation is optimized until a minimum induced-loss condition is met [33, 15]. For the Source Noise Modeling Module, the relevant inputs from the Propulsor Sizing and Evaluation Module are the radial distributions of chord, $c(r)$, and blade twist, $\beta(r)$, which are used to generate a 3D propulsor. The use of XROTOR provides additional information such as coefficient of thrust, C_T , solidity ratio, and shaft power.

4.1.5 Propulsor Noise Modeling

Propulsor noise is modeled using the procedure described in Section 3.3. Tonal noise is modeled by solving the Ffowcs Williams–Hawkings equations (FW-H) [43] using Farassat Formulation 1A [39]. Broadband noise is modeled using the semi-empirical Brooks, Pope, and Marcolini method (BPM) [18]. These are implemented in this framework through NASA’s ABEAT tool within the Aircraft NOise Prediction Program 2 (ANOPP2) [74, 17]. ABEAT calculates propulsor loads using BEMT methods and uses functional modules to calculate broadband and tonal noise generated during propulsor operation [65]. Within ANOPP2, ABEAT can be used as a source noise module when modeling community noise using flyover profiles. For the purpose of this chapter, source noise alone is sufficient for the comparison of propulsor designs. The broadband and tonal noise are combined to generate the aircraft

source noise output at local observer locations. Depending on the application, observer geometry can range from a two-dimensional ring to a full 3D hemisphere, as shown in Fig. 3.8.

In this example, observer points are represented as nodes at the intersections of latitudinal and longitudinal lines. Noise is represented as color-mapped contours on the surface of the hemisphere. A vehicle is depicted alongside the hemisphere to orient the propulsors with respect to the observer grid. The center of the propulsors is coincident with the center of the hemisphere. These source noise hemispheres can be further processed to obtain noise metrics including proportional-band spectrum (PBS), narrow-band spectrum (NBS), power spectral density (PSD), and overall sound pressure level (OASPL).

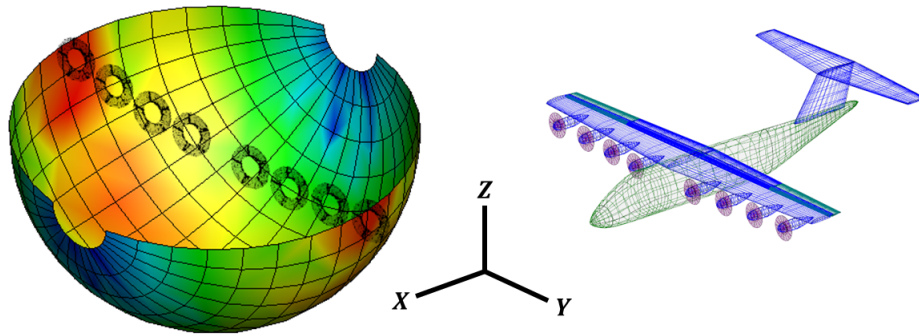


Figure 3.8 (repeated).

4.2 Noise Effects of Propulsor Geometry

This section demonstrates the methodology’s ability to model the effects of propulsor geometry and operating conditions on propulsor noise. All propulsors were designed for a fixed vehicle architecture with identical values of thrust and airspeed. Geometric parameters include blade count, blade radius, and chord distribution, whereas operational parameters include tip speed. Potential contributors to noise were hypothesized to include tip Mach number and propulsor solidity ratio [52]. Tip speed, represented by M_{Tip} , is a function of blade radius and rotational speed, Ω . Experimental and computational studies of rotorcraft have shown

that tip speed is a dominant factor in trailing-edge broadband noise, scaling to the 5.5th power of M_{Tip} [69]. Other studies have suggested that tip speed influences tonal noise by increasing the prominence of blade passage frequency harmonics and influences broadband noise through contributions from tip vortex noise [45] and increased overall blade loading [12]. Propulsor solidity ratio is defined as the ratio of total blade area to the swept disk area and can be controlled through changes in blade aspect ratio and blade count. Solidity ratio has been hypothesized to influence tonal noise by reducing blade loading, while affecting broadband noise through increased blade-wake interaction and trailing-edge turbulence associated with higher blade counts [55, 12].

Torque, RPM, and power limits were imposed by motor design, where applicable, whereas the required thrust was specified by the representative vehicle’s climb and takeoff requirements. The methodology’s ability to accept top-level aircraft requirements and generate a matrix of designs makes it well suited for sensitivity analyses aimed at developing parametric relationships between propulsor geometry, operating conditions, and noise. These studies demonstrate how the propulsor modeling and noise prediction modules described in Sections 3.1 and 3.3 can be applied to propulsor design for a complete vehicle.

4.2.1 Single Propulsor Design Generation

Table 4.1 lists the characteristics of a representative STOL vehicle propulsor based on values found in [29]. Blade count, propulsor radius, and RPM were the primary design parameters evaluated in this study. Figure 4.3 depicts wire-mesh examples of propulsor designs with constant radius and RPM but varying blade counts and solidity ratios. Blades are pitched to provide the required thrust for the takeoff condition. The range of torque, RPM, and power was constrained by motor limits, whereas the required thrust was specified by the vehicle’s climb and takeoff requirements. Each variable combination was used to design a propulsor using the process described in Section 3.1. The inflow was simplified and assumed to be purely axial for all operating conditions. An intermediate design condition at 48.9

m/s and 745 N per propulsor was selected to establish an initial propulsor design. This design was subsequently evaluated for stall at takeoff conditions by verifying that sectional lift coefficients did not exceed 1.5 and for windmilling behavior at cruise. If these conditions were not satisfied, the propulsor blade chord and twist distributions were modified until the requirements were met. This process generated a matrix of propulsor designs capable of meeting the required performance at both takeoff and cruise.

Table 4.1: Single-propulsor design matrix and operating conditions.

Propulsor Design Variables:			
Blade Count	3–9		
Radius (m)	0.30–0.60		
RPM	1000–5000		
M_{Tip}	0.30–0.80		
Power (kW)	43.6–62.5		
Performance Characteristics (Per Propulsor):	Takeoff	Design	Cruise
Thrust (N)	1018	745	471
Forward flight speed (m/s)	19.5	48.9	78.2

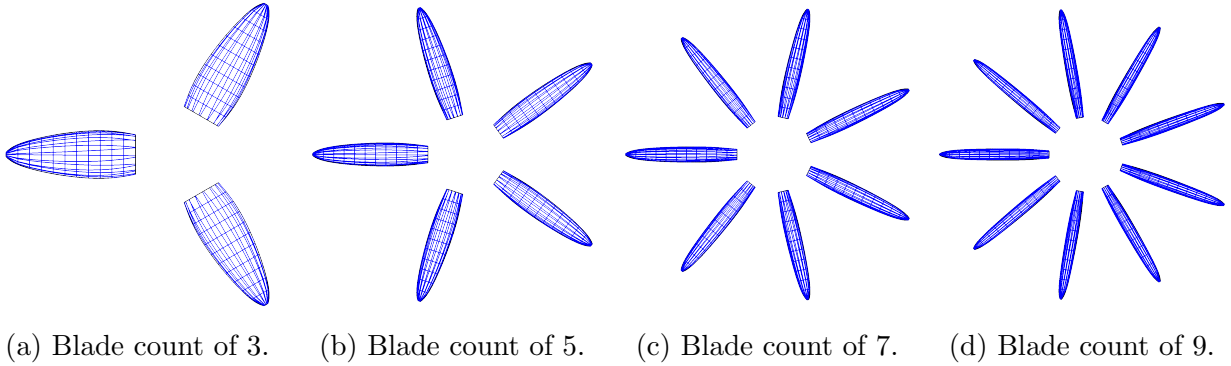


Figure 4.3: Propulsor designs with varying blade count at a radius of 0.4 m, solidity ratio of 0.24, and 4000 RPM.

Each of the candidate propulsors was modeled as described in Section 3.3. Propulsor loads were calculated for a single propulsor operating at a constant velocity. The modeled flight speed corresponds to the vehicle’s takeoff segment, where propulsor noise would be dominant over airframe noise. Modeling only a single propulsor also enabled the use of a simplified

uniform inflow model for propulsor load calculations, since there was no off-axis velocity. These loads were then used to generate tonal and broadband noise as described in Section 3.3. Noise observers were arranged on a hemisphere to capture directivity effects, particularly at sideline, undertrack, fore, and aft locations. The observer radius was set to five times the propulsor diameter to ensure that the far-field noise characteristics were captured and remained independent of the hemisphere size.

4.2.2 Propulsor Geometry Noise Results

The number of propulsor designs capable of satisfying the design requirements decreased as propulsor radius and blade count decreased and as the propulsor blades became more heavily loaded. Noise was highest at observer positions in the disk plane. Although forward and aft observer stations had lower noise levels than the sideline and undertrack positions, all four locations shared similar noise trends with changes in blade count, propulsor radius, and M_{Tip} . Across all combinations of blade count and radius, M_{Tip} was the most sensitive parameter. Increasing blade count decreased the dominance of M_{Tip} , with designs of the same radius exhibiting lower noise as blade count increased. To further analyze these sensitivities, the following three sections isolate operational and geometric parameters with respect to propulsor power. In this context, power is defined as the product of propulsor torque and rotational speed, Ω . Finally, it should be noted that this study investigates noise from propulsor geometries *optimized* for their respective operating conditions and designed to satisfy identical performance requirements while minimizing induced losses. This differs from analyses of propulsors operating at off-design RPM conditions or comparing propulsors created for different design points.

4.2.3 Effects of Operation

Figure 4.4 shows undertrack noise as a function of tip speed and power for radii of 0.4, 0.5, and 0.6 meters. For all propulsor radii, higher tip speeds increased noise levels at a given power. For a required power of 46.1 kW and a blade radius of 0.5 meters, a 0.16 increase

in M_{Tip} corresponded to a 15 dBA increase in noise. At a constant tip speed, increases in propulsor power also increased noise. For propulsors with a radius of 0.4 meters operating at an M_{Tip} of 0.63, a 1.1 kW increase in required power produced an 11 dBA increase in noise. These effects became more pronounced at higher tip speeds. For a propulsor radius of 0.6 meters and an M_{Tip} of 0.31, a 5.8 kW increase in power resulted in a negligible increase in noise, whereas at an M_{Tip} of 0.75, a 0.3 kW increase in power produced a 4 dBA increase in noise.

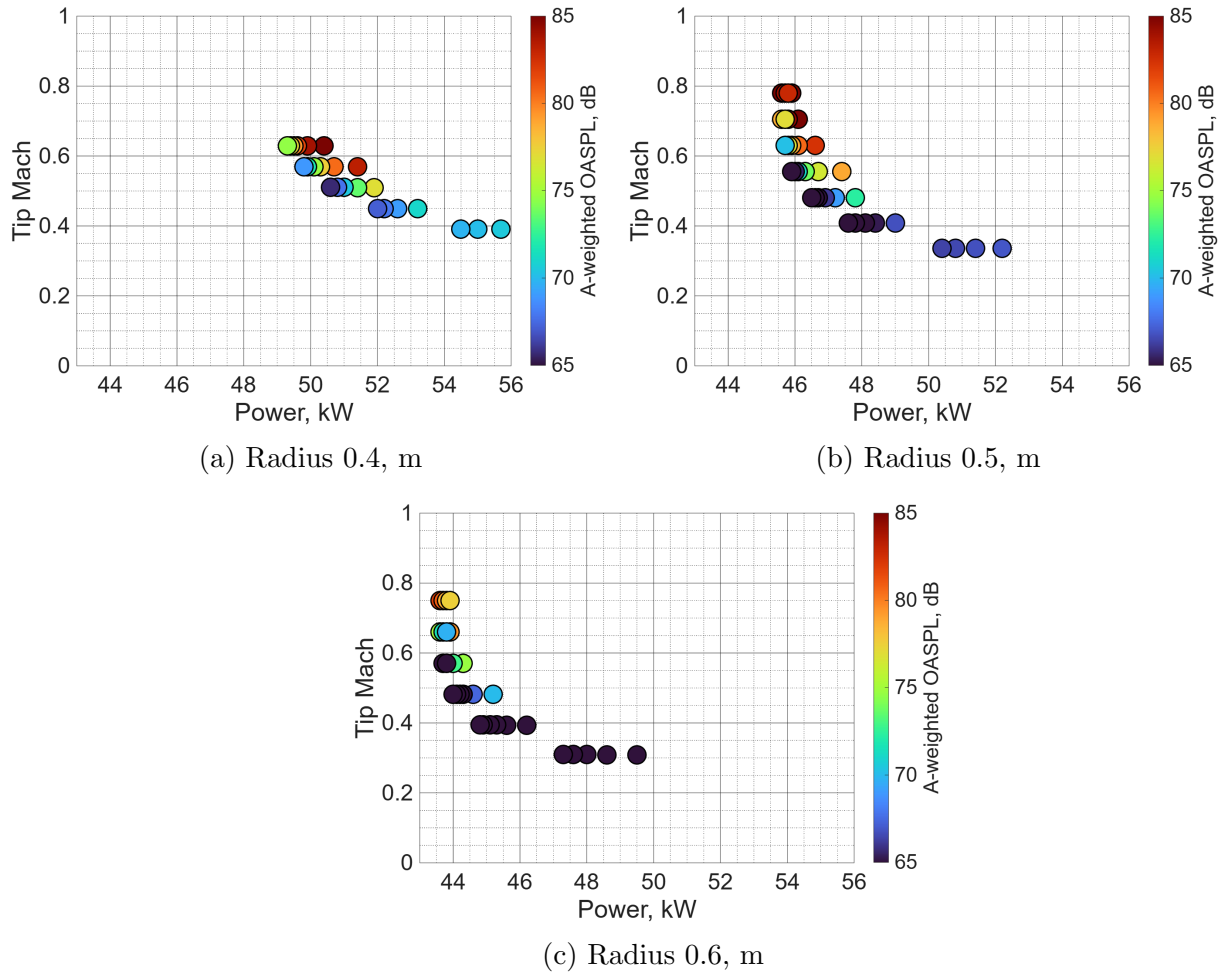


Figure 4.4: Undertrack A-weighted OASPL as a function of tip Mach number and propulsor power for radii of 0.4–0.6 m at constant thrust.

Increasing propulsor radius reduced the required power across all tip speeds. For a radius of 0.6 meters, design power ranged from 43–50 kW, whereas at a radius of 0.4 meters, design

power ranged from 47–56 kW. However, propulsor radius had a smaller influence on the magnitude of noise compared to tip speed and power. Overall, propulsor designs with lower tip speeds produced less noise but required more power.

These results are consistent with experimental and modeled studies that observed a similar noise sensitivity to changes in tip speed. Li and Lee experimentally measured a 5 dB increase in propulsor OASPL for an M_{Tip} increase from 0.5 to 0.6 [69]. For blade counts between 3 and 6, Zolbayar similarly reported a roughly 10 dB increase in propulsor OASPL per 0.1 increase in M_{Tip} for tip Mach numbers between 0.3 and 0.7 [111]. The results of this study indicate that low-noise propulsor designs would have a large blade radius and operate at a low tip speed. Propulsors operating at higher tip speeds would require smaller blade radii to produce a comparable noise level.

4.2.4 Effects of Blade Geometry

Figure 4.5 depicts noise results as a function of propulsor solidity ratio and power for propulsor radii ranging from 0.4 to 0.6 meters. Solidity ratio was used to evaluate the combined impacts of blade count and propulsor radius, enabling a better understanding of how blade geometry influences noise while maintaining the same thrust. For all propulsor radii, higher solidity ratios resulted in lower noise levels. At a given solidity ratio, increasing power increased noise, although this effect diminished as solidity ratio increased above approximately 0.5. Increasing propulsor radius generally decreased required power for a given solidity ratio, while also reducing the number of converged designs with high solidity ratios. For a radius of 0.6 meters, required power ranged from 43–50 kW for solidity ratios between 0.05 and 0.55, whereas at a radius of 0.4 meters, required power ranged from 49–56 kW for solidity ratios between 0.2 and 0.8. Higher values of solidity ratio were generally associated with lower noise but required higher power.

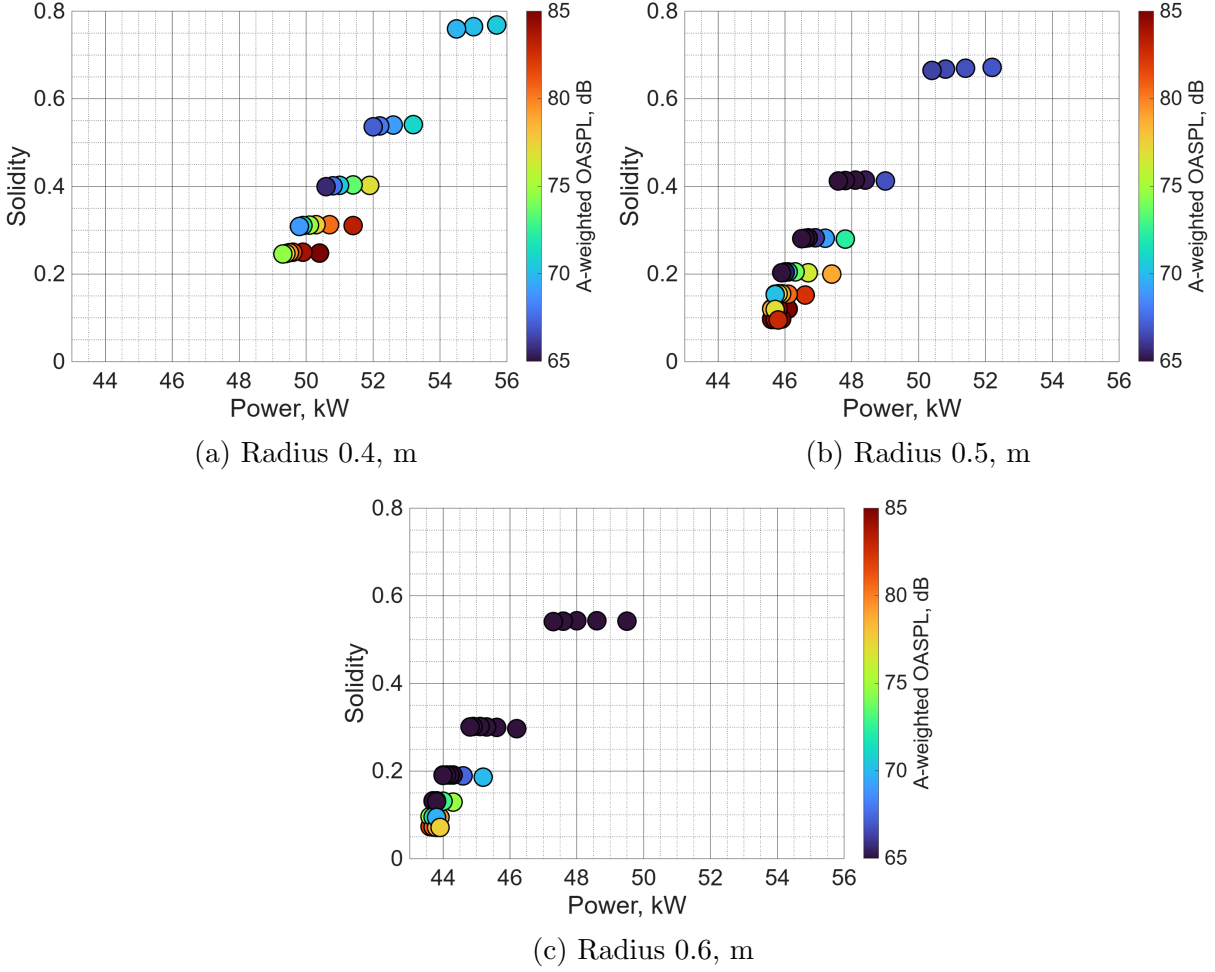


Figure 4.5: Undertrack A-weighted OASPL as a function of solidity ratio and propulsor power for radii of 0.4–0.6 m at constant thrust.

These results agree with existing experimental and computational studies that investigated changes in solidity ratio resulting from variations in blade count and radius. Baskaran et al. found that for propulsors at a tip speed of Mach 0.2094, a 0.039 increase in solidity ratio from 2 to 3 blades reduced propulsor noise by up to 5 dB OASPL, whereas a further 0.077 increase to 5 blades reduced noise by another 6 dB OASPL across a range of tip speeds [12]. The same study also found that increasing solidity ratio increased required power for fixed thrust and RPM. Jardin et al. observed in both modeled and experimental studies that increasing solidity ratio through blade count produced greater noise reduction than increasing solidity ratio through changes in blade aspect ratio [55].

The results of this section suggest that low-noise propulsor geometry would favor large blade radii and high solidity ratios, although such designs would not minimize required power. Propulsors with smaller radii would require higher solidity ratios to achieve comparable noise levels.

4.2.5 Comparison of Geometric and Operational Variations

Figure 4.6 expresses combinations of blade count and propulsor radius in terms of propulsor solidity ratio and tip speed. Similar to the results in Section 4.2.3, noise increased with higher tip speeds. At a given tip speed, increasing solidity ratio also increased noise, although with a much smaller impact than that of tip speed. This differs from the overall trend in Section 4.2.4, where higher-solidity designs generally exhibited lower noise because solidity ratio was not isolated from correlated changes in tip speed and propulsor geometry. These effects diminished with increasing tip speed. At an M_{Tip} of 0.55, a 0.24 increase in solidity ratio increased noise by 5 dBA, whereas at an M_{Tip} of 0.39, a 0.46 increase in solidity ratio increased noise by 2 dBA. Propulsors designed for lower tip speeds generally required higher solidity ratios to achieve the same thrust.

Noise was observed to increase sharply beyond a threshold tip speed. This threshold increased with blade count, occurring near an M_{Tip} of 0.60 for three blades compared to an M_{Tip} of 0.75 for nine blades. Blade count otherwise had less direct effect on noise compared to tip speed. However, higher blade counts enabled propulsor designs with lower tip speeds and higher solidity ratios. A 3-bladed propulsor maintained M_{Tip} values above 0.45, solidity ratios below 0.19, and noise above 70 dBA, whereas a 7-bladed propulsor reduced M_{Tip} to 0.31, increased solidity ratio to 0.80, and decreased noise to 65 dBA. For a given solidity ratio, noise levels varied little with changes in blade count. These results agreed with similar studies exploring variations in blade count, diameter, and tip speed [55, 12]. Zolbayar’s modeled results showed a similar trend, with an increase in blade count from 3 to 6 shifting noise levels by as much as 10 dB OASPL across all evaluated tip speeds [111].

These findings indicate that low-noise propulsor designs favor high blade counts and low tip speeds. Such designs require higher solidity ratios compared to those of propulsors operating at a higher tip speed for equivalent thrust. While solidity ratio alone does not directly indicate a quieter design, higher solidity ratios are generally associated with characteristics such as lower tip speeds and larger blade radii, which have been shown to significantly reduce noise.

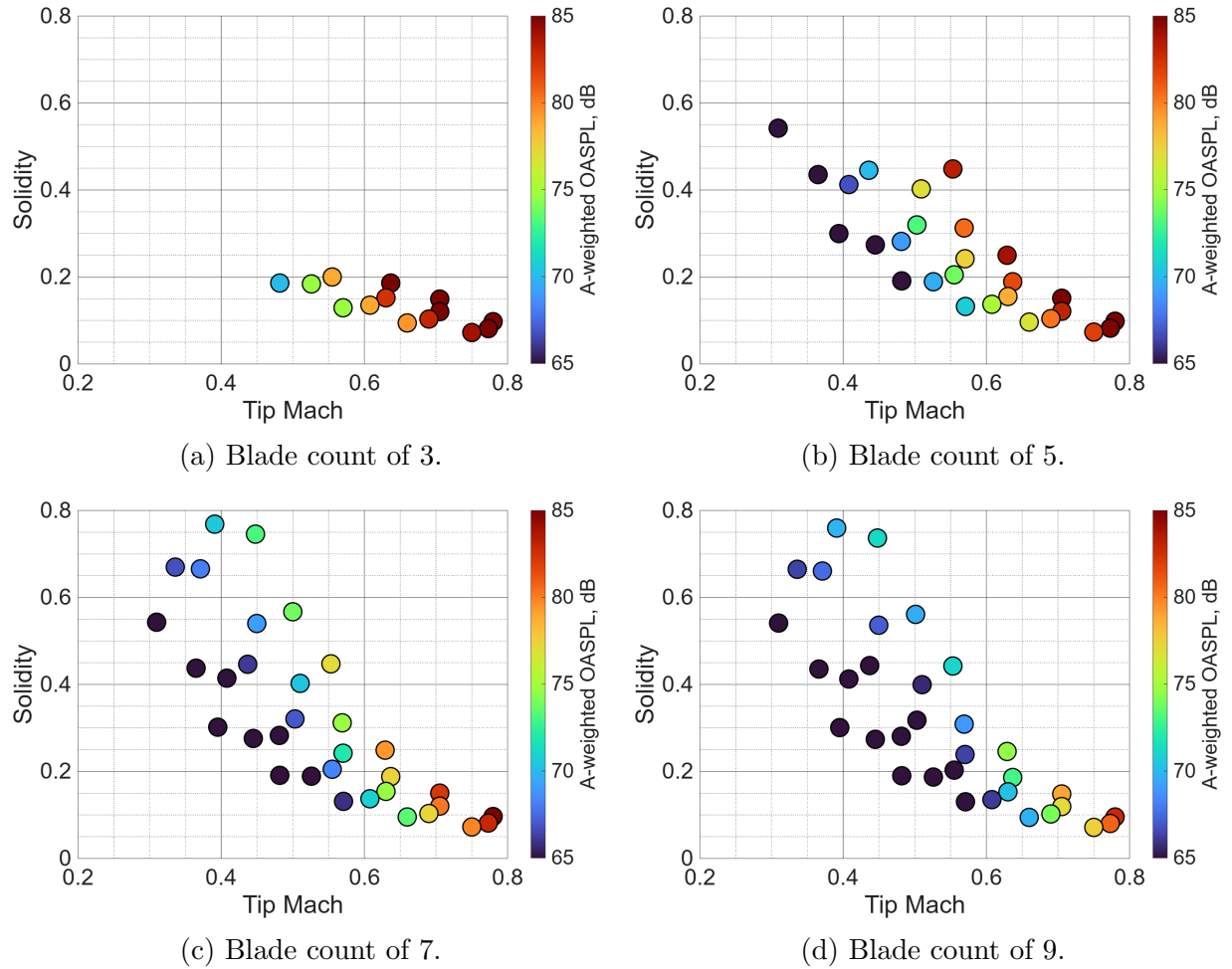


Figure 4.6: Undertrack A-weighted OASPL as a function of tip Mach number and solidity ratio for blade counts of 3–9 at constant thrust.

4.3 Noise of DEP Propulsor Architecture Design

Equations (3.4)–(3.6) indicate that increasing propulsor radius augments lift by increasing ΔC_J ; however, increasing propulsor radius also decreases δ_J for a given flap angle. This suggests that varying propulsor count changes the total vehicle thrust required during high-lift flight segments and that, for a given wing geometry and flight condition, there is a DEP propulsor count that maximizes lift augmentation [78, 29]. The former effect poses a particularly important consideration for noise modeling because total vehicle thrust cannot be assumed constant across different distributed propulsor arrangements and instead must be sized to account for the lift augmentation effects described in Section 3.2.1.1. This is distinct from the trivial case where vehicle thrust is constant for all arrangements and thrust per propulsor is simply total thrust divided by propulsor count.

The following section explores the noise impacts of varying propulsor count on a representative blown-flap STOL vehicle based on [29, 107], with its characteristics summarized in Table 4.2. Each propulsor arrangement was sized to satisfy a representative STOL takeoff flight profile consisting of a 45-meter ground roll and an initial climb to 30.5 meters based on [49] and [107].

Table 4.2: Baseline blown-flap DEP STOL vehicle characteristics.

Vehicle Characteristics	
Vehicle Weight (kg)	2721
Passenger Count	9
Wing Span (m)	13.3
Wing Area (m ²)	25.3
Wing Aspect Ratio	7
Propulsor Count	8
Airspeed at rotation (m/s)	16
Cruise Speed (m/s)	78

4.3.1 DEP Propulsor Design

Takeoff thrust was determined using an existing departure profile as described in Section 3.2.1.2. For each propulsor arrangement, lift and drag forces were calculated based on the procedure described in Section 3.2 using propulsor geometry and an averaged thrust. An acceleration profile was then used with the kinematic equations, Eqs. (3.7) and (3.8), to relate the vehicle's change in momentum to the resulting lift and drag forces. Thrust was iteratively increased or decreased until the forces were balanced at all time steps during takeoff. The resulting output was thrust as a function of time. Other parameters, such as velocity and distance, can also be derived as functions of time from the acceleration profile. Table 4.3 summarizes the overall propulsor design constraints and the range of parameters evaluated. Radius was a function of propulsor count such that all propulsors were contained within the exposed wingspan. A tip-to-tip spacing of 5% of the propulsor diameter, corresponding to $S/D = 1.05$, was maintained to minimize the potential for propulsor-propulsor interactions. Although propulsor-wing interactions may potentially increase total vehicle noise relative to isolated propulsor cases, these effects can be significantly reduced by maintaining a horizontal separation beyond 0.25 propulsor radii and a vertical separation greater than 1.25 propulsor radii, as discussed in Section 4.1.3. Takeoff specifications and propulsor design characteristics are listed in Table 4.3.

Table 4.3: DEP STOL takeoff requirements and propulsor design range.

Takeoff Specifications	
Ground-roll Distance (m)	45
Initial Climb Angle	10°
Ground-roll Average Acceleration (m/s ²)	10.7
Takeoff Flap Angle δ_F	40°
Propulsor Design Range	
Number of Propulsors	4–16
Blade Count per Propulsor	5
Propulsor Spacing (S/D)	1.05
Total Cruise Vehicle Thrust (N)	3772

An intermediate design speed of 25 m/s was selected to allow all propulsor designs to achieve at least 90% efficiency at cruise speeds while providing the required thrust in static conditions without excessive sectional blade stall. Cruise conditions were constant for all designs because flaps are retracted and provide minimal, if any, blown-lift effects. A linearly tapered C_L distribution was used to further help avoid stalling near the blade root when pitched for departure. All propulsor arrangements were designed to operate at a constant RPM. XROTOR was used to determine the appropriate blade pitch to achieve a desired amount of thrust. Table 4.4 summarizes the performance characteristics of each arrangement as a function of propulsor count.

Table 4.4: Blown-flap DEP arrangement geometry and thrust requirements.

Propulsor Count	Tip Radius (m)	Maximum Thrust (N)	Maximum Thrust Per Propulsor (N)
4	1.43	16680	4170
6	0.95	15180	2530
8	0.71	14560	1820
10	0.57	14200	1420
12	0.48	14040	1170
14	0.41	13832	988
16	0.36	13712	857

Figures 4.7a and 4.7b show that both individual and total vehicle thrust decrease with propulsor count, with a diminished rate of change at higher propulsor counts. The effects of propulsor count on the effectiveness of blown lift are evident when comparing the values of total takeoff thrust. Between four and six propulsors, $\frac{h_d}{c}$ decreases by 34% from 1.02 to 0.68, which results in a 1640 N decrease in total thrust required for takeoff. These effects lessen at higher propulsor counts, with the change from 14 to 16 propulsors decreasing $\frac{h_d}{c}$ by 11.9% and resulting in a 120 N decrease in total thrust. These findings are consistent with the diminished lift augmentation associated with increasing $\frac{h_d}{c}$ described in Section 3.2.1.1. Additionally, Fig. 4.7a depicts the thrust per propulsor for the trivial case in which total thrust remains constant and individual propulsor thrust is calculated as the total

thrust divided by propulsor count. Across propulsor counts from 4 to 16, the augmented lift performance analysis results in a 503–1280 N reduction in thrust per propulsor compared to designs using conventional lift assumptions.

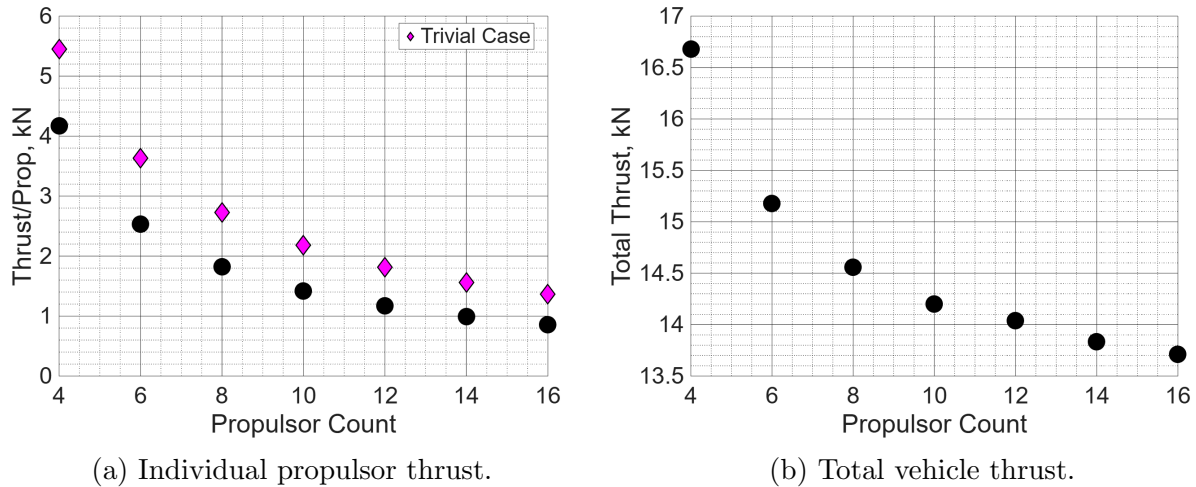


Figure 4.7: Individual propulsor and total vehicle thrust as functions of propulsor count.

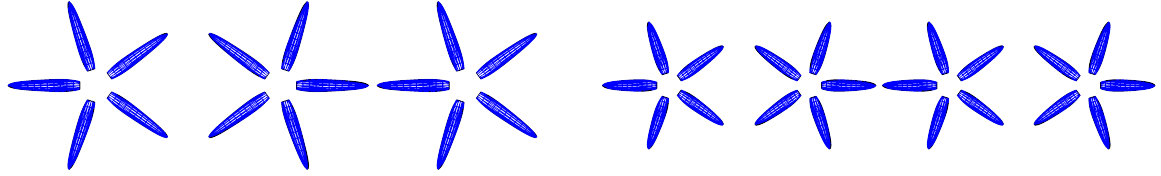
Thrust and propulsor radii are solely functions of propulsor count and are independent of propulsor geometry. The remaining propulsor design variable is RPM. Two design strategies were considered to determine the operating RPM: constant tip speed and constant thrust coefficient.

4.3.1.1 RPM Design – Constant Tip Speed

Section 4.2.3 demonstrated that noise is highly sensitive to tip speed; therefore, a constant M_{Tip} isolates the effects of propulsor geometry on noise and performance. Table 4.5 summarizes the specifications of the constant tip speed propulsor designs. Rotational speed was sized to maintain an M_{Tip} of 0.45, corresponding to the nominal operating speed of the 8-propulsor reference STOL vehicle. Figure 4.8 depicts example constant- M_{Tip} propulsors, illustrating the increase in blade solidity ratio with increasing propulsor count.

Table 4.5: Constant-tip-speed DEP propulsor design characteristics.

Propulsor Count	Maximum Thrust Per Propulsor (N)	Maximum RPM	Maximum Power (kW)	Thrust Coefficient C_T	M_{Tip}	Solidity Ratio
4	4170	1010	608.0	0.181	0.45	0.14
6	2530	1516	606.0	0.247	0.45	0.19
8	1820	2019	630.4	0.315	0.45	0.25
10	1420	2524	665.0	0.384	0.45	0.32
12	1170	3028	706.8	0.455	0.45	0.39
14	988	3533	747.6	0.524	0.45	0.46
16	857	4038	793.6	0.594	0.45	0.54



(a) Propulsor Count of 6 with C_T of 0.247.

(b) Propulsor Count of 8 with C_T of 0.315.



(c) Propulsor Count of 12 with C_T of 0.455.

(d) Propulsor Count of 16 with C_T of 0.594.

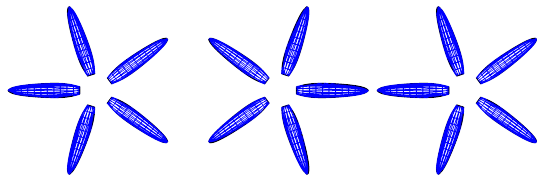
Figure 4.8: Example DEP propulsor designs at a constant M_{Tip} of 0.45; only half of each symmetric arrangement is shown.

4.3.1.2 RPM Design – Constant Coefficient of Thrust

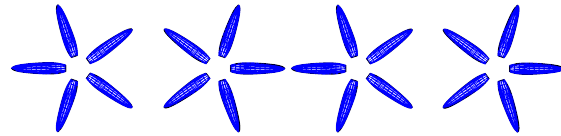
A less restrictive approach, however, is to allow tip speed to increase with propulsor count as individual radii decrease. Higher tip speeds incur a noise penalty but may reduce required power because propulsor solidity ratio and torque increase at a lower rate than in the constant-tip-speed designs. A constant-thrust-coefficient design also provides a better comparison of the effects of operational parameters because solidity ratio remains constant. Table 4.6 specifies DEP arrangements for a constant C_T of 0.315, corresponding to the thrust coefficient of the 8-propulsor baseline configuration. Figure 4.9 depicts example propulsors designed with a constant coefficient of thrust.

Table 4.6: Constant-thrust-coefficient DEP propulsor design characteristics.

Propulsor Count	Maximum Thrust Per Propulsor (N)	Maximum RPM	Maximum Power (kW)	Thrust Coefficient C_T	M_{Tip}	Solidity Ratio
4	4170	315	632.0	0.315	0.34	0.25
6	2530	765	618.0	0.315	0.40	0.25
8	1820	1343	630.4	0.315	0.45	0.25
10	1420	2018	650.0	0.315	0.49	0.25
12	1170	2788	673.2	0.315	0.53	0.25
14	988	3639	694.4	0.315	0.57	0.25
16	857	4558	715.2	0.315	0.61	0.25



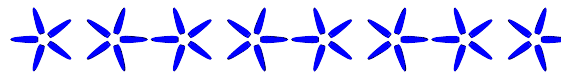
(a) Propulsor Count of 6 with M_{Tip} of 0.40.



(b) Propulsor Count of 8 with M_{Tip} of 0.45.



(c) Propulsor Count of 12 with M_{Tip} of 0.53.



(d) Propulsor Count of 16 with M_{Tip} of 0.61.

Figure 4.9: Example DEP propulsor designs at a constant C_T of 0.315; only half of each symmetric arrangement is shown.

4.3.2 DEP Noise Modeling

The generated sectional geometry was then processed and assembled into a surface-mesh model of the propulsors, depicted in Figs. 4.8 and 4.9. These models, along with required thrust, inflow, and operating RPM, served as inputs to the Source Noise Modeling Module, described in Section 3.3, to generate source noise hemispheres around the vehicle. For the DEP study, the observer radius was fixed at 35 meters, ensuring a minimum distance of $5D$ from the outermost propulsor so that source noise directivity was independent of observer size. The distributed propulsor arrangements were modeled at 25 m/s to represent takeoff conditions. The result was a set of source noise hemispheres that captured the magnitude and directivity of the total vehicle noise.

Within a source noise hemisphere, broadband noise is generally dominant fore and aft of the vehicle, whereas tonal noise is strongest along the propulsor disk plane. Observed noise varies substantially with observer location, resulting in different characterizations. The most representative analyses would be based on community noise, obtained by propagating source noise along a flight path to ground observers, and would account for source noise directivity. This approach, however, is beyond the scope of the current study. Instead, the following comparisons use modeled undertrack noise at an observer located 35 meters directly below the vehicle. This location provides a direct representation of noise propagating toward ground observers. Undertrack noise represents a "snapshot" in time during the departure procedure and provides the most meaningful point of comparison for the present study.

4.4 DEP Noise Results

Figure 4.10 depicts undertrack source noise for both design approaches. Constant tip speed designs are represented by squares in Fig. 4.10a, whereas constant C_T designs are represented by triangles in Fig. 4.10b. For both approaches, increasing propulsor count increases undertrack noise, although the magnitude of the increase differs between the two cases. The constant C_T designs exhibit a 31.3 dBA noise increase from 4 to 16 propulsors, compared to 13.5 dBA for the constant M_{Tip} designs. Additionally, the rate of noise increase is greater at lower propulsor counts for both design approaches. For the constant C_T designs, undertrack noise increased by 11.1% from 6 to 8 propulsors, but only by 4.3% from 14 to 16 propulsors. Similarly, undertrack noise for the constant tip speed cases increased by 4.8% and 1.7% for the same propulsor count increases. Consistent with the single-propulsor study in Section 4.2.3, these results indicate that noise is most strongly influenced by tip speed. For the same tip speed, increasing propulsor count and, therefore, solidity ratio also increases noise, but to a much lesser extent.

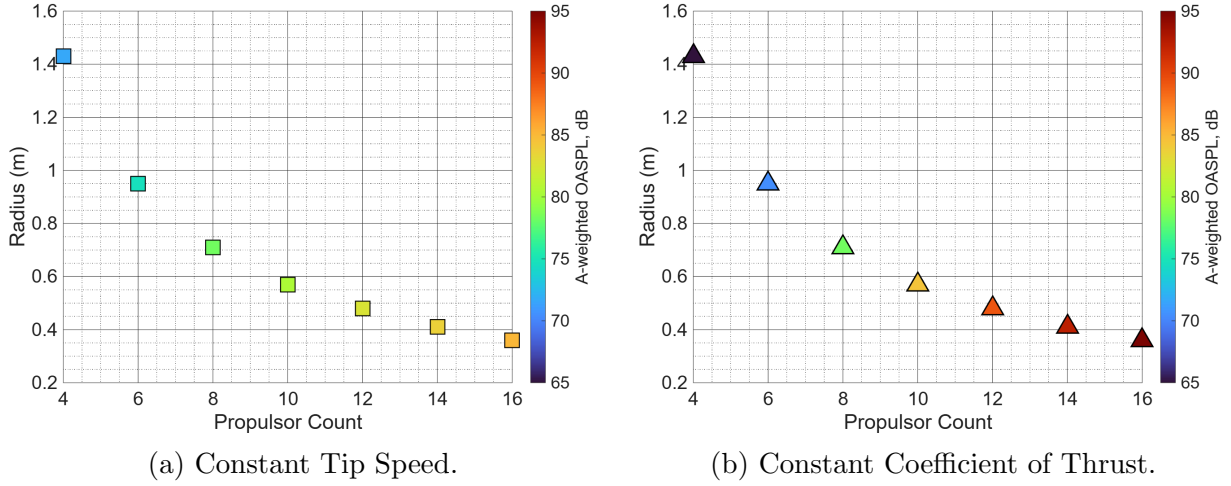


Figure 4.10: Undertrack A-weighted OASPL as a function of propulsor count for constant-tip-speed and constant-thrust-coefficient DEP arrangements.

In comparison, the SEL of a Cessna 208B, a similarly sized conventional passenger aircraft, is 94.6 dB at a distance of 35 meters below the vehicle during departure [67]. Assuming a 10 dB-down duration, constant C_T arrangements with 10 or fewer propulsors produce 71.5–90.8 dB SEL, and all constant M_{Tip} designs remain below 91.5 dB SEL. These design approaches produce DEP designs with the potential to generate significantly less propulsor noise than their conventional counterparts.

Figures 4.11a and 4.11b show the variation in individual propulsor power and total vehicle power for the two design approaches. Individual propulsor power is defined in Section 4.2.2. Total vehicle power is calculated as the power per propulsor multiplied by propulsor count, representing the total power required by the vehicle at a given flight condition.

For both constant M_{Tip} and constant C_T designs, individual propulsor power decreases as propulsor count increases. At higher propulsor counts, the constant C_T cases have greater power reductions than the higher-solidity constant-tip-speed designs. This reduction, however, comes with a noise penalty. For a 16-propulsor arrangement, the constant C_T design reduces power by 4.9 kW per propulsor (78.4 kW total) but increases noise by 11.3 dBA.

This trend reverses for configurations with fewer than 8 propulsors. For a 6-propulsor arrangement, the constant-tip-speed case with a solidity ratio of 0.19 requires 12 kW less total power than its constant C_T counterpart with a solidity ratio of 0.25.

Total vehicle power increases with higher propulsor counts, indicating that propulsive efficiency decreases as propulsor count increases for both design approaches. However, an inflection point exists between arrangements with 8 and 4 propulsors, where total power increases with *decreasing* propulsor count. Although the solidity ratio continues to decrease over this range, there is a sharp increase in torque for arrangements with fewer than 8 propulsors, as shown in Fig. 4.11c. This effect is most pronounced for the constant C_T designs, which experience a 170% increase in torque between 6 and 4 propulsors. These findings suggest that while decreasing the solidity ratio reduces required propulsor power, excessively low RPM values require a disproportionate increase in torque, ultimately increasing the total required power.

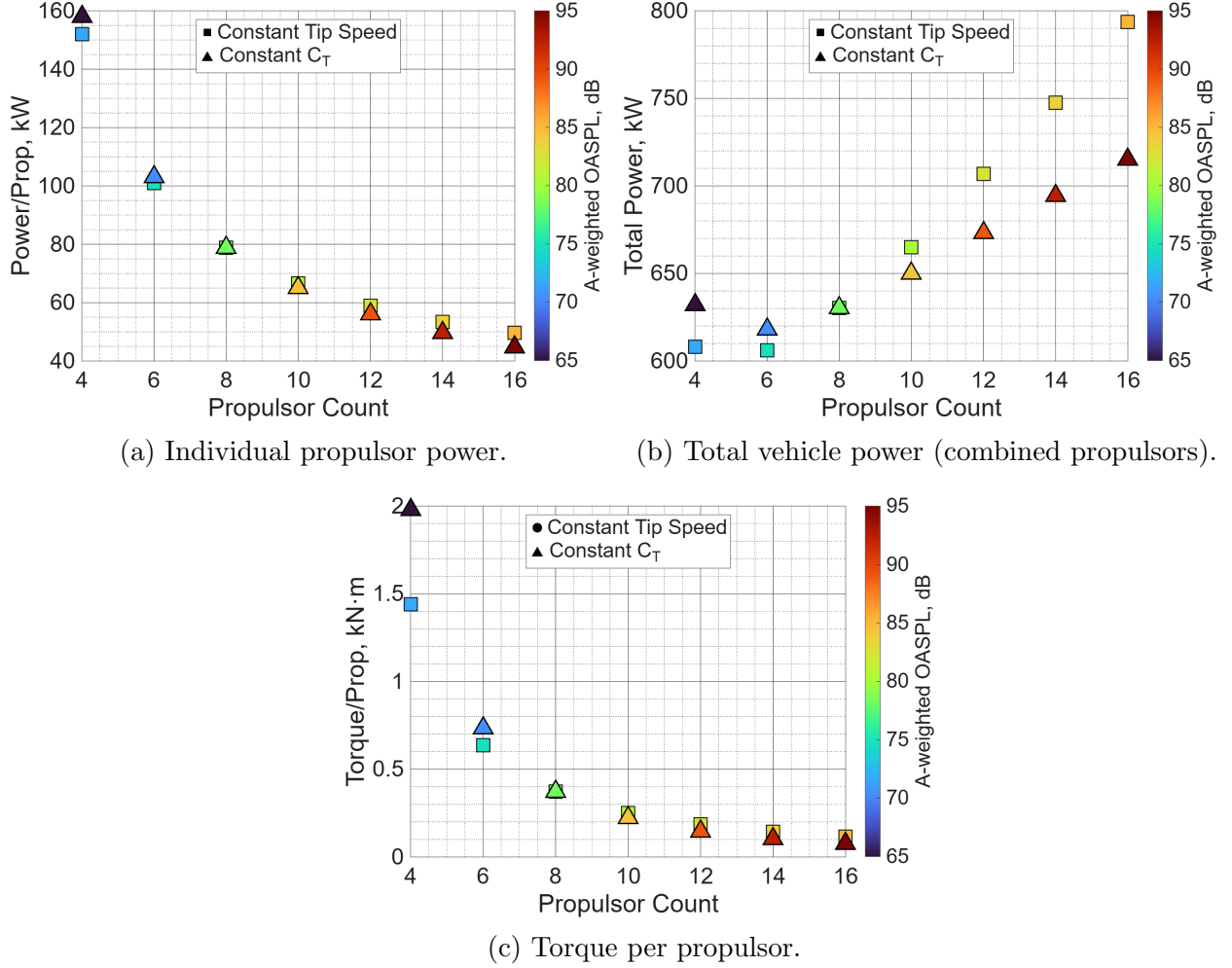


Figure 4.11: Individual propulsor power, total vehicle power, and propulsor torque as functions of propulsor count.

These findings are consistent with existing work that associated increases in solidity ratio with increases in required power due to higher propulsor disk loading [12, 55]. Similar to the single propulsor results in Section 4.2.4, propulsors with higher solidity ratios were found to require greater power. The DEP assessment suggests that these trends are also applicable to various arrangements of a multi-propulsor system.

The results of this section indicate that DEP arrangements have the potential for power reduction while also mitigating propulsor noise. The lift augmentation effects of a blown-flap DEP STOL vehicle significantly reduce required vehicle thrust. This benefit alone,

however, was not sufficient to offset the increases in power and noise as propulsor disk loading increased at high propulsor counts. Two RPM strategies were explored, generating designs with varying tip speeds and solidity ratios. Reducing the solidity ratio was an effective method to reduce power for a given arrangement, but the higher required tip speeds resulted in a noise penalty. Very low solidity ratios led to an increase in power due to the sharp increase in torque. While DEP designs have well-known benefits when used with lift-augmentation systems, their impacts on noise and efficiency are highly sensitive to geometric and operational design variables.

4.5 Conclusions

The results of Section 4.2 indicate that propulsor blade count, radius, and rotational speed have a significant influence on propulsor noise. Low-noise propulsor designs require low tip speeds, large blade radii, and high blade counts. Such designs result in higher solidity ratios, increased propulsor torque, and higher required power for the same thrust. Section 4.3 demonstrated that although these single-propulsor design trends remain valid for most multi-propulsor arrangements, design extremes, such as excessively low rotational speeds, can limit their effective implementation in a propulsor system. The two propulsor design approaches illustrate the tradeoffs between power and noise and demonstrate how DEP architectures provide greater control over these parameters. Furthermore, the results of this section show that the lift-augmentation effects of a DEP STOL vehicle depend on propulsor count and that variations in propulsor arrangement affect fundamental vehicle performance parameters, such as thrust. While DEP arrangements have the potential to reduce power and noise, their effective implementation requires a multidisciplinary analysis capable of considering these coupled factors.

The findings of this work demonstrate that vehicle noise is heavily influenced by individual propulsor geometry and operation as well as by the overall arrangement of a multi-propulsor

DEP system. Accurate noise evaluations of such vehicles must account for these factors not only in propulsor noise modeling but also in the performance evaluation and overall propulsor architecture design. These results demonstrate the utility of the proposed methodology for preliminary propulsor design. Propulsor noise may be predicted with sufficient detail to conduct trade studies and sensitivity analyses early in the design process, when more design variables can be modified. Such capabilities are increasingly important as noise becomes a significant metric in vehicle design and emerging technologies provide greater opportunities for noise mitigation.

Chapter 5

Community Noise Assessment Based on Vehicle Operation

This chapter expands the methodology’s design and assessment capabilities to other multi-propulsor vehicle architectures, including DEP blown-flap STOL and lift-plus-cruise VTOL (LPC), depicted in Figure 1.1. This study explores the effects of parametric changes in flight profiles on community noise for vehicles with fixed architectures. In this analysis, the Propulsor Sizing and Evaluation Module, described in Section 3.1, is used to create propulsor geometries based on published performance information and vehicle geometry for the two AAM vehicles. Flight paths are generated using the Flight Profile Design Module, described in Section 3.4, based on representative propulsor performance and known vehicle parameters such as weight and aerodynamic characteristics. These departure profiles are designed to capture a broad range of feasible operating states unique to each vehicle. Each profile is sampled to create discrete operating conditions defined by parameters such as power, thrust, velocity, and RPM. These points are aggregated into an array of operating conditions that serves as input to the Source Noise Modeling Module, presented in Section 3.3. The result is a database of source noise hemispheres representative of the feasible operating conditions encountered along likely flight trajectories for each vehicle, allowing profiles beyond the original set to be created and evaluated. Finally, these source noise hemispheres are propagated to ground-based observers along the flight profiles using the Noise Propagation Module to obtain community noise. The resulting community noise footprints are used to explore variations in departure flight profiles and reveal how both propulsor and aircraft operation affect community noise.

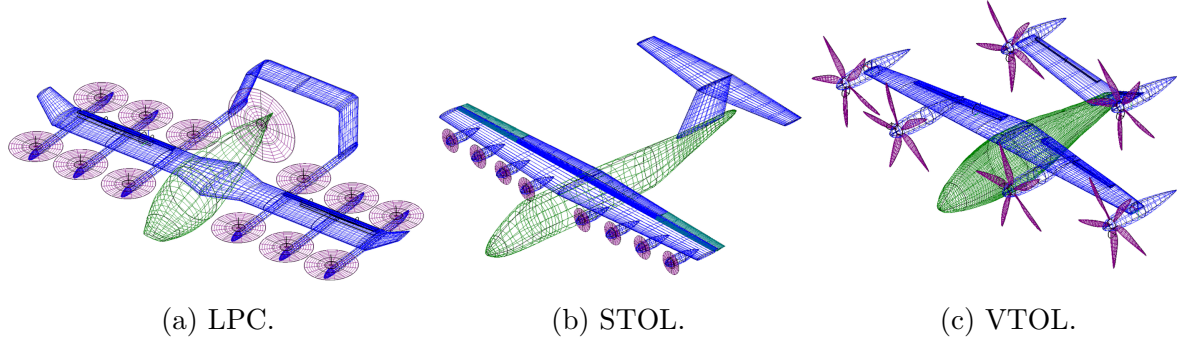


Figure 5.1: Representative AAM multi-propulsor vehicle architectures (reproduced from Fig. 1.1).

Propulsor designs were based on published operating characteristics and available geometry for each representative vehicle. Two vehicle platforms were selected based on mature and well-developed AAM designs. These vehicles have distinct flight capabilities and demonstrate the methodology’s ability to model and predict noise for a variety of existing and potential future aircraft architectures. Detailed propulsor design for each vehicle platform was based on published design and operating information [11]. Key parameters include takeoff and cruise velocities, vehicle weight, propulsor diameter and blade count, and typical operating ranges for parameters such as power and RPM. These values can be obtained from a single design or assembled from representative data across multiple vehicles of the same architecture type.

The purpose of this study is not to quantitatively compare the noise results between the two vehicles. Instead, the goal is to demonstrate how the methodology outlined in Chapter 3 can be adapted to vehicles with different characteristics using top-level aircraft requirements. The following sections describe how this procedure is applied to each vehicle.

5.1 Community Noise of Lift-Plus-Cruise VTOL

The lift-plus-cruise VTOL vehicle is examined first to demonstrate how the methodology is applied to an aircraft architecture that uses separate propulsor systems for vertical lift and

forward propulsion. This configuration requires distinct sizing and operating considerations for the lift and cruise propulsors and produces a flight profile that includes hover, transition, climb, and cruise segments. The following sections describe the representative LPC vehicle, develop its propulsor designs, and evaluate how variations in departure operation affect source and community noise.

5.1.1 Propulsor Sizing

Table 5.1 summarizes design parameters for a representative lift-plus-cruise VTOL vehicle, depicted in Figure 5.2, based on the vehicle described in [11]. This vehicle design features separate lift and cruise propulsors. Lift propulsors provide thrust during vertical flight, while the cruise propulsor and fixed wing provide thrust and lift, respectively, during horizontal flight. Critical characteristics, such as vehicle weight, flight speeds, and power, were obtained from published data [37]. Vehicle and propulsor geometry were also estimated from publicly released drawings and images of similar LPC vehicles [63]. The maximum operating RPM was selected to remain within power limits consistent with published values and available motor data for similar vehicles [11, 36].

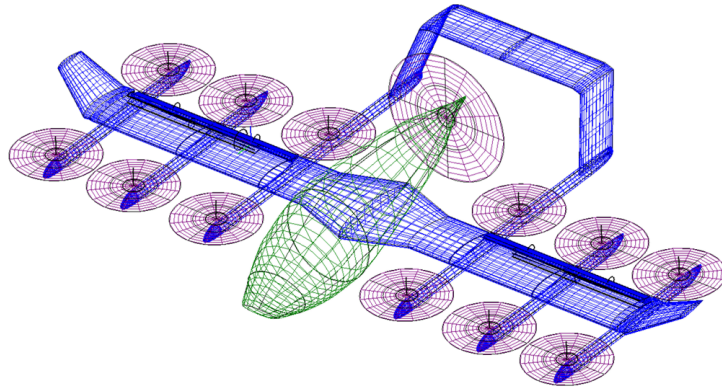


Figure 5.2: Representative lift-plus-cruise vehicle model used for propulsor and noise analysis.

The propulsor design procedure described in Section 3.1 was used to design the lift and cruise propulsors. An initial design velocity of 50 m/s was selected as an intermediate condition

between hover and cruise to ensure high-speed capability while maintaining efficient hover performance. This approach is consistent with suggested procedures for initial propulsor design [105]. The resulting lift and cruise propulsors provide similar thrust across low- and high-speed flight conditions, with power requirements consistent with LPC vehicles of similar size and configuration. These vehicle and propulsor characteristics are summarized in Table 5.1. The modeled lift and cruise propulsors are depicted in Figure 5.3.

Table 5.1: Representative lift-plus-cruise vehicle and propulsor characteristics.

Vehicle Characteristics		
Vehicle Weight (kg)	1814	
Wingspan (m)	10.9	
Cruise Speed (m/s)	50	
Performance Characteristics	Lift	Cruise
Number of Installed Propulsors	12	1
Maximum Thrust per Propulsor (N)	1060	5591
Maximum RPM per Propulsor	2900	1700
Maximum Power per Propulsor (kW)	27	98
Geometric Characteristics	Lift	Cruise
Propulsor Diameter (m)	1.3	2.0
Blade Count per Propulsor	2	3

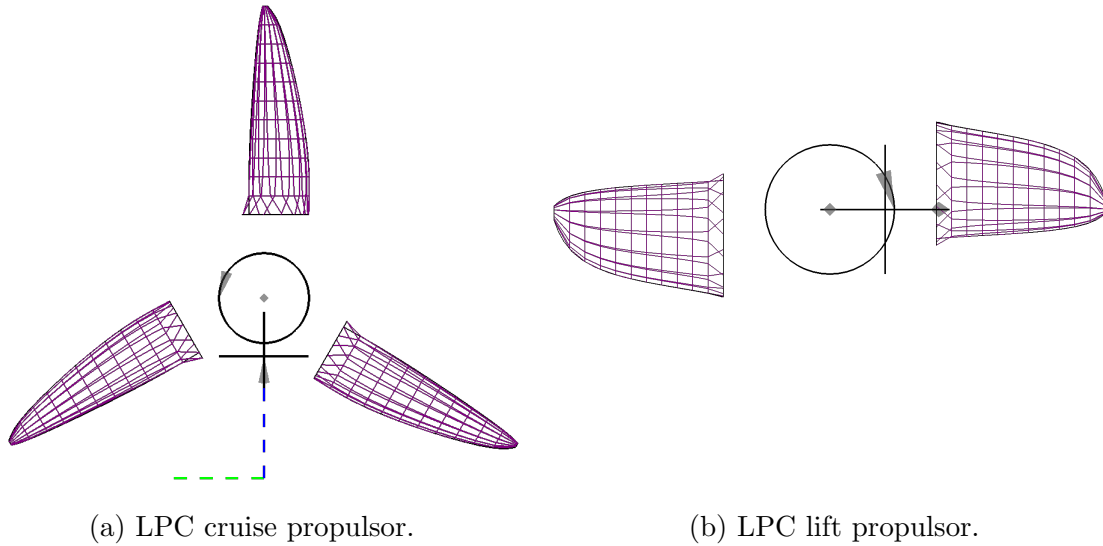


Figure 5.3: Designed lift and cruise propulsors for the LPC vehicle.

5.1.2 Flight Profile Design

Before noise modeling can be performed for each vehicle, operating conditions such as thrust, RPM, and flight speed must be defined. Although noise hemispheres could be created for all possible operating conditions, this would require excessive computational effort and would be unnecessary because the study considers only physically feasible conditions encountered during realistic flight profiles. For each vehicle, flight profiles were used to determine the operating range of each propulsor design. Flight-profile parameters were varied such that each vehicle's trajectories covered a sufficiently broad range of operating conditions to capture noise over representative flight scenarios. For the LPC vehicle, propulsor performance was dictated by rotational speed, thrust, power, and airspeed. Flight profiles were varied by changing propulsor power, flight path angle, and hover altitude, when applicable. Vehicle trajectories were divided into segments, each representing a major flight condition. Flight profiles were generated using the procedure described in Section 3.4.

5.1.2.1 Vehicle Physics and Flight Profile Segment Generation

The LPC vehicle trajectories focused on variations in the transition from primarily vertical climb using only the lift propulsors to more conventional departures utilizing both lift and cruise propulsors for a longer, more gradual climb. Figure 5.4 presents the forces acting on the LPC vehicle, as well as the sign conventions for the vehicle's angle of attack, α , and flight path angle, γ .

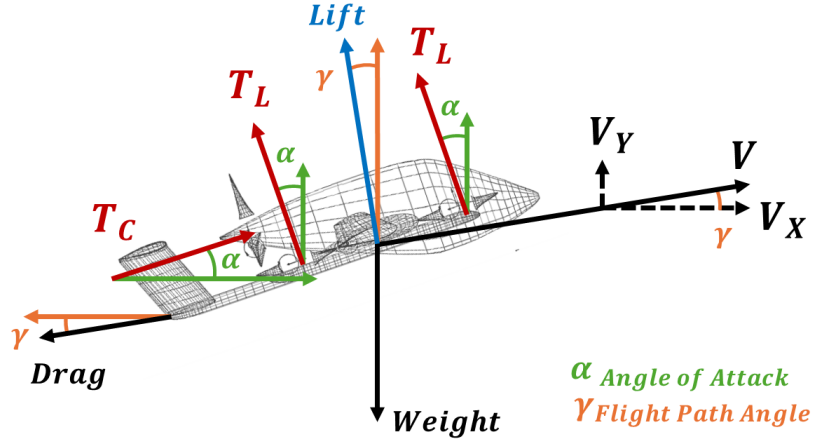


Figure 5.4: Forces, velocity, and angle conventions used for the LPC flight-profile model.

The flight-profile governing equations, Eqs. (3.9) and (3.10), from Section 3.4 were adapted for the LPC vehicle, where T_C and T_L represent thrust from the cruise and lift propulsors, respectively.

$$F_X = T_C \cos(\alpha) - T_L \sin(\alpha) - \sin(\gamma) \text{Lift} - \cos(\gamma) \text{Drag} \quad (5.1)$$

$$F_Y = \cos(\gamma) \text{Lift} + T_L \cos(\alpha) + T_C \sin(\alpha) - \text{Weight} - \sin(\gamma) \text{Drag} \quad (5.2)$$

All LPC trajectories consist of six segments and are based on the procedure described in [49]. Segment 1 consists of a vertical climb using only the 12 lifting propulsors. The wing is assumed to provide zero lift. Once the target hover altitude, h_1 , is reached, the lift propulsors reduce thrust in Segment 2 until the vertical velocity reaches zero and the vehicle becomes stationary. In Segment 3, the cruise propulsor accelerates the vehicle, and the wing begins to produce lift. As flight speed increases, the thrust from the lift propulsors is reduced at a specified rate to maintain the desired transition flight path angle, $\gamma_{Transition}$. This segment ends when the lift propulsors are no longer operating and all lift is produced by the wing. In Segment 4, the vehicle climbs at a constant speed and is trimmed using angle of attack to maintain the desired flight path angle, γ_{Climb} . This continues until the target cruise altitude,

h_2 , is reached. In Segment 5, the LPC vehicle reduces its flight path angle while accelerating until level flight at the cruise speed is established. Angle of attack is used to trim the vehicle such that lift balances vehicle weight. This configuration is maintained in Segment 6 until a ground-track distance of 12 km is reached. Figure 5.5 illustrates the six departure segments from hover through the start of cruise, including the transition and climb flight path angles, γ , the target hover altitude, h_1 , and the final climb altitude, h_2 . Segments 1 and 2 terminate at the same ground-track location as the start of Segment 3, marking the beginning of the transition. The resulting propulsor designs establish the operating limits used to generate the LPC flight profiles and evaluate their associated source and community noise.

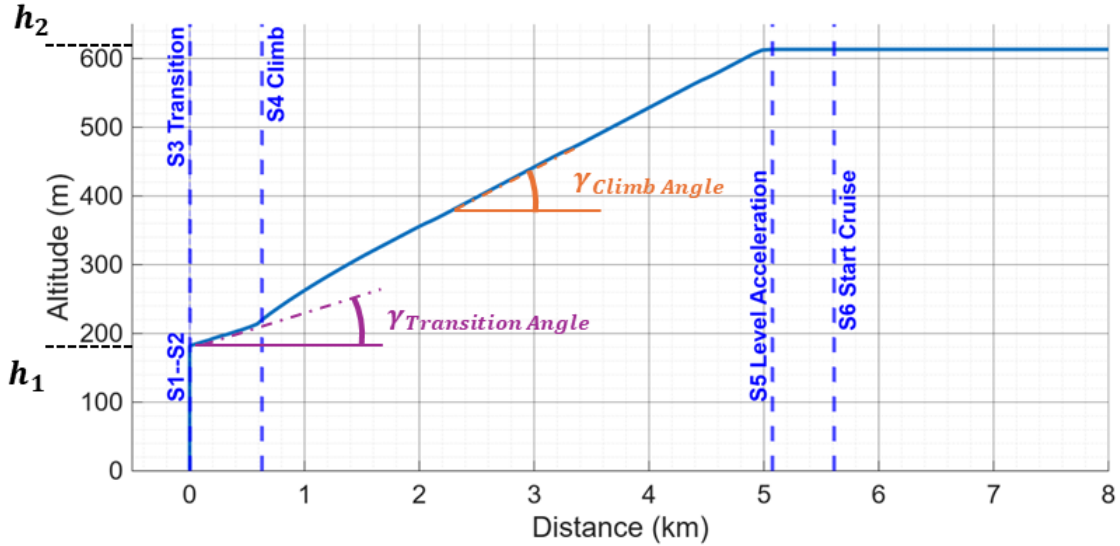


Figure 5.5: Example LPC departure profile showing altitude as a function of ground-track distance from hover through cruise.

5.1.2.2 Flight Profile Variations

The three main parameters used to vary the LPC vehicle flight profiles were hover altitude, transition flight path angle, and climb flight path angle. Hover altitude, h_1 , represents the target altitude reached during the initial hover segment before transition begins. Higher altitudes allow transition to occur farther from the ground but require the lift propulsors to operate longer, increasing noise and the duration of operation at a single location. The

transition angle represents the flight path angle during the transition segment. Transition angles range from 0 to 5 degrees, where an angle of 0 degrees corresponds to a level transition with no change in altitude. A transition angle of 5 degrees was determined to be the maximum within the propulsors' power limits because higher angles resulted in a sufficiently slow transition that the vehicle reached cruise altitude before accelerating to its target velocity. Lower transition angles result in faster forward acceleration and shorter transition segments, although the vehicle remains closer to the ground during these segments. Finally, climb angle represents the flight path angle during the climb segment, with higher climb angles reducing the time required to reach cruise altitude at the cost of increased power and propulsor noise. A climb angle of 7 degrees was determined to be the maximum before the cruise propulsor power limit was exceeded. Table 5.2 summarizes flight profile variations modeled for the LPC vehicle.

Table 5.2: LPC flight-profile parameter ranges.

LPC Flight Profile Parameters	
Transition Flight Path Angle (degrees)	0–5
Climb Flight Path Angle (degrees)	0–7
Hover Altitude, h_1 (m)	15–152
Cruise Altitude, h_2 (m)	609.6

Figure 5.6 depicts departure profiles with climb flight path angles ranging from 3 to 7 degrees, a constant transition flight path angle of 0 degrees, and a hover altitude of 15 m. At the same hover altitude, Figure 5.7 depicts transition flight path angles ranging from 0 to 5 degrees at a constant climb flight path angle of 5 degrees. Because the climb segments are longer in duration than the transition segments, changes in climb angle have a greater impact on the time required to reach cruise than similar changes in transition angle. In Figure 5.6, increasing the climb angle from 5 to 7 degrees shortens the climb duration by nearly 40 seconds. This reduction in climb duration, however, requires an additional 500 N of thrust from the cruise propulsor, which potentially increases community noise.

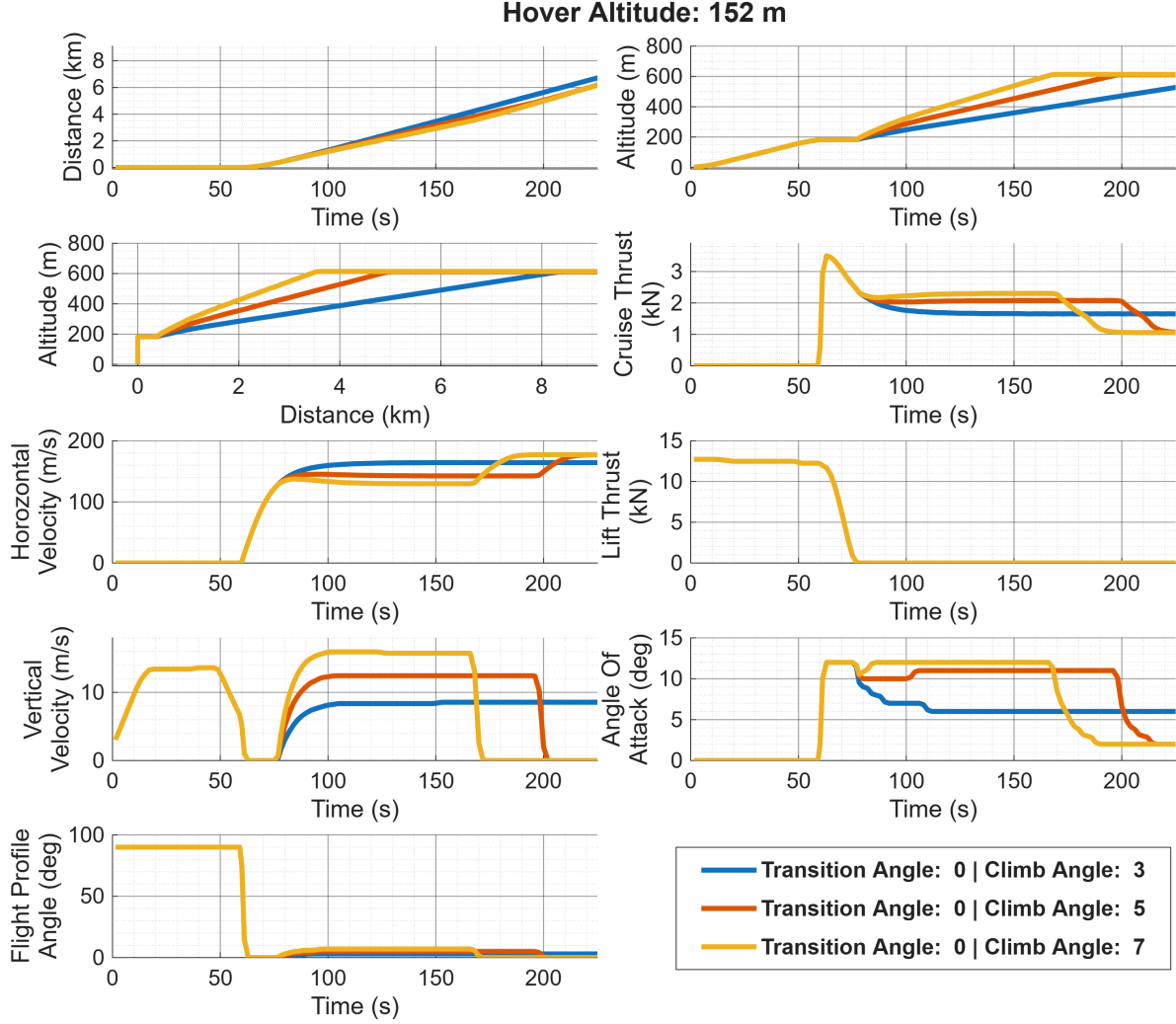


Figure 5.6: LPC departure profiles for climb flight path angles of 3–7 degrees at a 15-m hover altitude and 0-degree transition angle.

In comparison, a two-degree change in transition flight path angle, shown in Figure 5.7, does not substantially alter the overall departure profile because cruise altitude is reached in roughly the same time. Instead, increasing the transition angle affects the power required from the lift propulsors. This is especially important for noise because the transition segments occur at low altitudes and experience less altitude-based noise attenuation. This effect is further demonstrated in the community noise results, where these flight profiles are propagated to ground-based observer arrays.

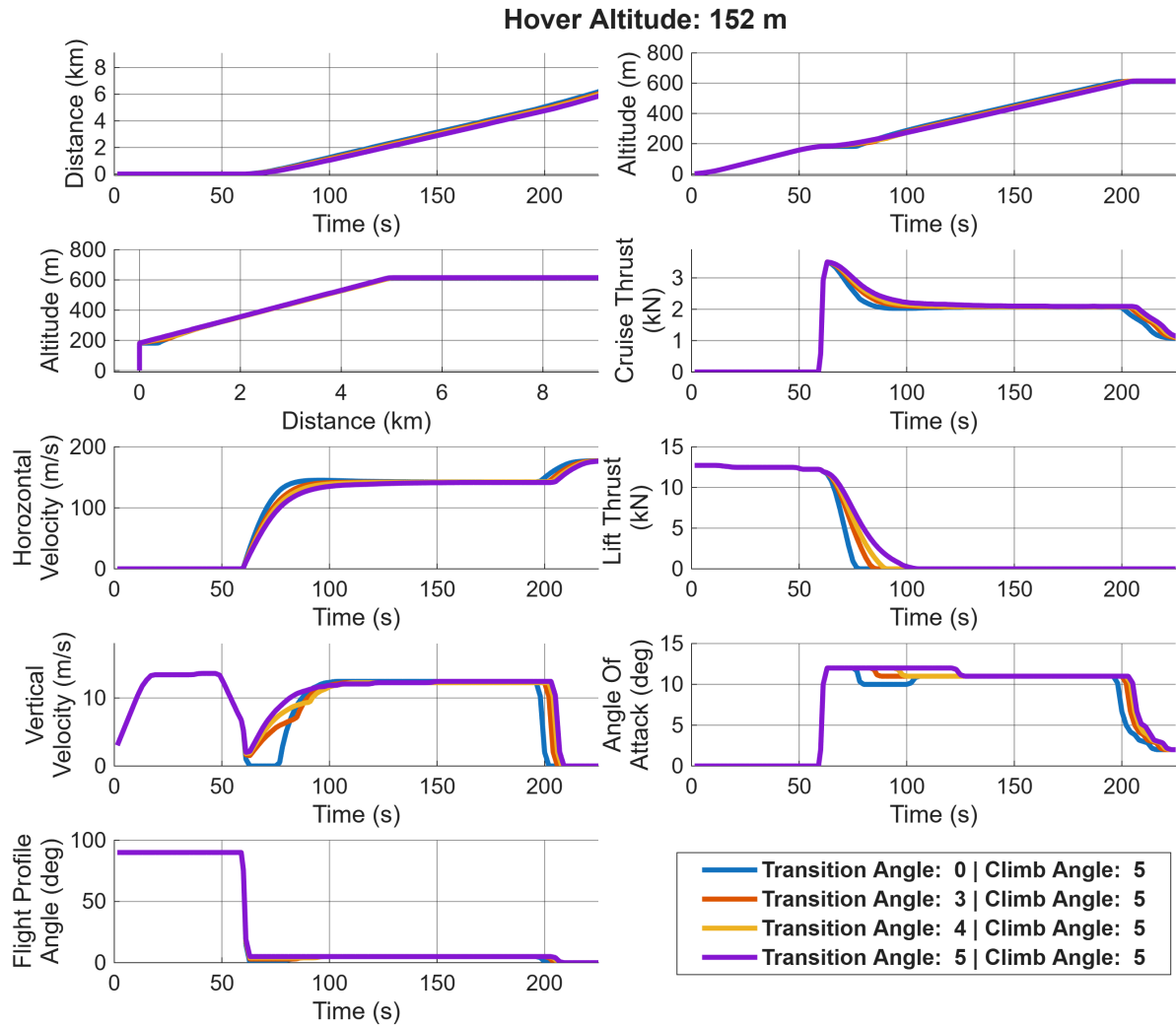


Figure 5.7: LPC departure profiles for transition flight path angles of 0–5 degrees at a 15-m hover altitude and 5-degree climb angle.

Figure 5.8 depicts the same departure profiles as in Figure 5.6, but at a target hover altitude of 152 m. This longer hover segment has minimal impact on the overall flight profile, with cruise conditions reached at a similar time to the 15 m departure profiles.

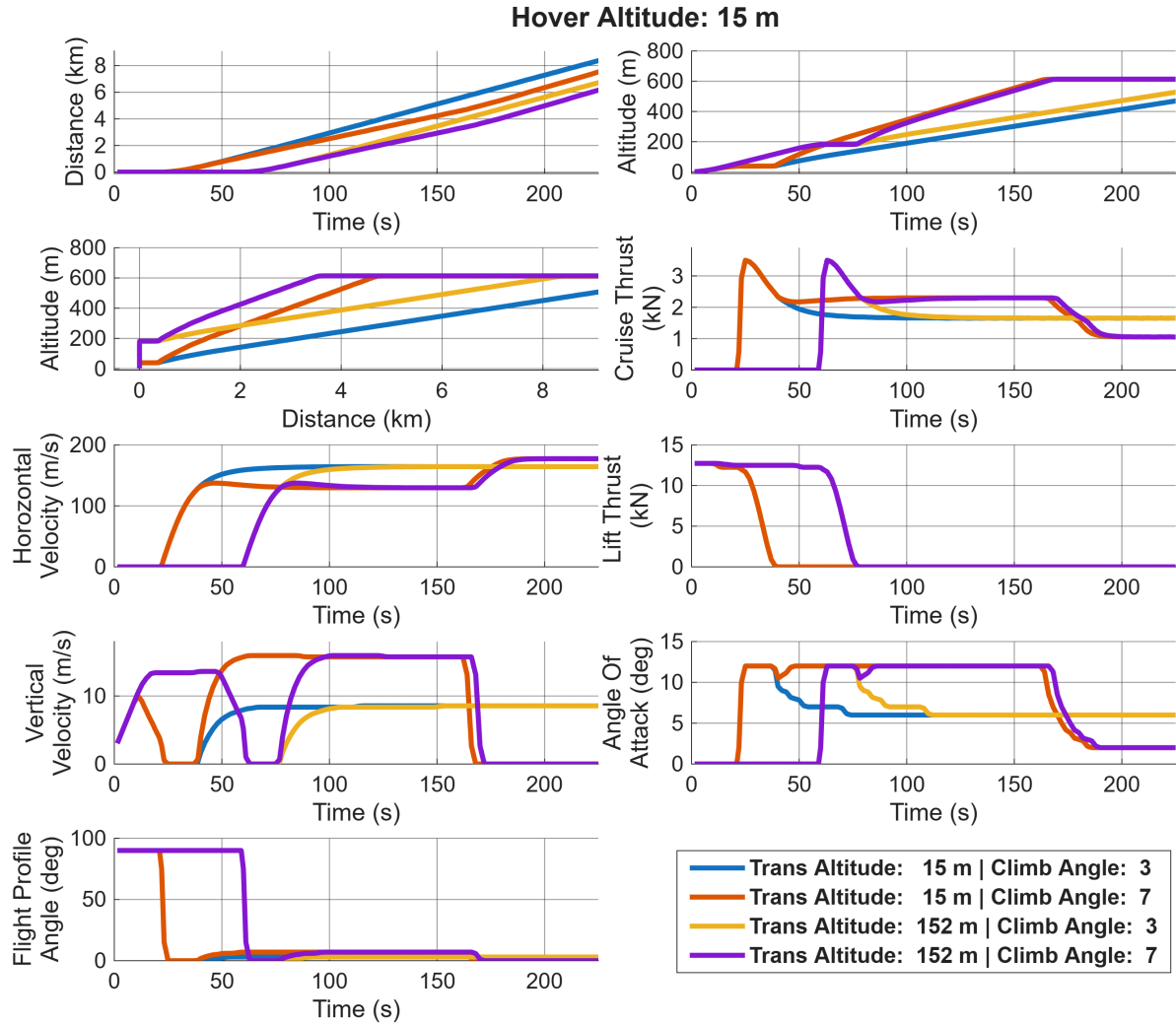


Figure 5.8: LPC departure profiles for hover altitudes of 15 and 152 m and climb flight path angles of 3–7 degrees at a 0-degree transition angle.

Variations in flight-profile angles show similar trends, with higher climb angles allowing the vehicle to reach cruise conditions faster and requiring an additional 500 N of cruise-propulsor thrust between climb angles of 5 and 7 degrees. The primary effect of a higher hover altitude is greater noise attenuation during the transition segment because the vehicle operates farther from the ground.

5.1.3 Propulsor Source Noise Characteristics

For the LPC vehicle, the main operating conditions include flight speed, vehicle attitude, propulsor thrust, and RPM. Vehicle attitude is used to obtain the axial and cross-flow components from the resultant speed along the flight path. Separate source noise hemispheres were generated for the 12 lift propulsors and the single cruise propulsor. The center of each hemisphere corresponds to the theoretical center of the vehicle, ensuring that each observer point has the same coordinates when the source noise is summed to produce total vehicle noise. Source noise was generated using the procedure described in Section 3.3.

Figure 5.9 plots the cruise propulsor noise in A-weighted OASPL for an observer 25 m directly below the vehicle. Lower-thrust conditions generally occur at higher flight speeds, during which the LPC vehicle is in cruise and requires less thrust due to reduced acceleration and induced drag. Likewise, higher thrust values generally occur at lower speeds corresponding to the transition segments, which require greater propulsor power due to the rapid acceleration of the vehicle. Consequently, cruise propulsor noise is noticeably lower during cruise segments. Although increasing inflow speed is generally associated with higher propulsor noise, the reduction in thrust from 3500 N to 1500 N is sufficiently large to dominate the cruise propulsor noise characteristics, resulting in lower source noise as flight velocity increases along the flight profile.

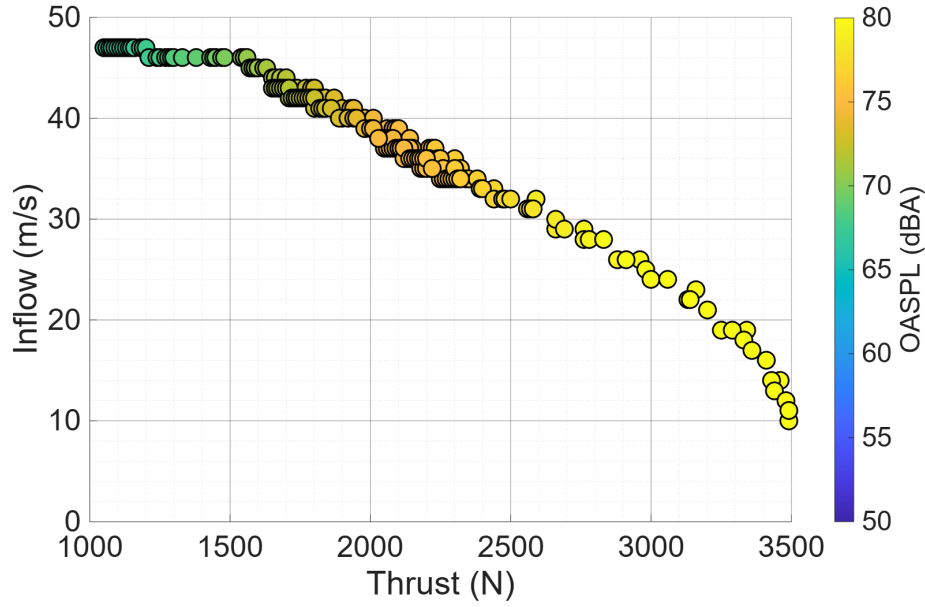


Figure 5.9: LPC cruise-propulsor A-weighted OASPL as a function of thrust and inflow velocity for 18 representative departure profiles.

In contrast to the cruise propulsor, Figure 5.10 presents the source noise generated by the set of 12 lift propulsors. The lift propulsors operate during flight segments with significant cross flow. As a result, this parameter is included in the source noise analysis. Most high-thrust cases above 800 N occur at cross-flow velocities below 10 m/s and inflow speeds below 4 m/s. These conditions correspond to vertical flight or early transition, during which the lift propulsors provide most of the force required to keep the vehicle airborne. As cross-flow velocity increases beyond 30 m/s, propulsor thrust quickly drops below 400 N as the wing experiences sufficient horizontal velocity to generate lift. At the same time, inflow velocity increases slightly, corresponding to an overall increase in flight speed as the vehicle accelerates through transition. Similar to the cruise propulsor, lift propulsor source noise is primarily influenced by thrust along the flight path, with higher noise levels typically corresponding to high-thrust, low-cross-flow flight.

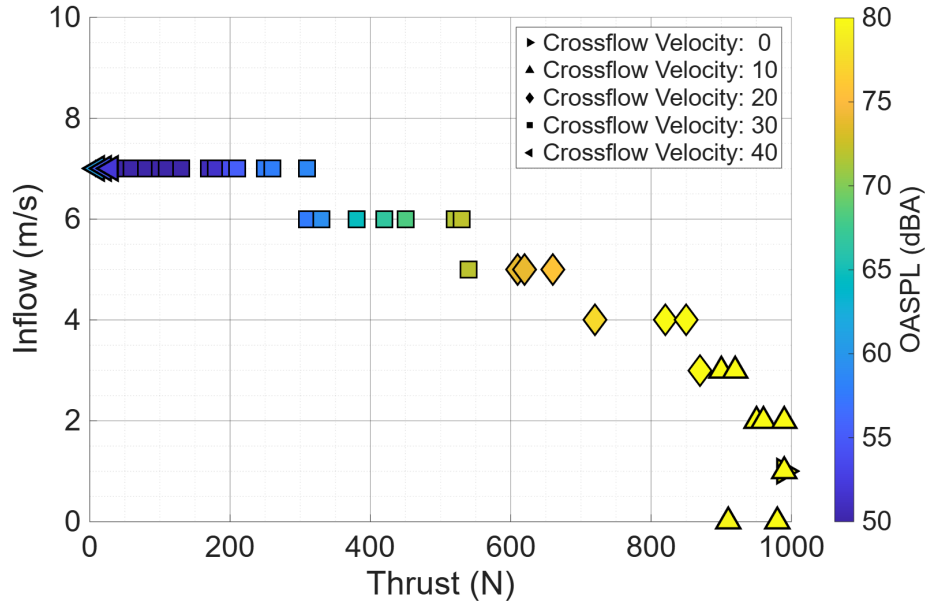


Figure 5.10: LPC lift-propulsor A-weighted OASPL as a function of thrust and inflow velocity for 18 representative departure profiles; marker shapes indicate cross-flow conditions.

The source noise characteristics of the cruise and lift propulsors provide the basis for evaluating how changes in vehicle operation affect noise propagated to the surrounding community.

5.1.4 Community Noise Contours

Source noise was propagated along select flight profiles using the procedure described in Section 3.5 to obtain community noise contours during departure. The following sections present community noise contours for flight profiles that vary in transition angle, climb angle, and hover altitude. These results demonstrate how source noise and aircraft operation, as well as the tradeoffs between them, affect community noise.

5.1.4.1 Variations in Climb Angle

Figure 5.11 presents thrust, altitude, and community noise contours for three climb angles. Transition angle and hover altitude remained constant at 0.0 degrees and 152 m, respectively, for all profiles.

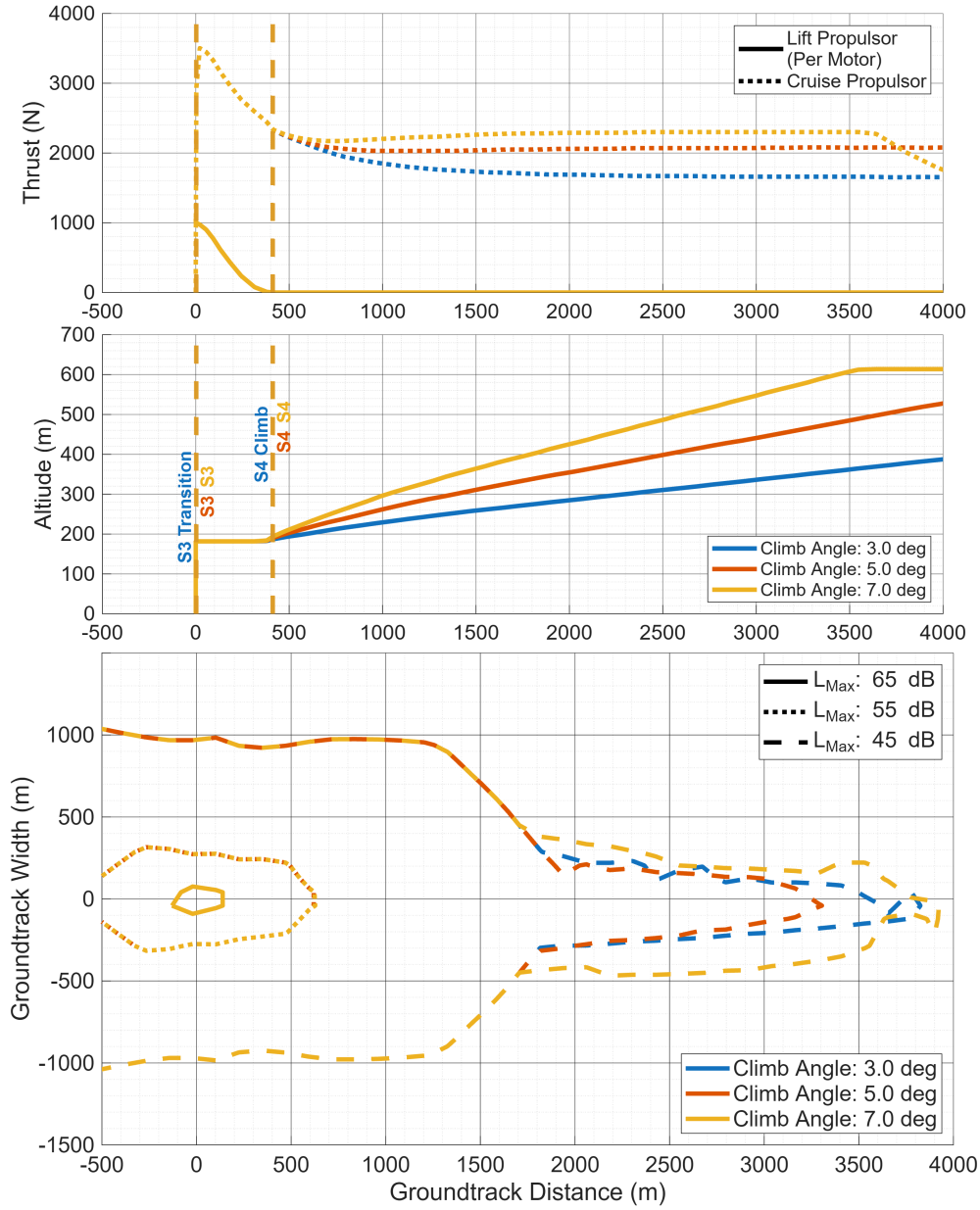


Figure 5.11: L_{Max} community-noise contours for varying LPC climb angles. Solid, dotted, and dashed lines denote 65, 55, and 45 dB, respectively.

The steepest flight profile resulted in the largest noise contour area, but the shallowest profile did not necessarily yield the smallest contour area. At an L_{Max} contour level of 45 dB, climb angles of 3.0 and 7.0 degrees produced contours of similar length. At a distance of 1000 m, increasing the climb angle from 3.0 to 7.0 degrees increases thrust by 380 N, resulting in a 2.1 dB increase in source noise. At the same distance, the 7.0-degree case reaches an altitude

67 m higher than the 3.0-degree case, resulting in roughly 2 dB of additional attenuation due to propagation distance. This observation suggests that, for certain climb angles, the increase in source noise can be partially offset by the additional attenuation associated with increased altitude. Furthermore, the 5.0-degree case produces a noise contour nearly 800 m shorter than those of the shallow and steep climb cases, suggesting that the tradeoff between source noise and altitude can reduce the overall community noise footprint.

5.1.4.2 Variations in Transition Angle

The effects of climb angle were most pronounced for the lower 45 dB L_{Max} contour since most profiles do not begin the climb segment until approximately 500 m into the departure profile. In contrast, transition occurs earlier in the departure profile and therefore has greater potential to affect noise contours closer to the departure origin. Figure 5.12 presents thrust, altitude, and community noise contours for four transition angles. All profiles have a hover altitude of 152 m and a climb angle of 5.0 degrees. As indicated by the thrust plots, the transition segment requires the highest thrust and therefore produces the highest source noise among the flight segments. Minimizing the duration of the transition segment has the potential to reduce community noise. For the 55 dB contour, all departure profiles share roughly the same width but vary in length. As indicated by the segment markers in the altitude and thrust plots, profiles with shallower transition angles begin climb over a shorter ground-track distance than steeper profiles. Steeper transition profiles produce longer noise contours because they spend more time in the high-thrust, high-source-noise transition state. Although these profiles gain altitude more quickly than their shallower counterparts, the additional attenuation associated with increased altitude is insufficient to offset the increase in source noise. Once in cruise, the 0–4-degree transition cases have similar 45 dB L_{Max} contours since they all begin climb between ground-track distances of 400 and 800 m. The 5.0-degree transition-angle profile does not begin climb until a ground-track distance of 1700 m and, as a result, produces a wider noise contour than all other profiles. These observations indicate that although shallow transition profiles maintain the vehicle closer to the ground,

they can reduce community noise by minimizing the time spent in the high-thrust transition configuration.

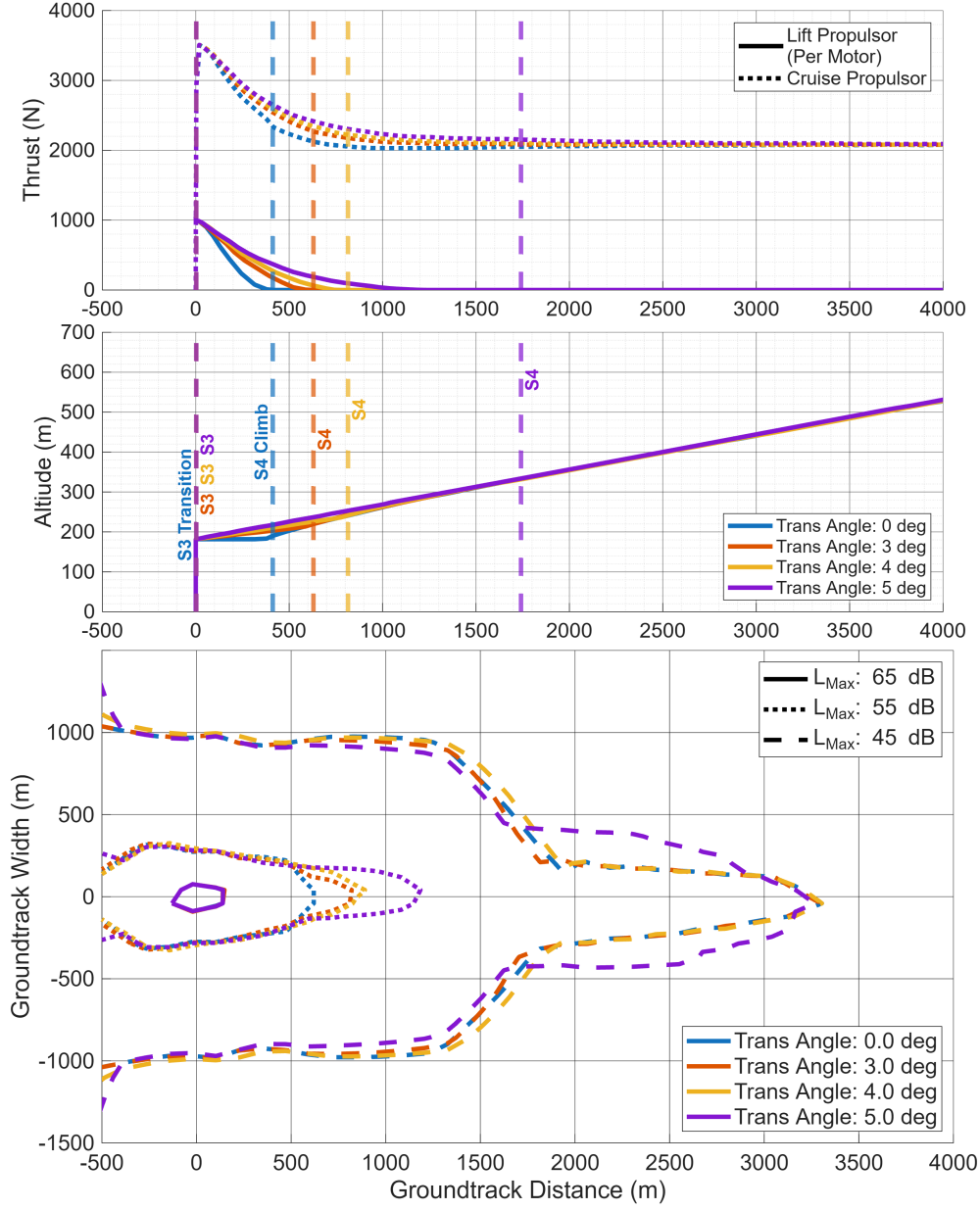


Figure 5.12: L_{Max} community-noise contours for varying LPC transition angles. Solid, dotted, and dashed lines denote 65, 55, and 45 dB, respectively.

5.1.4.3 Variations in Hover Altitude

Similar to transition and climb angles, hover altitude influences both the duration of flight spent in the hover configuration and the altitude at which transition begins. Higher hover

altitudes increase noise attenuation during subsequent flight segments; however, reaching these altitudes prolongs the time spent in the high-thrust, high-noise hover segment. Figure 5.13 presents thrust, altitude, and community noise contours for two hover altitudes and two climb angles. All profiles use a level transition angle of 0.0 degrees.

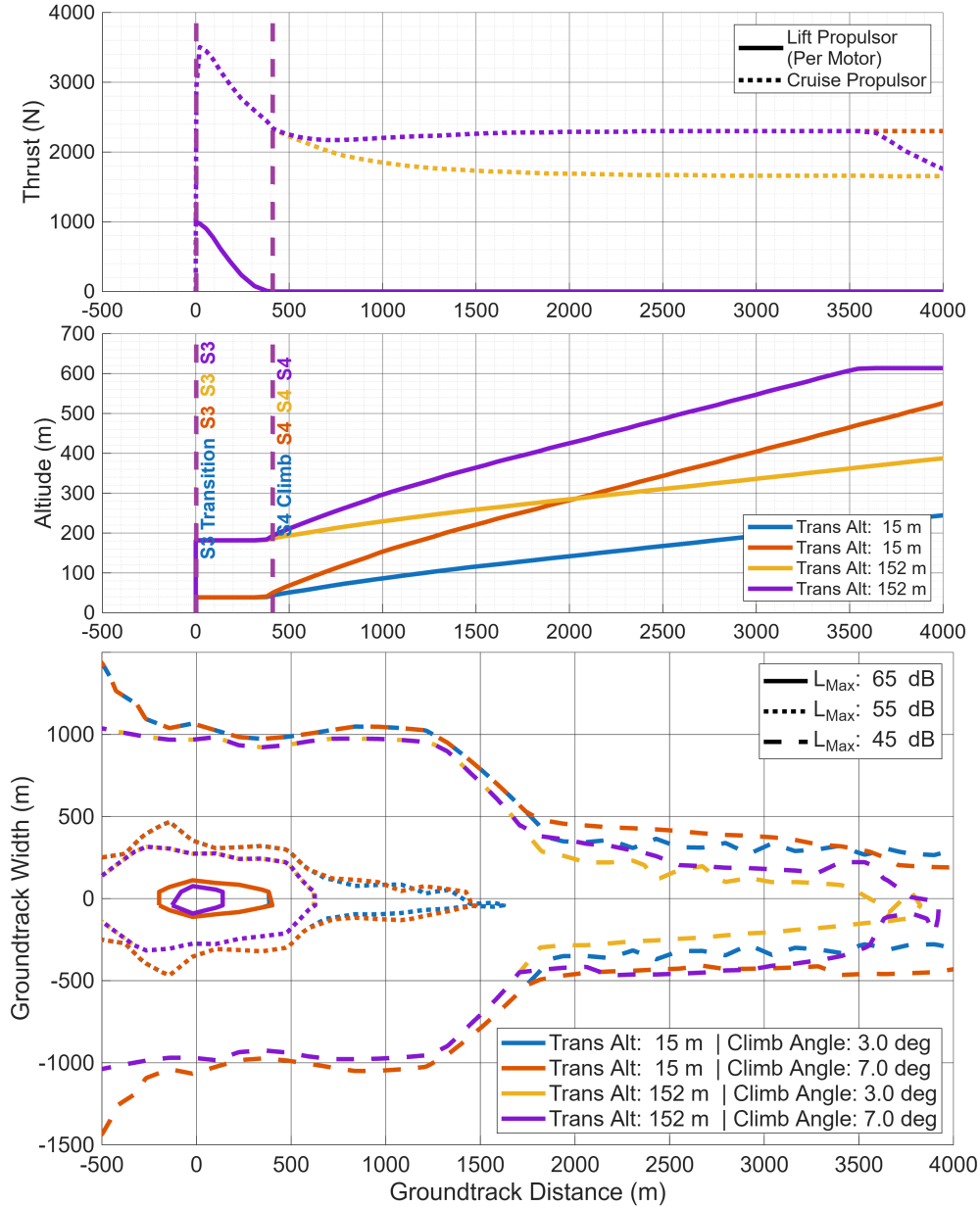


Figure 5.13: L_{Max} community-noise contours for varying LPC hover altitudes. Solid, dotted, and dashed lines denote 65, 55, and 45 dB, respectively.

Both 152 m hover-altitude profiles have the smallest L_{Max} contour areas at 65 and 55 dB.

The 15 m hover-altitude cases produce substantially wider noise contours that extend an additional 1000 m in ground-track distance. As observed in Section 5.1.4.1, there is little variation in contour area with climb angle because most flight profiles begin the climb segment at a ground-track distance of approximately 500 m. At an L_{Max} contour level of 45 dB, both 15 m cases continue to produce wider and longer noise contours than the 152 m cases. Consistent with the climb-angle variation discussed in Section 5.1.4.1, steeper climb angles produce wider noise contours. This effect is most prominent for the 152 m hover-altitude cases, for which the steeper 7.0-degree climb-angle case produces a 45 dB L_{Max} contour that is slightly wider but similar in length to that of the 3.0-degree case.

Overall, higher hover altitudes reduce community noise during departure. For a given hover altitude, climb angle exhibits behavior similar to that discussed in Section 5.1.4.1, in which altitude gain can be traded against changes in source noise to develop flight profiles that minimize the community noise footprint. Together, these results demonstrate how the timing and duration of high-thrust flight segments, combined with altitude-dependent propagation effects, govern the community noise of the LPC vehicle.

5.2 Community Noise of Blown-Flap STOL

This section presents the design and evaluation of a blown-flap STOL aircraft, with a focus on the interactions between propulsor operation, flight profile, and community noise. Representative STOL propulsors were developed using the propulsor design methodology described previously. Flight profiles were developed to explore the effects of pitch- and RPM-based propulsor operation on flight profile design. The resulting departure profiles were used to generate propulsor source noise, which was subsequently propagated to create community noise contours. This analysis provides a systems-level assessment of how propulsor and aircraft operation influence the performance and community noise of a blown-flap STOL aircraft.

5.2.1 Propulsor Sizing

Table 5.3 summarizes key design parameters for a representative blown-flap STOL vehicle, depicted in Fig. 5.14, based on the work of Courtin et al. [27, 28]. This vehicle type utilizes distributed propulsion to increase local velocity over the aircraft flaps, enabling high C_L values at relatively low airspeeds compared with conventional takeoff and landing (cTOL) vehicles. These advantages are used mainly during takeoff and approach segments, where a blown-flap distributed propulsion system enables the STOL vehicle to achieve a shorter takeoff field length than that of a cTOL vehicle of similar power. Propulsion and vehicle aerodynamic performance characteristics were derived from subscale STOL tests in [28]. The STOL configuration is depicted in Fig. 5.14, with vehicle and propulsor characteristics listed in Table 5.3.

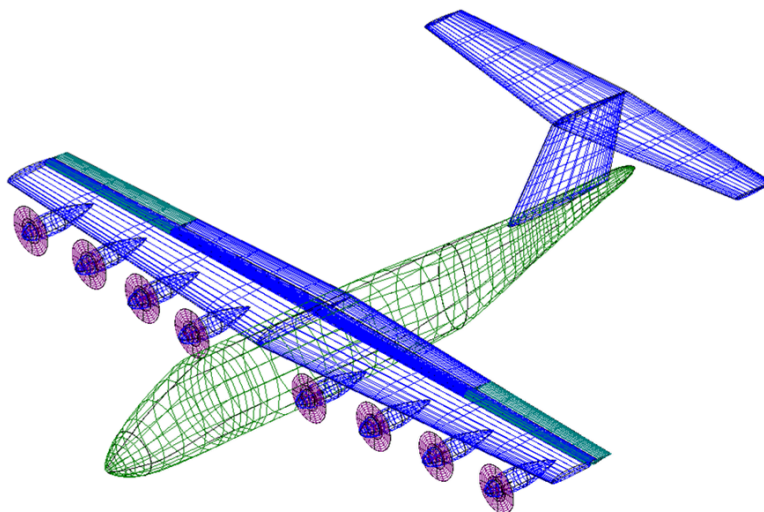


Figure 5.14: Representative blown-flap STOL vehicle model.

Table 5.3: Representative blown-flap STOL vehicle and propulsor characteristics.

Vehicle Characteristics	
Weight (kg)	2722
Wingspan (m)	11
Takeoff Speed (m/s)	20
Cruise Speed (m/s)	77
Performance Characteristics	
Number of Installed Propulsors	8
Max Thrust per Propulsor (N)	1566
Nominal RPM	4000
Max Power per Propulsor (kW)	100
Geometric Characteristics	
Propulsor Diameter (m)	0.8
Blade Count per Propulsor	5

Using the procedure described in Section 3.1, representative propulsors were designed based on the derived top-level aircraft requirements. A design speed of 20 m/s was chosen to represent end-of-takeoff and early-climb conditions because these high-thrust, low-speed segments represent the most demanding operating conditions. A nominal RPM of 4000 was chosen to limit M_{Tip} to 0.40 [11]. Thrust can be controlled by both RPM and blade pitch. Each propulsor was power-limited to 100 kW to represent the capabilities of existing motors. The result was a variable-pitch, variable-RPM, five-bladed propulsor with a radius of 0.4 m, depicted in Fig. 5.15.

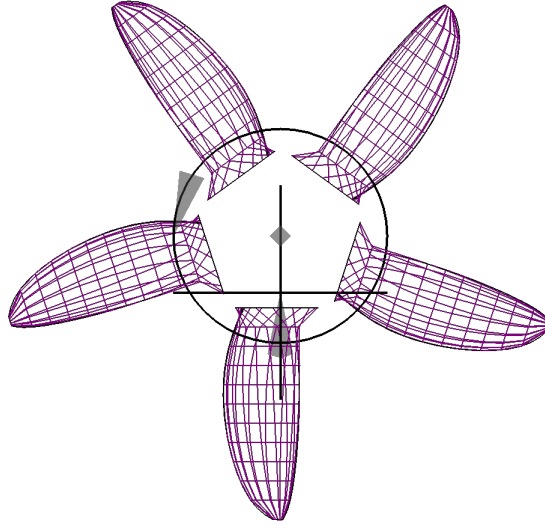


Figure 5.15: Representative STOL propulsor design.

5.2.1.1 Propulsor Operation Strategies

The findings in Section 4.2.3 indicated that, at the same thrust, noise emissions were highly sensitive to tip speed. A potential low-noise approach would minimize RPM while maintaining propulsor power or thrust through increased blade pitch. Since both RPM and pitch were viable means of controlling thrust, they provided significant flexibility in propulsor operation. To this end, two propulsor control strategies were designed to explore this operating space.

The first approach was a constant-power design that varied blade pitch to maintain a constant power level. This strategy allowed RPM to vary while holding power constant. Blade pitch was increased until either the power limit was reached or the blade sectional lift coefficients exceeded 1.5, at which point portions of the blade began to stall. A nominal power of 70 kW was used to allow lower values of RPM before the propulsors were unable to absorb this level of input power. This approach enabled comparison of different RPM and torque combinations to determine whether lower-RPM operation could reduce noise with minimal impact on performance.

The second approach was a constant-pitch operating strategy, in which blade pitch remained fixed across all RPM values and throughout the departure profile. This fixed blade pitch was defined as the nominal pitch that resulted in a shaft power of 100 kW at the maximum RPM of 4000. Lowering RPM reduced power and thrust, enabling the evaluation of variable-thrust takeoff profiles. Together, these propulsor control strategies were used to evaluate the effects of RPM, thrust, and power on community noise.

Both control strategies were applied only during the first three flight segments, which require the most thrust. Upon reaching the climb segment, all flight profiles switched to a constant RPM, and blade pitch was used to obtain the required level of thrust.

5.2.2 Flight Profile Design

The STOL aircraft departure trajectories were designed to evaluate variations in flight operation by changing propulsor RPM, pitch, and power. The rate of climb was determined by the available propulsor thrust for a given RPM and power configuration. The flight trajectories were based on the approaches in [27] and [49] and generated using the procedure described in Section 3.4.

5.2.2.1 Vehicle Physics and Flight Profile Segment Generation

Using a procedure similar to that for the LPC vehicle, the governing equations, Eqs. (3.8) and (3.7), were adapted to the STOL vehicle, as depicted in Fig. 5.16.

$$F_X = T \cos(\alpha) - \sin(\gamma)Lift - \cos(\gamma)Drag \quad (5.3)$$

$$F_Y = \cos(\gamma)Lift + T \sin(\alpha) - Weight - \sin(\gamma)Drag \quad (5.4)$$

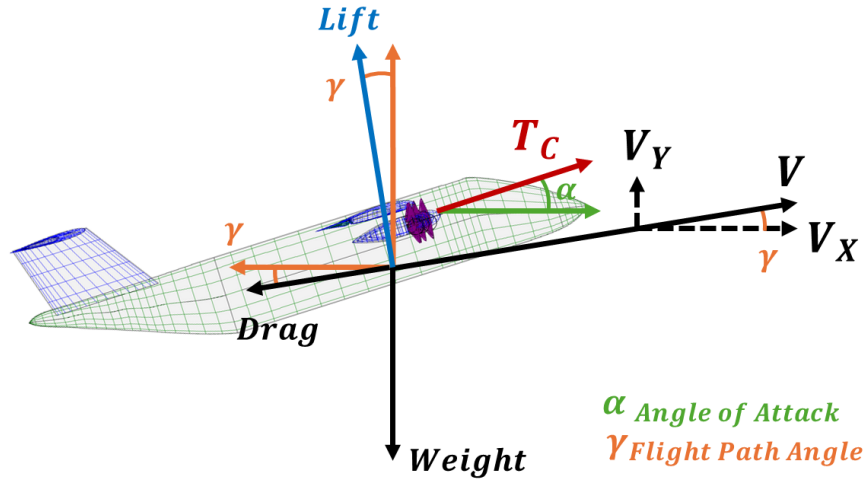


Figure 5.16: Forces, velocity, and angle conventions used for the STOL flight-profile model.

Lift and drag were modeled using the procedure described in Sections 3.2.1.1 and 3.2.1.2. Using Eqs. (3.1)–(3.5), lift and drag were expressed as functions of flap angle, angle of attack, and the jet momentum excess coefficient, ΔC_J . This approach accounted for the effects of propulsor disk height on jet momentum and the effects of flaps on jet deflection, as illustrated in Figure 3.3.

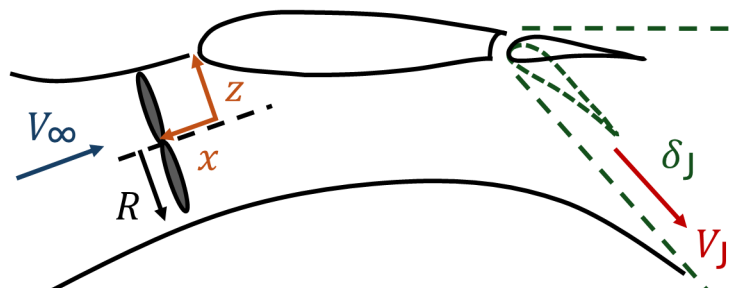


Figure 3.3 (repeated).

Table 5.4 summarizes the flight profile parameters used to define the six departure segments. All takeoff trajectories reach the target altitude, h_2 , and cruise speed after completing the final climb segment. Segment 1 consists of a ground roll and rotation. The length of the

ground roll and the rate of rotation were determined by the flap and RPM settings, with ground-roll distance varying from 75 to 200 m. This segment ends when the pitching moment about the landing gear becomes positive or when the lift produced by the wing equals the vehicle weight. In Segment 2, the aircraft climbs at the same RPM setting and continues until the target altitude h_1 is reached. The flight path angle is increased until the airspeed is approximately constant, trading forward acceleration for a higher climb rate. Angle of attack is set such that lift balances vehicle weight. To maintain stall margins, the angle of attack is limited to 12 degrees. In Segment 3, the vehicle accelerates by retracting flaps at a rate of 1.5 degrees per second and decreasing flight path angle. This continues until a flap-zero speed of 36 m/s is reached. In Segment 4, the flaps fully retract to a clean setting, and the aircraft continues climbing until the cruise altitude, h_2 , is reached. As in Segment 2, the flight path angle is selected to maximize the climb rate while maintaining a constant airspeed. Once at the cruise altitude, h_2 , the aircraft reduces flight path angle in Segment 5 and accelerates until cruise velocity is reached and level flight is established. At cruise conditions, blade pitch is reduced to maintain a constant airspeed, and the aircraft continues in steady, level flight until reaching a ground-track distance of 10 km at the end of Segment 6. Fig. 5.17 illustrates the six segments of an example departure, showing ground-track distance and altitude.

Table 5.4: STOL flight-profile and propulsor operating parameter ranges.

STOL Flight Profile Parameters	
Angle of Attack (degrees)	0–12
Flight Path Angle (degrees)	0–15
Flap Retraction Speed (m/s)	36
Flap Retraction Altitude (m)	30.5–121.9
Cruise Altitude, h_2 (m)	609.6
Ground-Roll Distance (m)	75–200
Performance Characteristics	
RPM_A	3200–4000
RPM_B	3200–4000
Max RPM	4000
Max Power per Propulsor (kW)	100

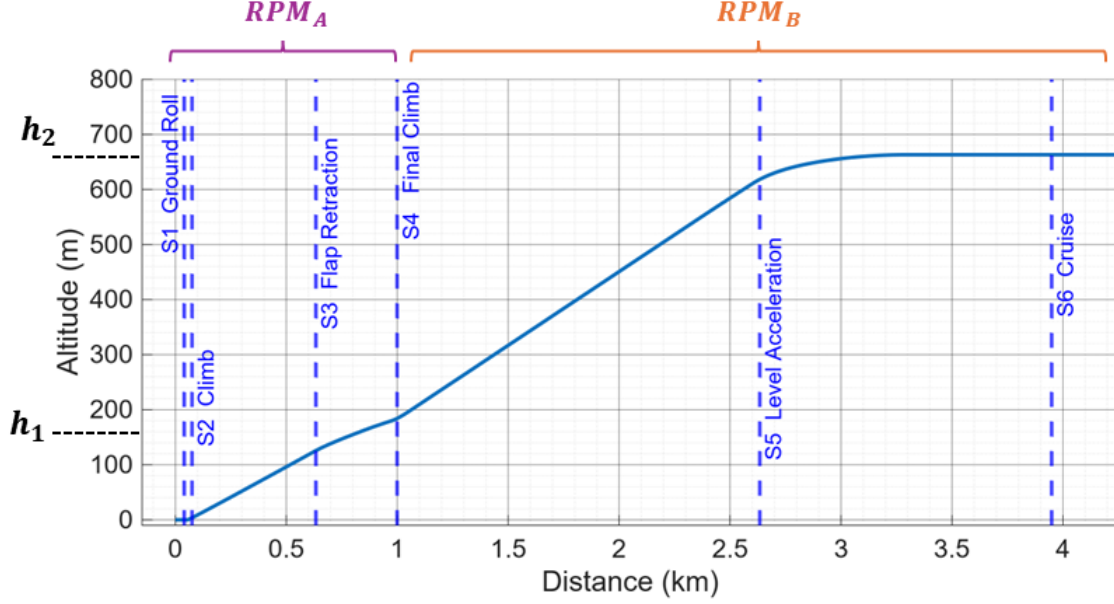


Figure 5.17: Example blown-flap STOL departure profile showing altitude as a function of ground-track distance from ground roll through cruise.

From ground roll to flap retraction (Segments 1–3), RPM is represented as RPM_A , while from climb to cruise (Segments 4–6), RPM is represented as RPM_B . These values can be identical or varied to represent alternative profiles. For example, RPM_B can be reduced once the vehicle is in a clean, low-drag configuration. Power is allowed to change with RPM and airspeed, but is limited to 100 kW per propulsor. Blade pitch is lowered if this limit is reached during departure. It is also lowered to reduce thrust during steady, level cruise. Three RPM levels were considered for the STOL profile variations: 4000, 3600, and 3200, corresponding to 100%, 90%, and 80%, respectively. RPM levels below 80% did not produce enough thrust for the 60-degree flap configurations to maintain a positive flight path angle in Segment 2, while the 40-degree flap configurations reached Segment 4 but could not reach cruise altitude within the 15 km departure window. Table 5.4 summarizes the full set of 18 flight profiles, which represent the expected operational range of the STOL aircraft.

5.2.3 Flight Profile Variations

The following section compares selected flight profiles to evaluate how RPM, power, and thrust affect STOL aircraft performance and, subsequently, community noise. Other variables, such as flap retraction altitude and flap setting, were found to have smaller effects on propulsor performance and noise than RPM, power, and thrust. During the ground roll (S1), initial climb (S2), and flap retraction (S3) segments, the propulsor is controlled by the operating strategies described in Section 5.2.1.1. Once in climb (S4), RPM is fixed at the current setting and thrust is controlled through blade pitch.

5.2.3.1 Flight Profile Variations for Constant-Pitch Operation

Figure 5.18 depicts flight profiles with constant blade pitch at three levels of RPM. An additional cutback case is included in which RPM starts at 100% but is reduced to 80% for the rest of the climb and cruise. Flap retraction begins at an altitude of 30.5 m, and flaps are initially set to 40 degrees for all profiles.

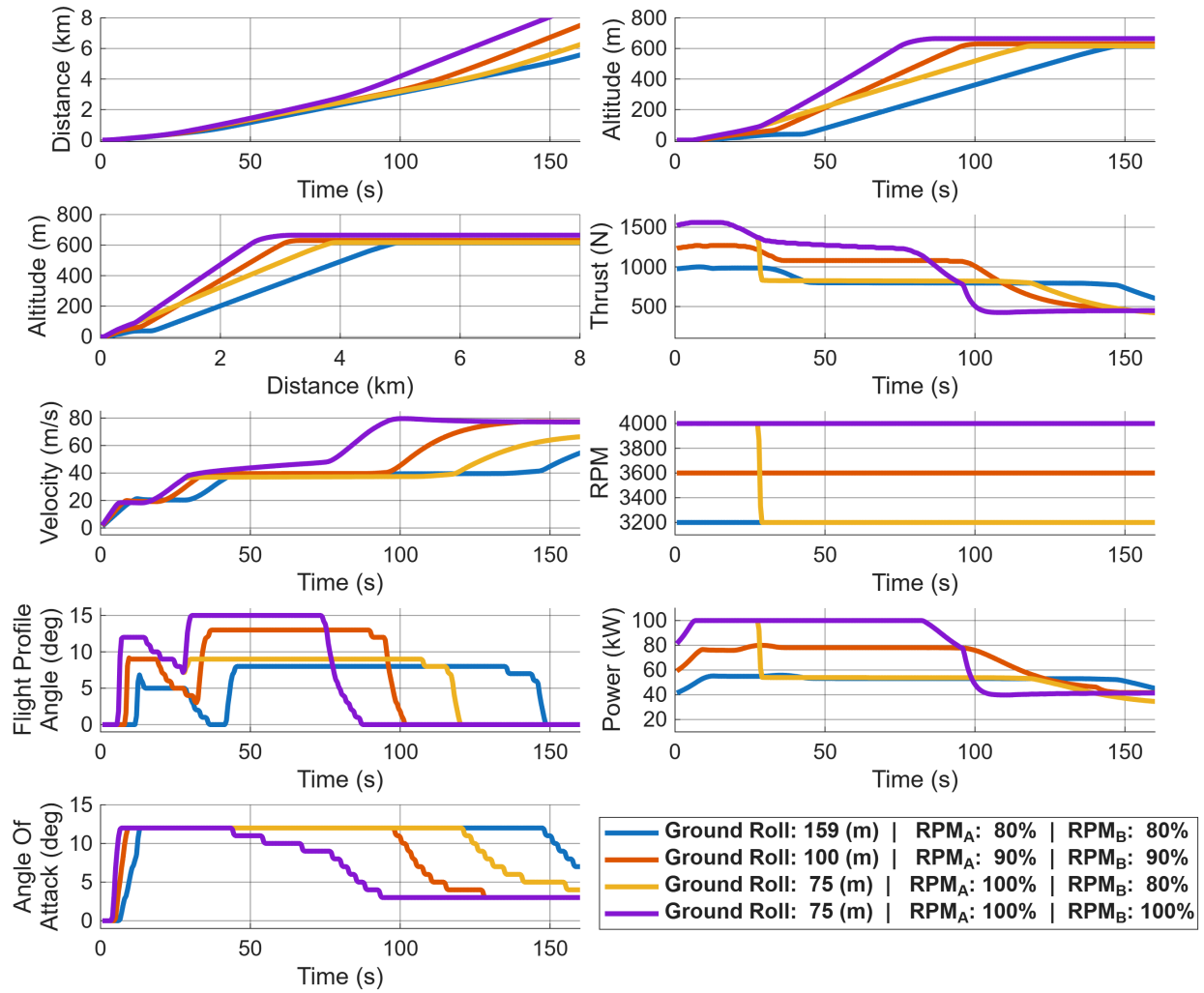


Figure 5.18: STOL departure profiles at constant blade pitch and varying RPM with fixed flap setting and retraction altitude.

RPM shows a strong correlation with thrust and power. During initial climb, reducing RPM from 100% to 90% reduces thrust by 339 N, while reducing RPM from 100% to 80% reduces thrust by 567 N. These differences in thrust are maintained for much of the departure and form three distinct bands of thrust and power corresponding to the three RPM values. The lower levels of available thrust result in longer ground rolls and lower rates of climb. These effects are more noticeable as RPM decreases. At 90% RPM, the climb angle decreased by 2 degrees, while at 80% RPM, it decreased by 6 degrees. This shallower climb extended the time to cruise by 25 s at 90% RPM and 75 s at 80% RPM. The cutback profile produced

performance intermediate between the 90% and 80% RPM cases, with a 50 s increase in the time required to reach cruise altitude. This profile started with an aggressive rate of climb, but after cutback, the higher flight path angle of the 90% RPM case offset this initial advantage.

5.2.3.2 Flight Profile Variations for Constant-Power Operation

The second set of flight profiles, depicted in Fig. 5.19, utilized the constant-power approach, in which blade pitch varied as RPM changed. Flap retraction begins at an altitude of 30.5 m, and flaps are initially set to 40 degrees for all profiles. Thrust is nearly identical for the 100% and 90% RPM cases, which differ by less than 50 N for most of the departure profile before cruise. The 80% RPM case matches the other cases closely during cruise but appears to be limited in the amount of power that can be absorbed at that RPM during the flap retraction segment. Likewise, the 90% RPM case is also power- and thrust-limited during the low-speed ground-roll segment. This results in slightly longer ground rolls for the 80% and 90% cases, but the increase is much smaller than in the variable-power cases presented in Section 5.2.3.1. Since all cases have the same thrust during climb, the cutback profile has little performance variation relative to the full 100% and 90% cases and reaches cruise altitude at nearly the same time as they do. The 80% RPM case is slightly delayed by its initially lower rate of climb but still reaches cruise within 50 s of the other profiles.

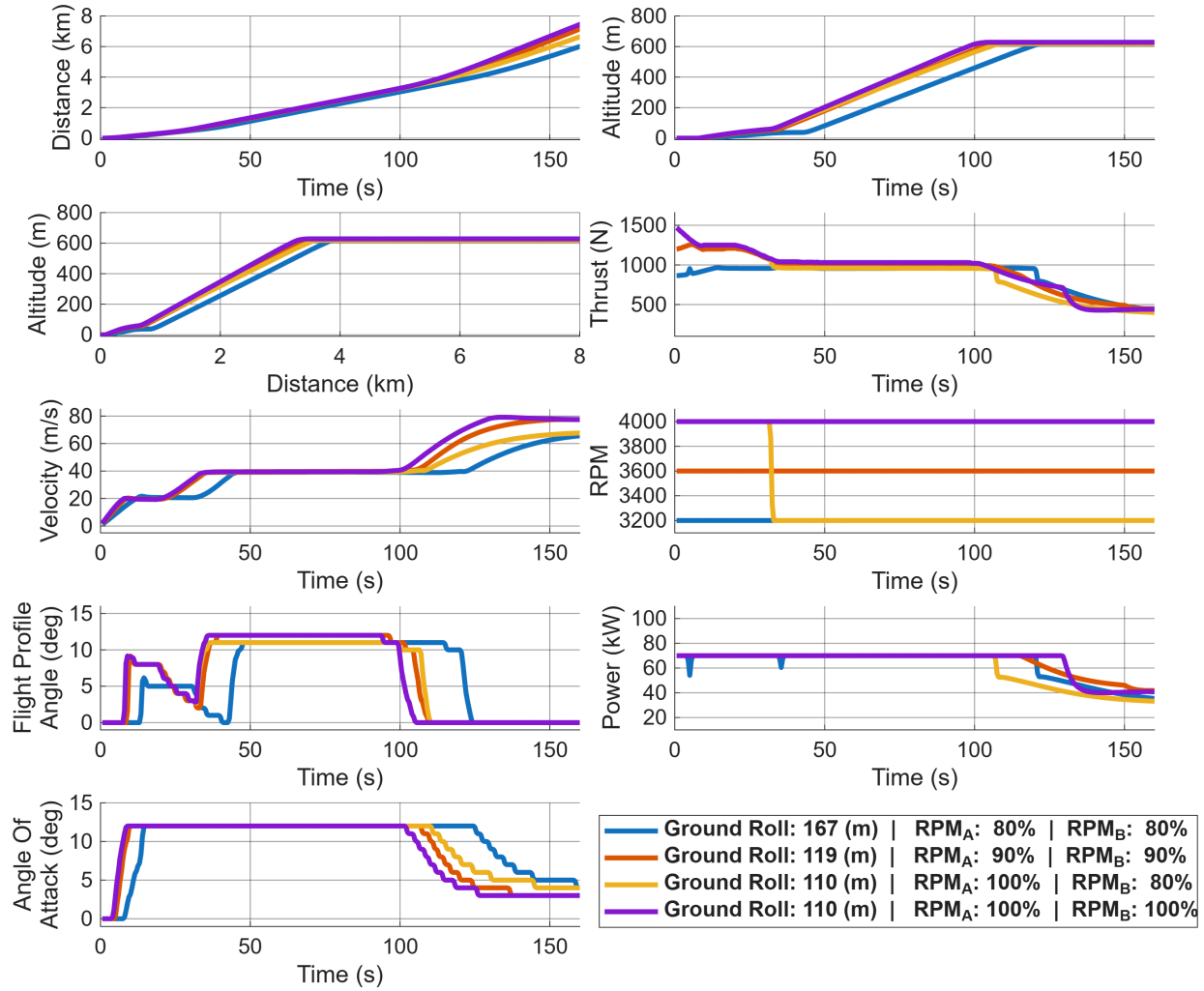


Figure 5.19: STOL departure profiles at constant power and varying RPM with fixed flap setting and retraction altitude.

5.2.4 Propulsor Source Noise Characteristics

Each flight profile was divided into discrete time points that specified operating and position information, including inflow velocity, RPM, thrust, and propulsor pitch angle. These points were aggregated across flight profiles, processed to remove duplicates, and used as inputs to generate a database of source noise hemispheres using the procedure described in Section 3.3. When analyzed as a function of power and velocity, these aggregated results provide insight into how the set of representative flight profiles affects the range of propulsor operation and the resulting noise emissions.

5.2.4.1 Source Noise for Constant Pitch Propulsor Operation

Figures 5.20a and 5.20b depict A-weighted OASPL source noise as a function of airspeed, power, and thrust for propulsors operating with variable power. Noise was modeled at an undertrack location 25 m below the aircraft. Three distinct lines correspond to the three values of RPM. Because these points were sampled from distinct flight profiles and both power and thrust were allowed to vary, there are almost no overlapping points at which different RPM levels share the same velocity-thrust or velocity-power condition. Higher thrust levels occur at airspeeds below 35 m/s, which correspond to the more demanding ground-roll, initial climb, and flap retraction segments. These high-power and high-thrust conditions also correspond to the highest noise levels. Between 40 and 60 m/s, thrust begins to decrease as the vehicle gains speed and completes the climb segment. For all RPM values, propulsor noise is lowest during cruise when thrust and power are lowest.

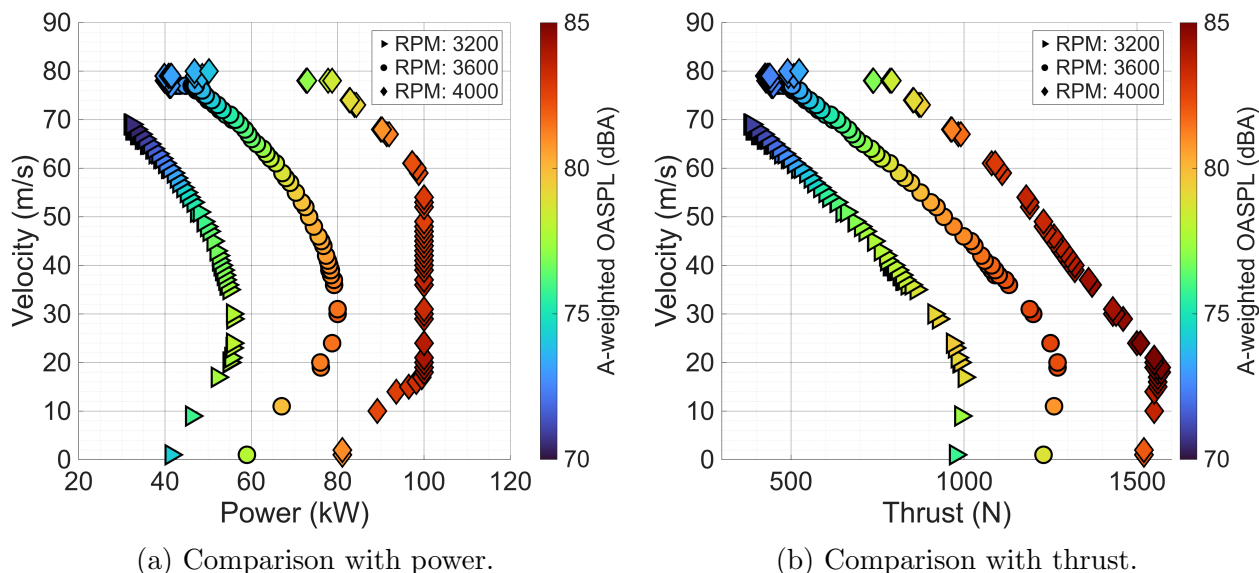


Figure 5.20: Undertrack A-weighted OASPL as a function of airspeed, power, and thrust for constant-pitch propulsor operation.

5.2.4.2 Source Noise for Constant Power Propulsor Operation

Figures 5.21a and 5.21b depict source noise as a function of airspeed, power, and thrust for propulsor operation with constant power. At higher airspeeds, propulsor noise shows trends

similar to those of the constant-pitch results, decreasing with decreasing power and thrust. This is expected since these conditions correspond to late-climb and early-cruise conditions, where airspeed, drag, and required thrust converge toward steady-state cruise. However, the results differ substantially during the constant-power segments. All three RPM levels have overlapping points at 40 m/s near 1000 N of thrust. However, despite similar thrust values, noise varies by up to 4.2 dBA and increases as RPM decreases. A possible explanation for this behavior is that, at a constant power of 70 kW, lower RPM increases blade loading as the propulsor operates farther from its 4000 RPM design condition. While the results of Section 4.2.3 indicated that noise was primarily influenced by tip speed, those findings were based on propulsors designed for the corresponding operating conditions. The flight-profile-based results suggest that lowering RPM through increased blade pitch may not effectively reduce noise within the operating range of this aircraft.

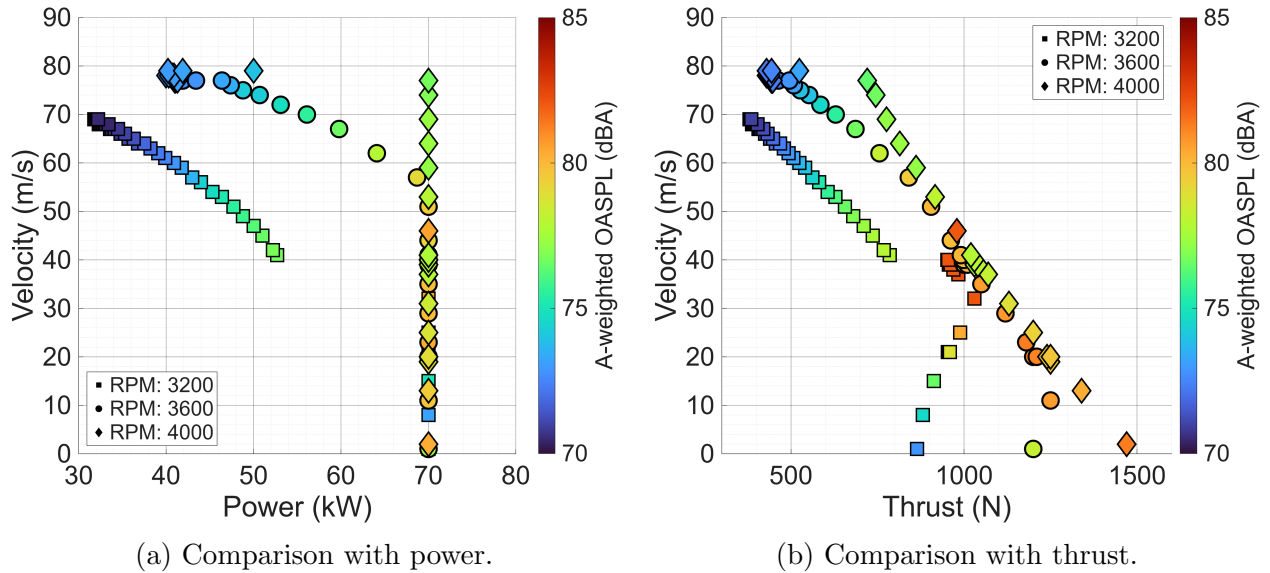


Figure 5.21: Undertrack A-weighted OASPL as a function of airspeed, power, and thrust for constant-power propulsor operation.

5.2.5 Community Noise Contours

The previously generated set of source noise hemispheres is propagated to ground-based observers along the flight profiles to obtain community noise. The following sections present

departure profile noise contours of maximum sound pressure level (L_{Max}) at 45, 55, and 65 dB. These noise levels are similar to those used by industry and regulatory agencies [67]. Thrust and altitude plots are scaled and vertically aligned with the community noise contours along the departure distance to illustrate the flight condition and aircraft position. Finally, Segments S3 and S4 are indicated for all flight profiles to identify the start of flap retraction and the beginning of climb, respectively.

5.2.5.1 Community Noise for Constant Pitch Operation

Figure 5.22 presents thrust, altitude, and community noise contours at three levels of RPM for flight profiles generated with the variable-power propulsor control strategy. The full 100% RPM case forms the widest and longest contour at all L_{Max} levels. Although this case has the highest climb rates, the high source noise observed in Fig. 5.20 offsets the additional noise attenuation associated with its greater altitude. For the variable-power propulsor control strategy, source noise has a greater influence on community noise than the differences in flight profile, and higher RPM results in longer and wider noise contours. The 45 dB contour of the cutback case is nearly identical in width to that of the full 100% RPM departure. However, upon reducing RPM, this contour abruptly terminates, resulting in a contour length slightly shorter than that of the full 80% RPM case. The higher-thrust initial climb of the cutback profile resulted in an altitude approximately 200 m greater than that of the 80% case, and once RPM was reduced to the same level, the cutback case benefited from greater attenuation due to its higher altitude. These results indicate that for constant-pitch operation, RPM is the dominant factor affecting community noise. While flight operation, such as an RPM cutback, can affect contour length, reducing RPM is the primary means of reducing noise contour area, at the cost of slower, longer departure segments.

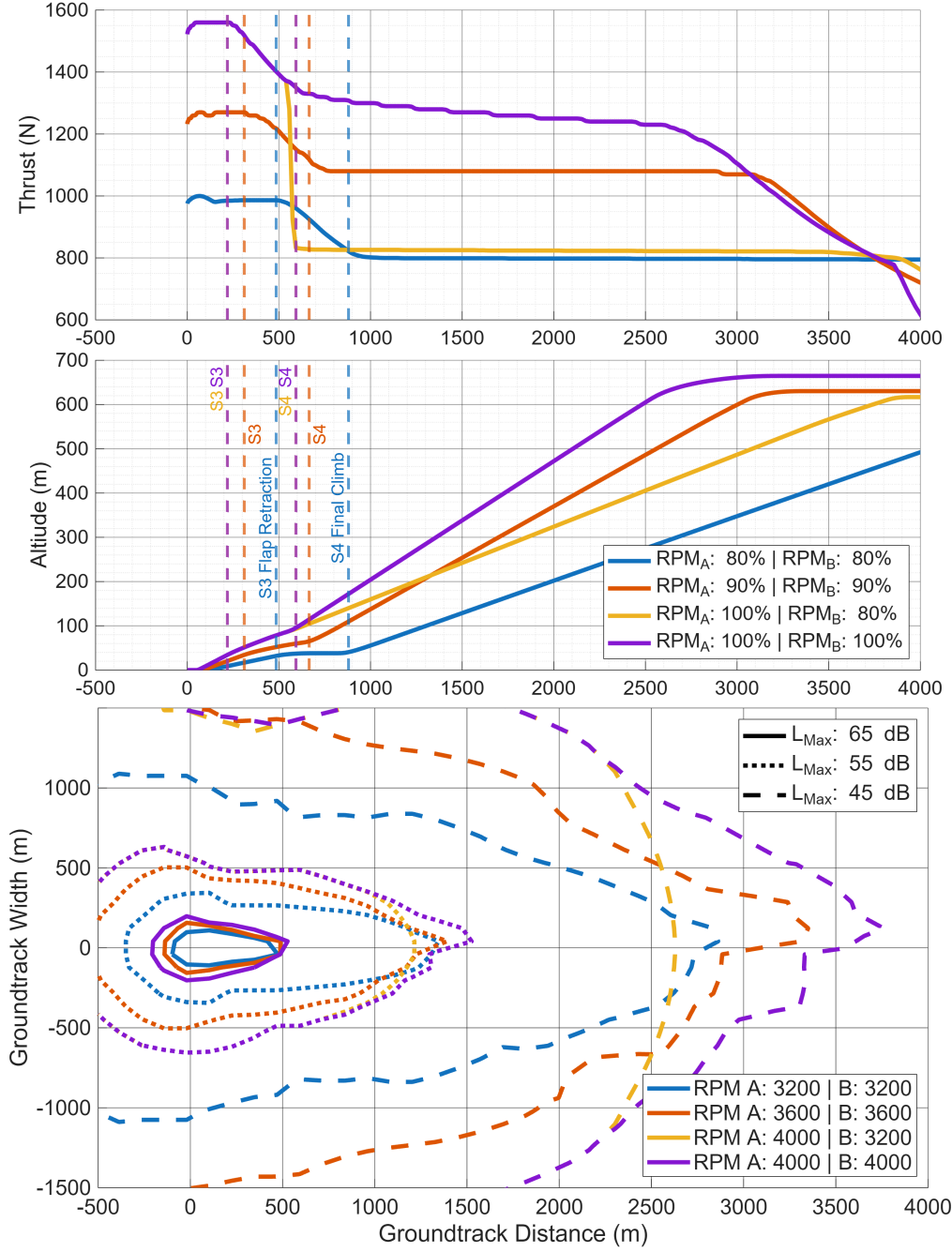


Figure 5.22: Thrust, altitude, and L_{Max} community-noise contours for constant-pitch STOL operation at varying RPM.

5.2.5.2 Community Noise for Constant Power Operation

Figure 5.23 presents thrust, altitude, and community noise contours at three levels of RPM for flight profiles generated using the constant-power propulsor control strategy.

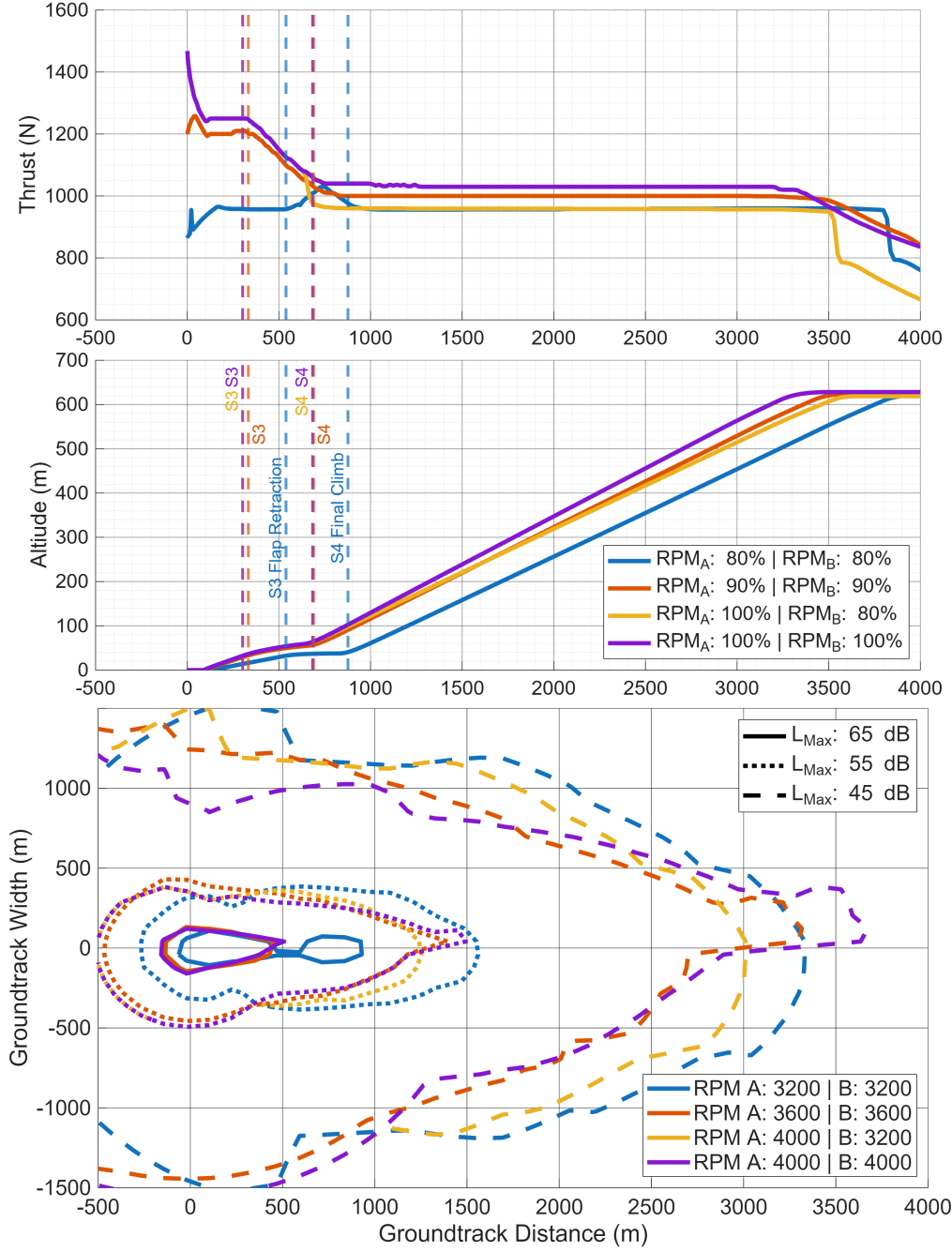


Figure 5.23: Thrust, altitude, and L_{Max} community-noise contours for constant-power STOL operation at varying RPM.

Higher values of RPM produce more thrust during ground roll and initial climb, resulting in a slightly higher rate of climb. This causes flight profiles with higher values of RPM to have narrower and slightly longer contours than those of cases with lower RPM. At an L_{Max}

of 45 dB, the 80% and 90% RPM contours extend to 3200 m, while the 100% RPM contour extends past 3500 m. The 100% and 90% cases are similar in width, while the 80% case is significantly wider. The cutback case is the shortest contour at 3000 m but lies between the 80% and the 90% cases in width. At an L_{Max} of 55 dB, the cutback, full 90% RPM, and full 80% RPM cases have similar teardrop-shaped contours that are wide near the runway and taper with increasing altitude. The 80% case is more oblong, starting narrower but remaining wide for a longer distance.

Compared with the constant-pitch, variable-power contours in Section 5.2.5.1, these constant-power flight profiles have much less variation in contour area. These results suggest that RPM can be traded against blade pitch while maintaining relatively similar flight profiles, but this strategy has a less consistent effect on community noise.

5.3 Conclusions

This chapter demonstrated the ability of the methodology outlined in Chapter 3 to integrate propulsor design, flight profile generation, source noise prediction, and community noise evaluation across different multi-propulsor aircraft architectures. Representative propulsors were developed for both blown-flap STOL and LPC aircraft using top-level vehicle requirements and available design information. The resulting performance characteristics were used to generate flight profiles with feasible operating conditions unique to each vehicle architecture. These operating conditions were used to generate a source noise database that could be applied across flight profiles to predict community noise beyond the original set of trajectories. Results from both aircraft demonstrate that community noise is influenced by the combined effects of propulsor source noise and flight-profile design.

The LPC vehicle demonstrated that the relative impact of propulsor source noise and flight profile variation on community noise depends strongly on the operating conditions of each flight segment. Variations in climb angle showed that increases in source noise associated with

higher thrust can be traded against the additional attenuation associated with more rapid altitude gain, potentially reducing the resulting noise contour area. Transition-angle variations demonstrated that during operating states with high source noise, community noise can be reduced by minimizing the duration of the transition segment. Because transition occurs over a relatively short portion of the departure, there is less potential for attenuation associated with altitude gain, and source noise has a greater influence on community noise. Hover-altitude variations showed different behavior, with community noise reduced primarily through increased propagation distance. Although hover was one of the highest-source-noise segments and higher hover altitudes prolonged its duration, these effects were offset by the rapid gain in altitude and additional attenuation during all subsequent departure segments. Together, these results demonstrate that effective means of reducing LPC community noise depend on the balance between source noise, segment duration, and altitude.

The STOL vehicle demonstrated that RPM and blade pitch provide feasible means of controlling thrust, but each operating strategy has different effects on achievable flight profiles and community noise. For constant-pitch operation, thrust and power were controlled primarily through propulsor RPM. Analysis of the resulting operating states revealed that propulsor power was a strong indicator of source noise across the range of RPM and airspeed conditions. Reducing RPM lowered propulsor power and source noise, but also reduced available thrust, decreased climb rate, and prolonged the departure profile. Despite the reduced attenuation associated with these shallower flight profiles, the decrease in source noise had the greater impact, resulting in smaller community noise contours at lower RPM. For a fixed blade-pitch configuration, reducing RPM and, consequently, propulsor power was therefore the most effective means of reducing community noise, although this benefit came at the cost of extended departure profiles.

For constant-power operation, blade pitch was varied with RPM to maintain a specified propulsor power. This approach allowed RPM to change with relatively small variations

in thrust, climb angle, and departure duration, reducing the influence of flight profile on community noise. However, the source noise analysis showed that lower RPM did not necessarily produce lower noise when power was held constant. Under high-thrust, low-speed conditions, lower RPM required increased blade loading and caused the propulsor to operate farther from its nominal design condition. This resulted in higher noise for the same thrust and velocity conditions. The resulting community noise contours therefore showed considerably less variation with RPM than those of the constant-pitch cases. These results indicate that RPM can be traded against blade pitch to maintain propulsor power and departure performance, but this trade does not necessarily reduce community noise. Instead, the acoustic benefit of reducing RPM depends on how the resulting blade loading and propulsor operating condition affect source noise.

Overall, these results demonstrate that community noise is impacted by both propulsor operation and flight profile design. Reductions in source noise do not necessarily result in lower community noise when they also reduce flight performance, whereas higher source noise can be offset by increased altitude or reduced segment duration. The relative importance of these effects varies with vehicle architecture and operating conditions, demonstrating the need to evaluate propulsor design, aircraft operation, and noise propagation as an integrated system. This methodology provides a means of identifying these tradeoffs and evaluating flight procedures that balance aircraft performance with reductions in community noise.

Chapter 6

Performance and Operation Exploration of Regional Turboprops Using Distributed Electric Propulsion

Many studies have been conducted on turboprop aircraft and their potential to offer solutions for sustainable aviation. Rising fuel costs and environmental concerns have recently directed commercial and research efforts into expanding the use of turboprops in current aviation markets [13]. Routes under 1000 nautical miles make up nearly half of US domestic flights and are dominated by regional jets and narrow-bodies [53]. Turboprops operate with substantially less fuel burn than turbofans and are especially suited for short flights due to their ability to operate at low cruise altitudes at lower speeds. Existing turboprop aircraft have capacities of over 60 seats and many manufacturers are proposing 90-plus stretched variants [20]. Turboprop aircraft are well poised to replace many existing aircraft and market segments due to their lower operating costs and fuel economy [32]. These existing capabilities, infrastructures, and commercial demand make turboprop aircraft an ideal platform to explore the implementation of advanced technologies such as electrification. Distributed electric propulsion decouples an aircraft's energy source from its propulsors and allows the use of electric and hybrid-electric propulsion systems. Electric motors allow much more freedom in the layout and operation of propulsors, which allows the design of propulsors with lower rotational speeds and lower disk loading. This results in potentially quieter and more efficient propulsors.

The goal of this chapter is to explore how propulsor geometry, operation, and overall arrangement affect propulsor efficiency and noise emissions when applied to a regional turboprop

aircraft. Additionally, the performance of DEP arrangements at high cruise speeds is compared with that of conventional turboprops and regional turbofan aircraft. The assessment is conducted using a representative retrofitted ATR 72-500 aircraft. Section 6.1 describes the design of DEP arrangements on a turbo-electric platform. Propulsor geometry, operation, and arrangement are varied, and the resulting changes in propulsion weight and drag are used to modify propulsor requirements to ensure that takeoff and design performance are maintained. Section 6.2 presents the resulting effects on efficiency, noise emissions, and high-speed operation. Power and propulsor efficiency curves show the effects of propulsor design and the impacts of DEP arrangements on required power during takeoff and cruise. The fuel burn of the DEP arrangements is also compared with that of conventional turboprop designs. The noise emissions of the DEP arrangements are also evaluated during takeoff and climb conditions, including community noise contours for propulsor count variation. Finally, the high-speed fuel burn of the DEP arrangements is compared against turbofan aircraft of similar size.

6.1 Design and Analysis of DEP Turboprop Arrangements

Figure 6.1 illustrates the procedure for designing DEP turboprop arrangements, evaluating their performance, modeling propulsor and engine core noise, and creating community noise contours. The main inputs are aircraft, flight profile, and propulsor characteristics. Aircraft characteristics are parameters used to calculate lift and drag, such as wing geometry, zero-lift drag coefficient, and total weight. Flight profile characteristics are used to determine operating points and include climb rate, speed schedules, and operating altitudes. Finally, propulsor characteristics include known geometric parameters, such as diameter and blade count, and operating conditions, such as RPM and thrust. These parameters are used as a reference for baseline performance and as a starting point for modeling alternative DEP configurations.

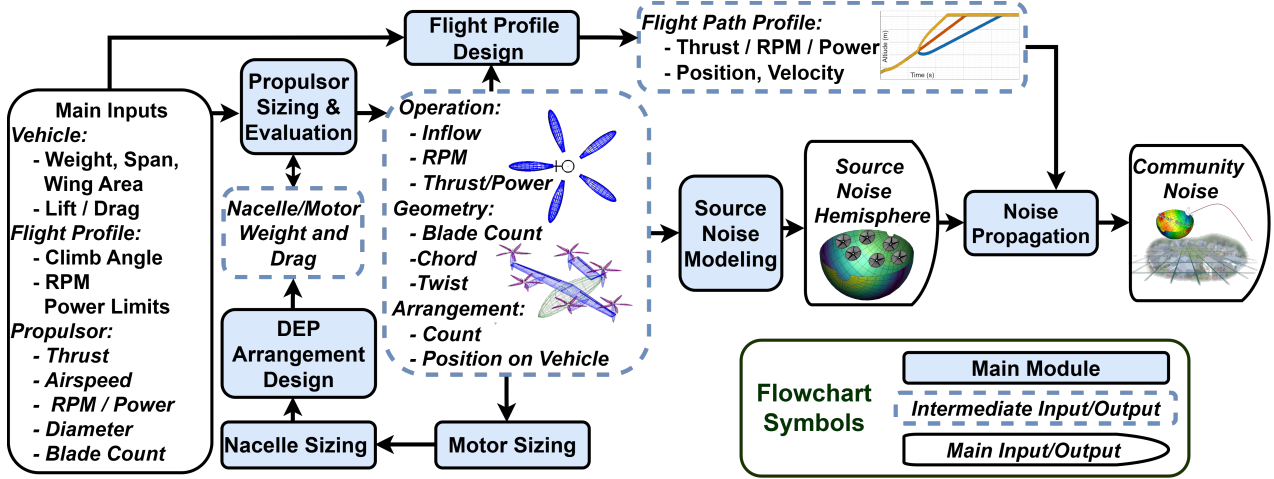


Figure 6.1: DEP turboprop design, performance, and noise-evaluation process.

During propulsor sizing and evaluation, detailed propulsor designs are created from the main inputs using blade element momentum theory (BEMT), as described in Section 3.1. The outputs of this process include parameters such as blade count, chord and twist distributions, and operating conditions such as torque and power. This process also calculates parameters such as the coefficient of thrust, C_T , and propulsor efficiency, η , which are used to normalize propulsor performance for comparisons.

DEP arrangements are created using the resulting outputs and a design criterion, such as a specific coefficient of thrust, a desired disk loading, or a wing span limit. For example, DEP arrangements may be designed to maintain a specified tip speed for all propulsor counts. In this chapter, all DEP arrangements were designed to minimize disk loading for a set span length at a given coefficient of thrust. This criterion resulted in a set of designs that vary in propulsor count, tip speed, and disk loading.

Changes in propulsor count affect not only propulsor design but also nacelle and motor design. These components are sized based on propulsor power, diameter, and RPM as described in Section 3.2.2. For each arrangement to maintain a desired level of performance, such as a specified flight path angle, the design thrust must be altered to account for varia-

tions in drag and weight resulting from the additional nacelles and motors. These updated thrust requirements are used to resize the propulsors, which, in turn, alter the motor and nacelle weight and drag through changes in propulsor power. This process continues until the required thrust and resulting power converge.

The finalized propulsor designs are used to determine flight-profile operating conditions, which include propulsor power, RPM, and inflow velocity, as described in Section 3.4. Operating points are selected to represent key flight segments, such as climb and cruise. These operating points are used in conjunction with the finalized propulsor geometry to create source noise hemispheres using the process detailed in Section 3.3. Source noise represents the localized noise around the aircraft and characterizes its noise magnitude and directivity. These source noise hemispheres are propagated along the flight profile to create community noise contours, which represent noise levels over the ground during aircraft operation. These processes are described in detail in the following sections.

6.1.1 Baseline Turbo-Electric Aircraft

A medium-range regional turboprop aircraft was selected as the reference vehicle for this chapter, as it enabled the resulting DEP propulsor designs to be compared with existing commercial aircraft with well-documented performance characteristics, such as cruise Mach number and brake-specific fuel consumption (BSFC). This reference was used to design a twin-propulsor turbo-electric powertrain, which would then be used to develop DEP arrangements. The objective was not to replicate the reference aircraft exactly or compare powerplant architectures, but rather to establish a representative level of performance against which the DEP configurations could be evaluated in terms of noise and performance.

6.1.1.1 Existing Turboprop Specifications

The aircraft geometry, aerodynamic drag, power requirements, and operating conditions were based on the ATR 72-500. General characteristics, such as propulsor diameter and

blade count, were obtained from published sources [10, 31, 3], while hub and wake diameters were estimated from available engineering drawings and photographs. Aircraft parameters, including maximum take-off weight and wingspan, were obtained from published specifications [3]. Operating conditions, including take-off and cruise power requirements and flight speeds, were derived from publicly available aircraft data [31]. Engine performance and mass were obtained from engine certification documentation [38] and operator guides [7]. Aerodynamic drag coefficients were obtained from a published drag build-up study on the ATR-72 airframe [82]. The required takeoff thrust was calculated from the estimated aircraft drag and a longitudinal acceleration of 1.4 m/s^2 , which was derived from the aircraft's published takeoff field length and rotation speed [10]. Cruise thrust was subsequently determined from the steady-state drag at the selected cruise operating condition.

6.1.1.2 Turbo-Electric Configuration

The ATR-72-500 uses a geared turboprop propulsion system in which each engine core mechanically drives a propulsor. The DEP arrangements of this study, however, assume a turbo-electric propulsion architecture in which one or more engine cores generate electricity that powers electric motors that drive the propulsors. This necessitates reconfiguration of the baseline design, which retains a conventional propulsor configuration with high RPM and high disk loading but is electrified so that differences in noise and performance are due to variations in DEP design rather than propulsion architecture.

Power-plant weight, including the motor and nacelles, is assumed to be the main change for the electrified design. For the scope of this chapter, the existing wing, fuselage, empennage, and control surfaces do not change with electrification. Reconfigured motor and nacelle weights were based on studies of the implementation of electric retrofits on commercial turboprop aircraft. Gesell et al. presented one such work exploring various electric and hybrid-electric powertrains for the ATR-72 [46]. One such configuration was a power-controlled turbo-electric architecture consisting of a gas turbine, electric motor, wiring, power

electronics, and a cooling system. This resulted in a net increase of 1,151 kg per propulsion unit relative to the ATR-72’s existing geared turboprop design. As a conservative estimate, the electrified conventional twin-propulsor baseline was assumed to have an additional 2,400 kg of total propulsor weight added to the ATR-72 takeoff gross weight of 22,000 kg. All percentage changes in DEP motor and nacelle weight are with respect to this 24,400 kg baseline. The final baseline turbo-electric non-DEP aircraft configuration is summarized in Table 6.1.

Table 6.1: Baseline turbo-electric non-DEP aircraft characteristics.

Design Conditions	Takeoff	Cruise
Power/Propulsor (kW)	1820	1590
Thrust/Propulsor (kN)	20	7
Total Vehicle Thrust (kN)	40	14
Speed at Condition (m/s)	59	140
Speed at Condition (kts)	115	272
General Characteristics		
C_{D0}	0.027	
Wingspan (m)	27	
Wing Planform Area (m ²)	61	
MTOGW (kg)	24,400	
Nominal RPM	1200	
Nominal Climb Angle (deg)	4.5	
Baseline Propulsor Characteristics		
C_T	0.20	
Propulsor Count	2	
Blade Count	6	
Radius (m)	1.97	

6.1.2 Propulsor Sizing and Evaluation

Propulsors are designed using BEMT methods in XROTOR [33]. Required inputs are propulsor diameter, hub diameter, blade count, airspeed, and RPM. Either thrust or power may be specified. Additional parameters, such as sectional c_l , may be specified if known. Blade chord and twist distributions are generated to minimize induced losses. The outputs are sectional blade geometries and propulsor performance parameters, such as torque, shaft power,

coefficient of thrust, and efficiency. This procedure is detailed in Section 3.1.

6.1.3 DEP Arrangement Design

DEP configurations were created by varying the number of propulsors from 4 to 18 using the procedure described in Section 3.2. Each propulsor was designed to minimize disk loading for a given wingspan and was constrained by the requirement that all propulsor centers remain within the aircraft wingspan. All arrangements maintained a propulsor-propulsor tip spacing of 5% of the diameter to minimize propulsor-propulsor interactions [104] and a propulsor-tip-fuselage spacing of 0.84 m to maintain the original propulsor clearance of the ATR-72 [31]. Propulsor radius was therefore determined solely by propulsor count and available wingspan. Propulsor geometry and RPM were determined from the coefficient of thrust, defined by Eq. (6.1):

$$C_T = \frac{T}{\rho D^4 n^2} \quad (6.1)$$

in which T is thrust per propulsor, D is propulsor diameter, and n is the rotational speed in revolutions per second. For a known coefficient of thrust, Eq. (6.1) can be rearranged to solve for RPM:

$$RPM = 60 \sqrt{\frac{T}{\rho D^4 C_T}} \quad (6.2)$$

The coefficient of thrust links propulsor geometry with RPM. Lower rotational speeds require higher C_T values for the same thrust. This increases propulsor blade area since the radius and, consequently, the disk area are fixed. Prior work has found that the ratio of blade area to disk area, known as the solidity ratio, has the potential to affect noise and efficiency [109]. Other ways of changing solidity include changing blade count. However, within the scope of this study, geometry variations are limited to changes in the coefficient of thrust.

To investigate the influence of propulsor geometry on the noise and efficiency of the DEP arrangements, C_T was varied over six values from 0.17 to 0.40. With eight DEP propulsor-count variations and one conventional twin-propulsor design, this resulted in six sets of

nine propulsor arrangements, each designed with a constant coefficient of thrust. Table 6.2 summarizes the range of C_T , RPM, and propulsor radii for the 48 DEP arrangements and 6 conventional designs.

Table 6.2: Propulsor design ranges for conventional and DEP arrangements.

DEP Arrangements	
C_T	0.17, 0.20, 0.25, 0.30, 0.35, 0.40
RPM	500–3000
Radius (m)	0.75–6.0
Propulsor Count	4–18
Non-DEP Conventional Twin Arrangement	
C_T	0.17, 0.20, 0.25, 0.30, 0.35, 0.40
RPM	1000–1400
Radius (m)	1.97
Propulsor Count	2

6.1.3.1 DEP Sizing Procedure

The preceding design constraints established the basic parameters used in the following detailed propulsor design procedure. Each propulsor was first sized for the takeoff condition in which the RPM was determined as a function of the selected C_T and propulsor diameter. The resulting design was then evaluated at the cruise condition. All propulsors were designed to operate with constant speed and variable pitch to control thrust and power. At the cruise condition, the radial c_l distribution was evaluated to ensure that it remained positive over the entire blade span and tapered toward the tip. If this condition was not satisfied or if M_{Tip} exceeded 0.8, the initial design was modified. These modifications included re-twisting the blade or altering the chord distribution to achieve the desired behavior during both low- and high-speed conditions. In addition to shaft power and noise, the maximum attainable flight speed was also evaluated. This value corresponds to the flight speed at which the propulsor tip Mach number reaches or exceeds 0.8, beyond which propulsor noise increases substantially due to the onset of transonic tip effects [79]. As the number of propulsors increased, the radius of each propulsor decreased. Consequently, disk loading and the required rotational

speed increased with propulsor count.

Additional considerations, such as blade structural and elasticity limits, were intentionally excluded to evaluate the idealized performance and acoustic characteristics of each DEP configuration. However, Fig. 6.2 illustrates the twin-conventional, 6-count, and 8-count DEP arrangements as they appear on an ATR-72-sized fuselage and demonstrates the feasibility of implementing these designs. This preliminary layout indicates that even the larger diameter DEP arrangements can be integrated on an existing aircraft and satisfy the FAA 14 CFR 25.925 7-inch ground clearance requirement [41].

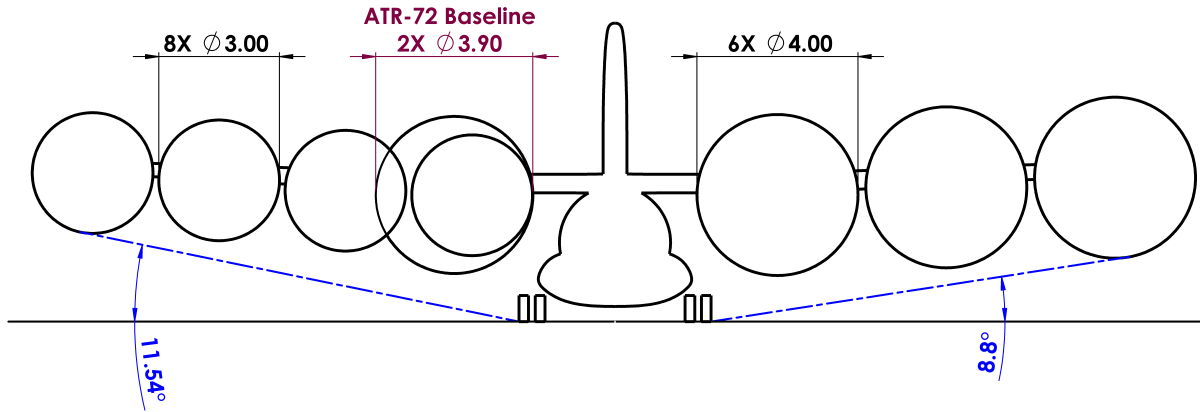


Figure 6.2: Conventional twin-propulsor and 6- and 8-propulsor DEP arrangements on an ATR-72-sized aircraft.

6.1.4 Motor and Nacelle Sizing and Iteration

DEP arrangements have the potential to affect aircraft noise and performance beyond variations in propulsor design. Propulsor count affects aircraft performance by altering weight and drag through the integration of additional nacelles and motors. Nacelle size affects parasitic drag through surface area and characteristic Reynolds number, which are functions of geometry. This change in volume also impacts total nacelle weight, which, along with motor weight, alters the induced drag and the aircraft weight component during climb.

To account for these effects, the changes in motor and nacelle weight and drag were in-

incorporated into the design condition at 59 m/s, and the required thrust was recalculated. Additional weight increased induced drag and required an increase in thrust when sizing the propulsors for the conditions in Table 6.2. All configurations were sized to maintain constant climb angle at a given airspeed [31]. This resulted in DEP propulsor arrangements with different total weights, drag characteristics, and required thrusts, but all capable of maintaining the same climb performance.

6.1.4.1 Nacelle Sizing

The nacelle surface area and weight were calculated as functions of propulsor power using the procedure described in Section 3.2.2.1. Figure 6.3 depicts the percentage change in nacelle weight compared to the twin-propulsor conventional design for each set of C_T values. Arrangement counts of 6 and above yield a 1–2% reduction in nacelle weight. This reduction continues until count 14, beyond which there is little additional decrease in weight. This suggests that even moderate DEP arrangement counts of 8–10 result in lighter nacelle structures.

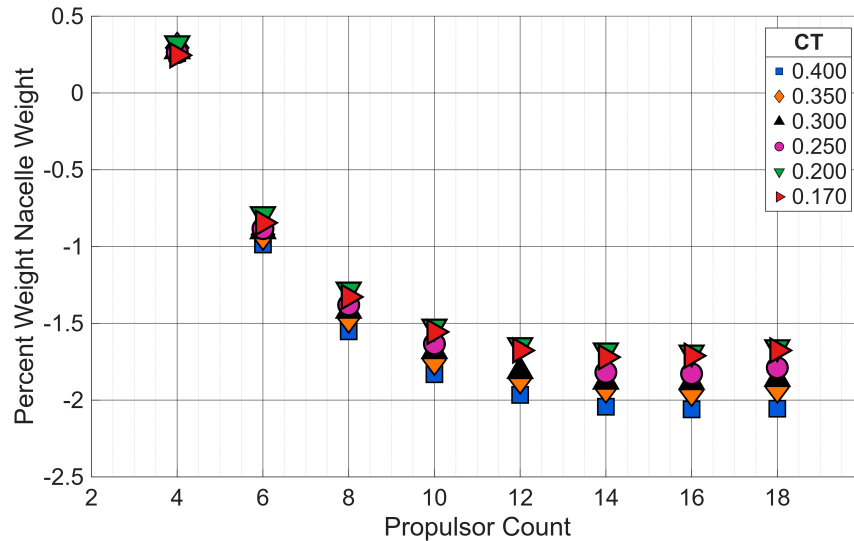


Figure 6.3: Percentage change in total nacelle weight relative to the conventional twin-propulsor design.

6.1.4.2 Motor Sizing

Motor weight was determined as a function of propulsor diameter and power using the procedure detailed in Section 3.2.2.2. Figure 6.4 shows the percentage change in motor weight for each of the C_T variations. Designs with lower propulsor counts are penalized by their low rotational speed, which results in a disproportionate increase in torque and motor size. A lower rate of increase is observed for propulsor counts of 12 and above, but all DEP designs result in at least a 1–2% increase in weight compared to the non-DEP arrangement. Similar to the nacelle sizing results, the motor sizing results indicate that moderate propulsor counts are sufficient to minimize the electric-motor weight penalty.

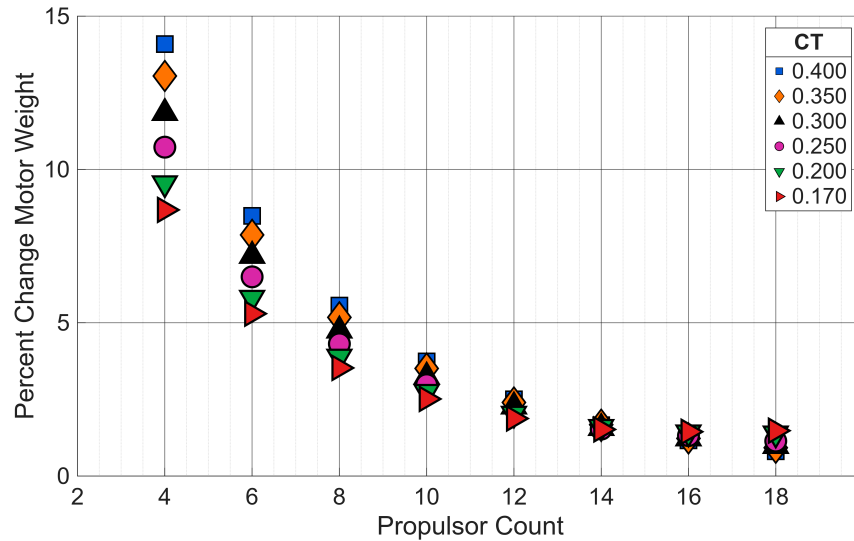


Figure 6.4: Percentage change in total motor weight relative to the conventional twin-propulsor design.

6.1.4.3 DEP Effects on Aerodynamics

Figure 6.5 shows the percentage change in drag due to the addition of nacelle and motor parameters to the DEP designs. At takeoff and cruise speeds, drag is reduced for propulsor counts above 6 and 8, respectively. This is most apparent at cruise speeds, where a design C_T of 0.40 yields a nearly 1.5% drag reduction at counts 14 and 16. The results of the motor and nacelle sizing suggest that higher propulsor counts benefit from a slight reduction in

required power, while lower propulsor counts between 4 and 6 are penalized by increased power requirements compared with a conventional twin-propulsor turbo-electric design.

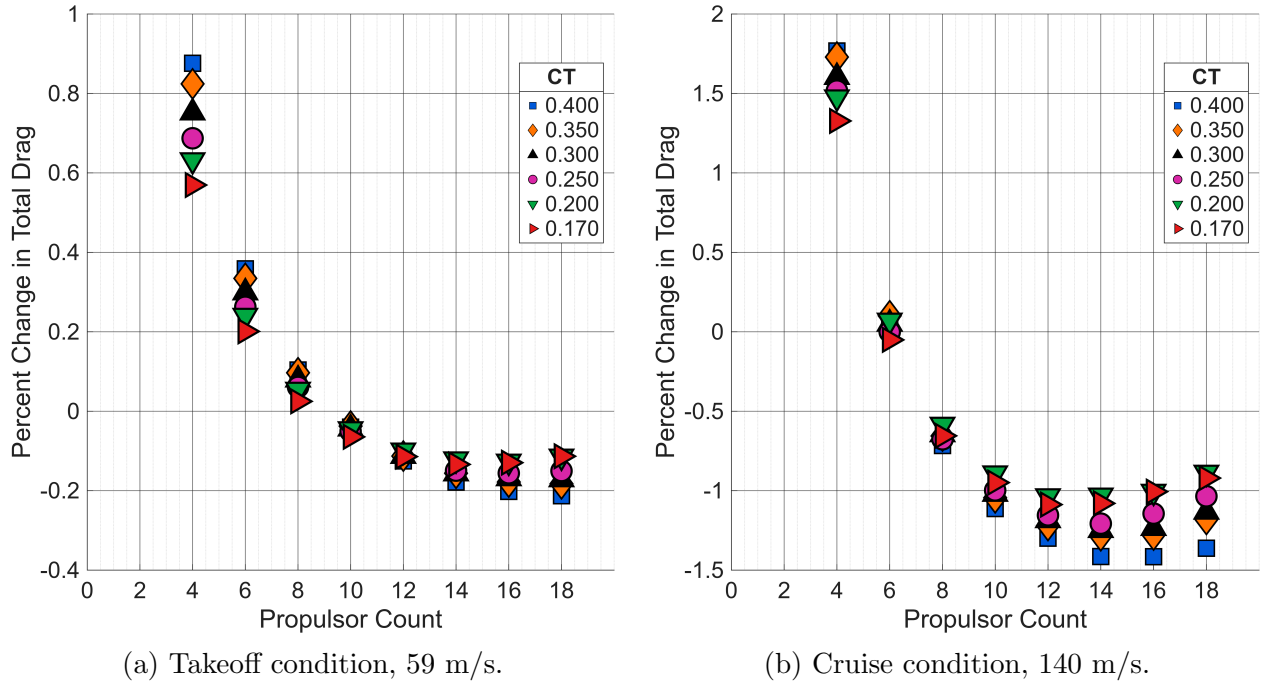


Figure 6.5: Percentage change in aircraft drag for DEP arrangements with varying propulsor count.

6.1.4.4 Summary of Final Converged DEP Arrangements

Table 6.3 lists final design parameters for propulsor counts between 4 and 18 designed at a C_T of 0.20. An additional metric describes the maximum flight speed attainable before the propulsor tip Mach number exceeds 0.80. The flight Mach number is calculated at an altitude of 10,668 m (35,000 ft) to allow comparison with existing high-speed passenger transports. In contrast to the conventional design, most DEP arrangements have lower disk loading, allow higher flight speeds before the propulsor tip Mach number exceeds 0.80, and require less power during cruise. Trends for additional coefficients of thrust are discussed in subsequent sections.

Table 6.3: DEP propulsor characteristics for $C_T = 0.20$ at the 140-m/s cruise condition.

Propulsor Count	Tip Radius (m)	Thrust per Propulsor (N)	Max RPM	Total Power (kW)	Disk Loading (N/m^2)	Flight Speed Mach Number for $M_{Tip} = 0.8$
Baseline: 2	1.97	6910	1200	2140	1970	0.34
4	6.00	3500	133	2210	424	0.78
6	3.00	2300	377	2150	633	0.74
8	2.00	1720	692	2130	845	0.69
10	1.50	1370	1066	2120	1054	0.64
12	1.20	1140	1490	2120	1266	0.58
14	1.00	976	1958	2130	1478	0.52
16	0.86	855	2468	2120	1686	0.46
18	0.75	761	3015	2140	1897	0.38

6.1.5 Source Noise Modeling

Propulsion noise comprises propulsor noise and engine core noise. Any required generators, gearboxes, and AC-DC converters are assumed to be configured such that the engine core can operate optimally based on required power, airspeed, and density altitude [46]. Other components, such as airframe and landing gear noise, are ignored since engine core and propulsor noise are the dominant sources during the takeoff and departure conditions considered.

6.1.5.1 Propulsor Noise

Propulsor tonal noise was modeled using the Ffowcs Williams–Hawkings equations implemented through the Farassat 1A formulation [39], while broadband noise was modeled using the semi-empirical Brooks, Pope, and Marcolini (BPM) method [18]. These were implemented using NASA’s ABEAT tool within the Aircraft NOise Prediction Program 2 (ANOPP2) [74, 17]. ABEAT uses BEMT methods to calculate propulsor loads based on the sectional blade geometry generated during propulsor design. Since propulsors are assumed to operate with little cross-flow, a purely axial uniform inflow model was used. This procedure is detailed in Section 3.3.

6.1.5.2 Turbine-Generator Fuel Consumption and Noise

All DEP designs assume a dual-turbine configuration, with the total required power split across two engines. Turbine-generator brake-specific fuel consumption (BSFC) was interpolated from existing turbine engines with shaft-power ratings similar to those required by the DEP arrangements [19]. The result was an estimate of fuel consumption for each DEP arrangement based on the required power at a specific operating condition. Gas-turbine core engine noise was modeled using the procedure described in [94]. Engine and operating specifications, such as mass flow rate and core-stage temperatures, were obtained from a model of the Pratt & Whitney PW127F turboprop engine developed by Choi et al. [24] for implementation on an ATR-72-500.

6.1.6 Flight Profile Generation

Flight profiles were based on a constant-speed departure profile using published nominal climb rates for an ATR-72 [31, 3] and the procedure described in Section 3.4. Profiles consist of an initial 10-degree post-rotation climb at 59 m/s. Upon reaching an altitude of 457 m (1,500 ft), the climb angle was reduced to 4.5 degrees at 87.5 m/s, which was maintained until the aircraft reached a nominal cruise altitude of 6,096 m (20,000 ft). Required thrust for these conditions was calculated using an ATR-72 drag buildup developed by Nita and Scholz [82] and modified for each arrangement's nacelle and motor weights.

6.1.7 Noise Propagation

Noise propagation is based on straight ray modeling, in which source noise is transmitted along a direct and reflected path to an observer [92]. Noise attenuation and diffusion are based on atmospheric models but do not consider additional factors such as wind and temperature variations, which are beyond the scope of this chapter. Noise propagation pairs the previously generated flight profile, which specifies the aircraft's position and state of operation, with corresponding source noise hemispheres. The source noise is subsequently

propagated to a grid of ground-based observers to determine community noise. This procedure is detailed in Section 3.5.

6.2 Results

The following section analyzes propulsor power consumption, fuel burn, source and community noise during departure profiles, and high-speed performance of the DEP arrangements and conventional propulsor designs for the baseline turbo-electric aircraft. These results demonstrate the impacts of propulsor geometry and operation and how DEP affects the influence of these design parameters on overall aircraft performance.

6.2.1 Propulsor Performance

The results of Section 6.1.4 indicate that DEP arrangements affect performance through changes in drag and weight resulting from motor and nacelle design, changes in propulsor disk loading resulting from arrangement count, and changes in propulsor efficiency resulting from propulsor geometry and operation. To better understand the effects of arrangement count and propulsor design, a set of constant thrust designs is presented in addition to the fully iterated designs with motor and nacelle sizing. The constant-thrust designs assume fixed motor and nacelle weight and drag-fraction values that do not change with propulsor count. Any changes in performance are purely due to changes in disk loading, solidity, and tip speed resulting from variations in C_T and propulsor count.

6.2.1.1 Influence of Propulsor Count for Fixed Motor and Nacelle Weight

Figure 6.6 depicts total propulsor power for DEP arrangements designed with fixed motor and nacelle weight and drag, and with constant design thrust. Marker shapes and color represent designs with different C_T values. The conventional twin-propulsor designs are outlined in red. The low airspeed of 59 m/s represents early climb operation shortly after rotation, while the higher 140 m/s condition represents cruise flight.

Across both conditions and all propulsor counts, lower values of C_T yielded designs with lower required power for each arrangement. At the early departure condition, a decrease in C_T from 0.40 to 0.17 results in a roughly 200 kW reduction in total power across all conventional and DEP arrangements. These effects diminish past C_T values of 0.20. For the high-speed condition, C_T changes become less prominent at higher propulsor counts, with the same coefficient of thrust change resulting in a 90 kW reduction at 16 propulsors compared to the 200 kW power reduction observed for 4 propulsors. The conventional twin-propulsor designs exhibit similar C_T trends at low speeds but much smaller changes at high speeds than the DEP cases.

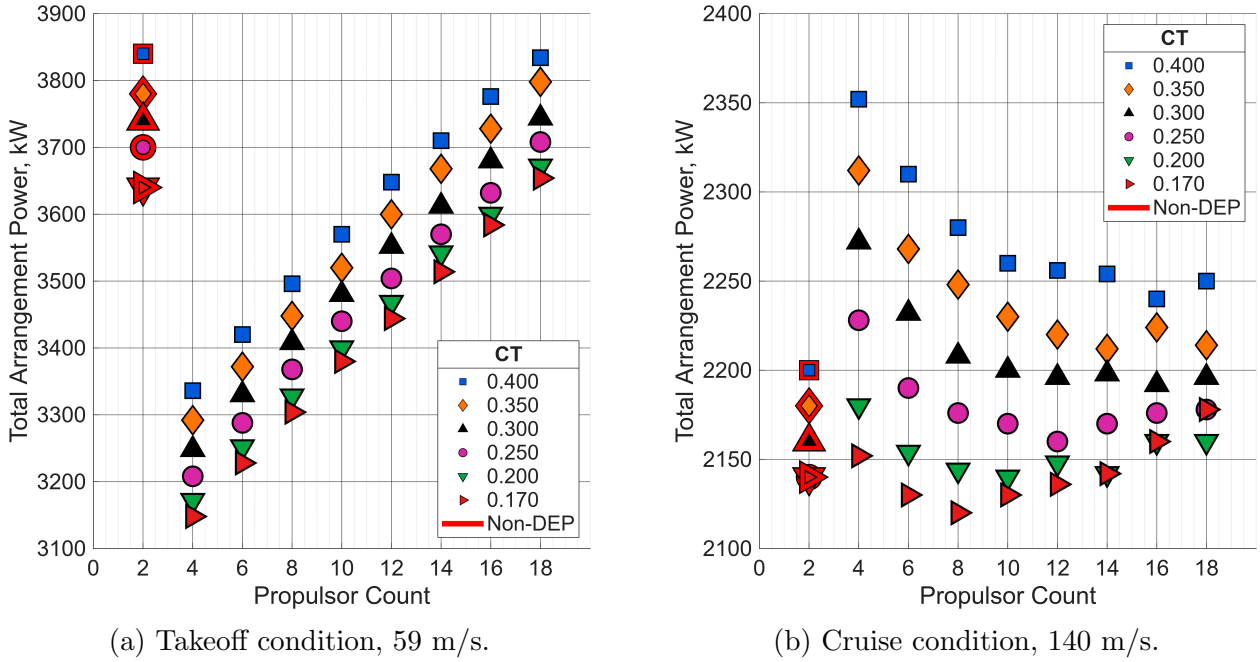


Figure 6.6: Total propulsor power as a function of propulsor count for fixed motor and nacelle weight and drag.

Additional trends are observed when viewing propulsor efficiency. Figure 6.7 presents the same comparison of total power and propulsor count shown in Fig. 6.6 but adds a color bar representing propulsor efficiency, η . Since there are no variations in weight and drag and all arrangements have the same design thrust, changes in power come exclusively from propulsor design and operation. This is reinforced by observing that propulsive efficiency

correlates directly with power, as designs of the same efficiency have nearly the same total power across all propulsor counts and operating speeds.

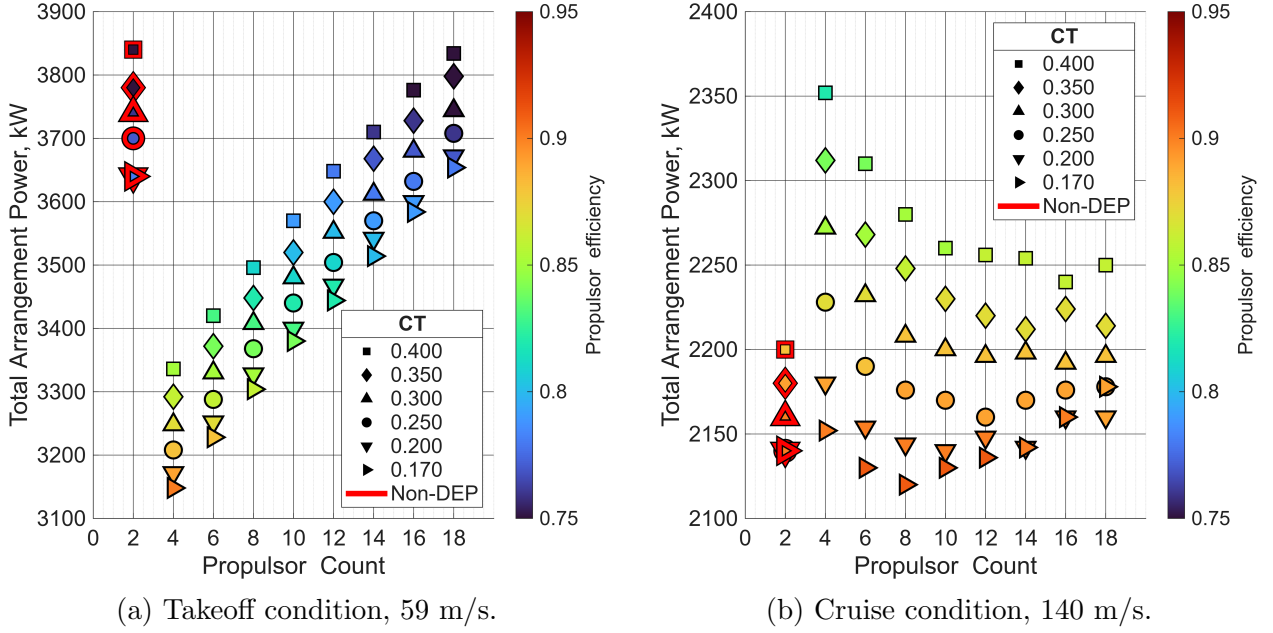


Figure 6.7: Propulsor efficiency of designs with constant motor and nacelle weight and drag.

At low speeds, propulsive efficiency decreases and total power increases with arrangement count. However, at high speeds, power and efficiency vary less with propulsor count and instead vary more with C_T value. At low speeds, nearly 75% of the DEP arrangements required less total power than the conventional twin designs. At high speeds, fewer than six of the DEP designs required less power than the conventional twin designs.

These trends can be partially explained by expressing propulsive efficiency as a function of disk loading using actuator disk theory [97]:

$$\eta = \frac{2}{1 + \sqrt{\frac{2T}{\rho\pi R^2 V_\infty^2} + 1}} \quad (6.3)$$

or

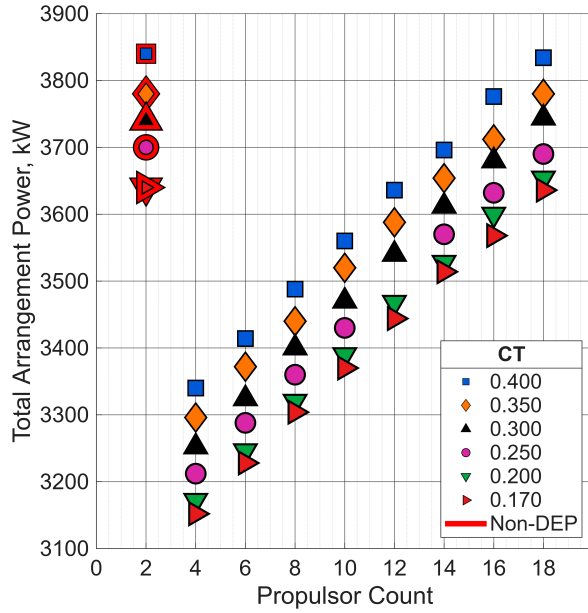
$$\eta = \frac{2}{1 + \sqrt{\frac{2DL}{\rho V_\infty^2} + 1}} \quad (6.4)$$

in which DL represents individual propulsor disk loading. At lower speeds, V_∞ is relatively small, and variations in disk loading cause significant changes in propulsor efficiency. In these designs, disk loading is a function of propulsor count because total thrust is constant. This potentially explains why, at low speeds, propulsor efficiency is strongly associated with propulsor count and why designs with disk loadings greater than those of the conventional designs have higher total power consumption. At higher speeds, disk loading becomes a relatively smaller term, and other factors, such as changes in solidity ratio due to design C_T , have a greater impact on efficiency.

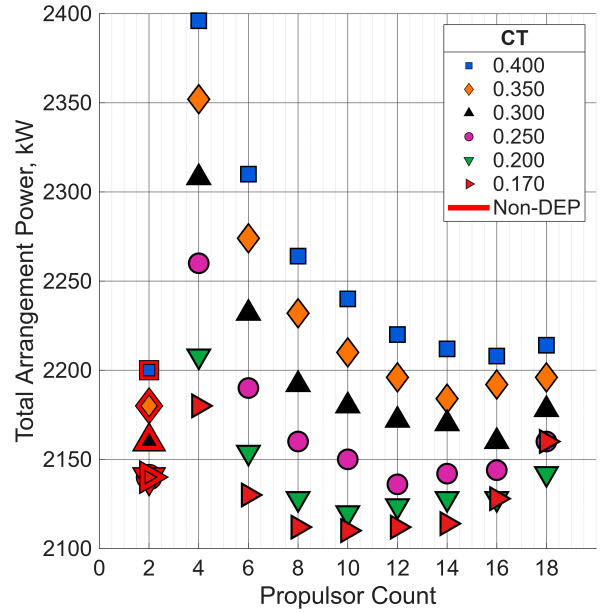
6.2.1.2 Influence of Propulsor Count with Iterated Nacelle and Motor Weight

The integration of variable nacelle and motor weight introduces additional complications by changing the required thrust for each arrangement. Figure 6.8 depicts total arrangement power for DEP designs with iterated motor and nacelle weights. The required thrust now varies with propulsor count. Compared with the constant-thrust results in Fig. 6.6, power increases for propulsor counts of 6 and below and decreases for higher counts. This correlates with the inflection points observed in nacelle drag and motor weight, as depicted in Figs. 6.3, 6.4, and 6.5. This effect is especially strong during cruise, where arrangement counts of 14–18 yield a 1.5% decrease in total drag and a 50 kW reduction in power.

The addition of motor and nacelle weight penalizes high-torque, low-RPM designs but provides a slight advantage to designs with higher propulsor counts. At cruise, 14 DEP designs now have lower power consumption than the conventional baseline. While the propulsor efficiency analysis favored a moderate DEP implementation of 8 propulsors, the iterated results instead identify propulsor counts of 10 or 12 as the lowest-power designs.



(a) Takeoff condition, 59 m/s.



(b) Cruise condition, 140 m/s.

Figure 6.8: Total propulsor power as a function of propulsor count with iterated motor and nacelle weight and drag.

For additional context, a preliminary fuel burn estimate is calculated from the total arrangement power during cruise conditions. Figure 6.9 compares the fuel burn of the turbo-electric designs with that of an unmodified ATR-72 turboprop. Several turbo-electric designs have comparable or lower fuel burn than the conventional turboprop aircraft. Furthermore, these results demonstrate the vast variation in performance achievable with DEP arrangements.

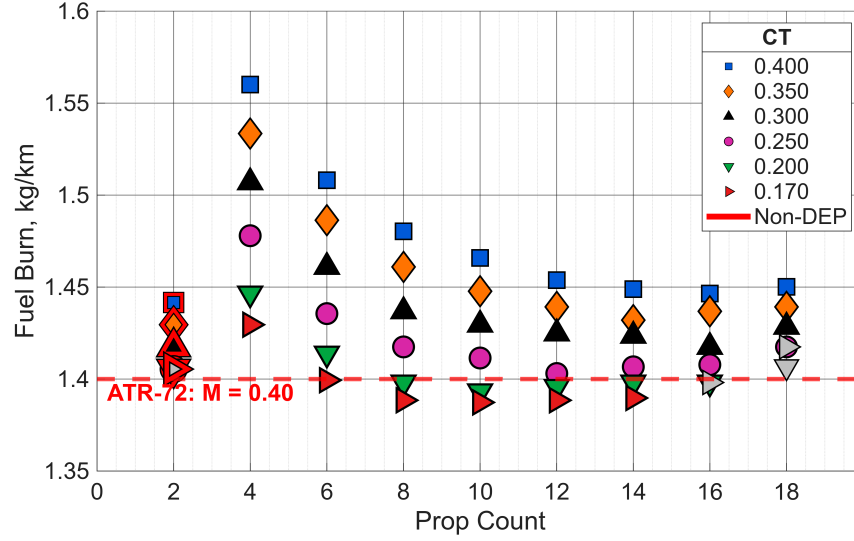


Figure 6.9: Cruise fuel burn of DEP and conventional turbo-electric designs compared with the ATR-72 at 140 m/s.

6.2.2 Propulsor Departure Noise

Propulsor designs with low coefficients of thrust and high tip speeds have the potential to reduce power. However, higher tip speeds have a negative impact on noise emissions. The following section discusses the impacts of propulsor count on aircraft noise emissions during departure. Source noise was modeled for both the propulsors and the engine core during the departure segment. The analysis of both component and total noise better reveals their relative magnitudes and conditions when propulsor noise dominates over engine core noise. The A-weighted overall sound pressure level (OASPL) at a point 72 m below each arrangement was selected to represent undertrack noise during a flyover. To better account for the effects of directivity, the combined propulsor and engine core source noise was used to create ground contours of maximum A-weighted sound pressure level (L_{AMax}) during the full departure segment.

6.2.2.1 Impacts of Propulsor Count on Propulsor Noise

Figure 6.10a depicts propulsor-only A-weighted OASPL as a function of total power and propulsor count. These results are for designs with iterated motor and nacelle weights.

Noise increased with increasing propulsor count and decreased with lower C_T values across all arrangements. This effect was more apparent at higher propulsor counts. At 6 propulsors and for C_T values from 0.40 to 0.17, noise ranged from 68.6–75.5 dBA, while at 18 propulsors, noise ranged from 77.5–88.5 dBA. DEP arrangement noise was as low as 65.5 dBA, which was lower than the minimum conventional arrangement noise of 83.1 dBA. All propulsor counts below 14 included at least one design C_T that produced less noise than the conventional twin-propulsor design. Figure 6.10b illustrates the tip-speed Mach number as a function of arrangement count and propulsor-only noise. Propulsor noise is very closely correlated with tip speed and forms a nearly linear gradient along constant values of M_{Tip} .

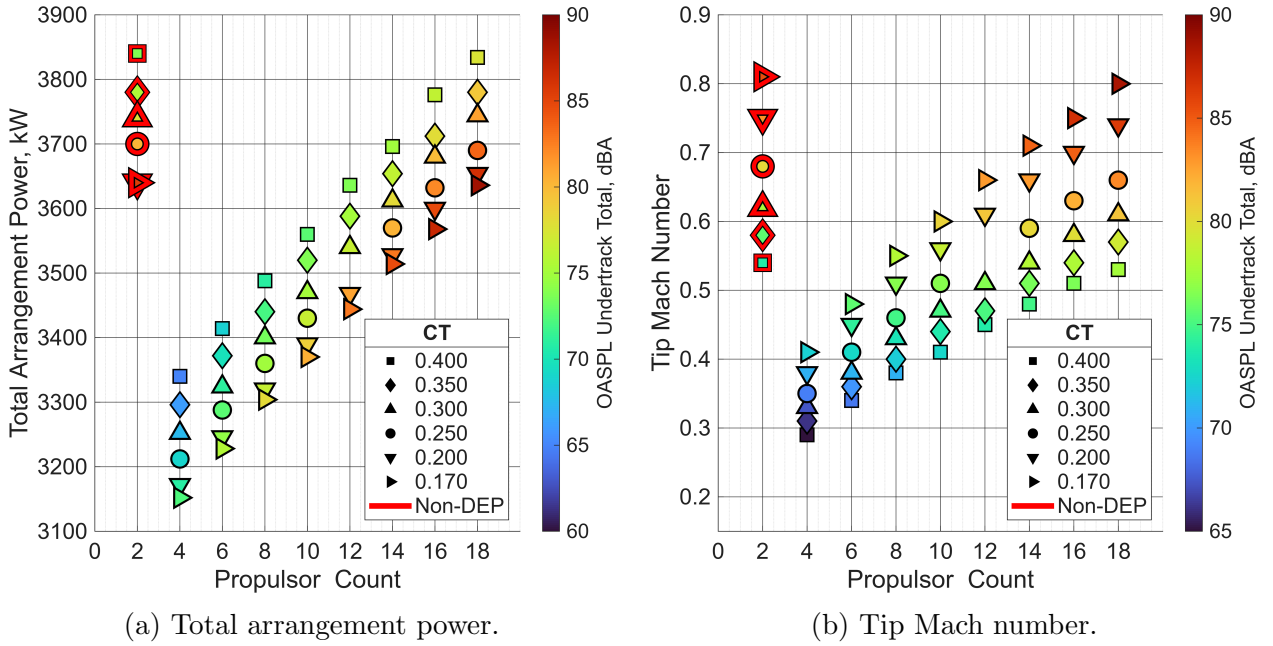


Figure 6.10: Propulsor-only A-weighted OASPL as a function of propulsor count, total power, and tip Mach number at 59 m/s.

For the same design C_T , as propulsor count increases, disk area decreases and tip speeds must increase to maintain thrust. At a thrust coefficient of 0.20, increasing propulsor count from 4 to 18 increases M_{Tip} by 0.36 and noise by 15.4 dBA. At a constant propulsor count, decreasing C_T has a similar effect. A reduction in C_T from 0.40 to 0.17 at a propulsor count of 4 increases M_{Tip} by 0.12 and noise by 7.2 dBA. These effects are compounded at high

propulsor counts, where changes in C_T result in greater changes in tip speed and noise. The same decrease in C_T at 18 propulsors, however, increases M_{Tip} by 0.27 and noise by 10.9 dBA.

6.2.2.2 Impacts of Propulsor Count on Propulsor and Engine Core Noise

Engine core noise was modeled at the same departure conditions based on the required propulsor power. Figure 6.11b depicts total core and propulsor A-weighted OASPL for power and propulsor count variations at the same 72-m undertrack location used in Fig. 6.10a. The modeled turbo-generators produce 75–80 dBA of noise and reduce much of the noise benefit achieved by low-RPM DEP arrangements. For all C_T values at propulsor counts of 4–8, the engine core is the dominant source of noise since propulsor-only noise is very quiet and well below 75 dBA. This is especially noticeable in Fig. 6.11b, which shows the impacts of M_{Tip} on noise emission. With the addition of core noise, M_{Tip} must exceed approximately 0.6 before tip speed shows an influence similar to that observed for propulsor-only noise in Fig. 6.10b. However, even with the addition of engine core noise, over half of the DEP arrangements still produce less noise than their twin-propulsor counterparts, which produce 85–95 dBA of total noise. Moreover, these results indicate that the low-power propulsor DEP designs with counts of 8–12, observed in Fig. 6.8b, are also designs that produce less noise than their conventional counterparts. These results suggest that both propulsor count and C_T are effective design variables for controlling propulsor noise and efficiency and that solutions exist that potentially benefit both metrics.

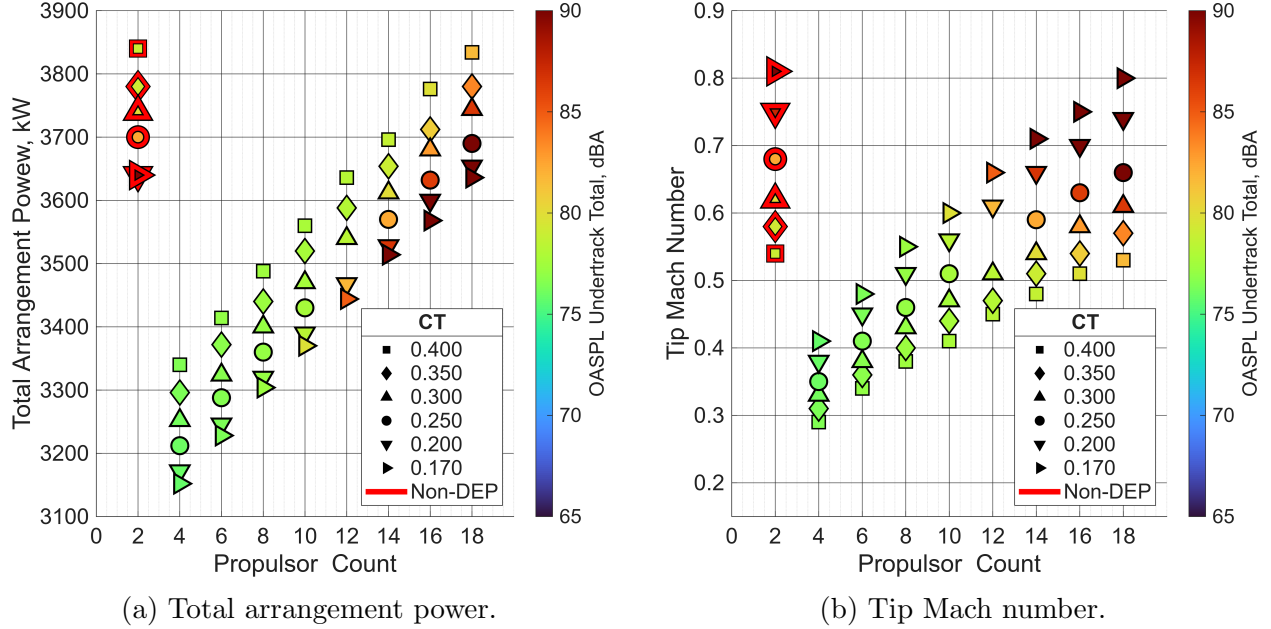


Figure 6.11: Combined propulsor and engine-core A-weighted OASPL as a function of propulsor count, total power, and tip Mach number at 59 m/s.

Undertrack noise provides a convenient reference for the relative magnitudes of each DEP arrangement. However, this comparison does not account for source noise directivity and provides a limited description of noise during operation. Figure 6.12 depicts community noise contours at an L_{AMax} level of 65 dB during a departure profile described in Section 6.1.6. Selected DEP arrangements are presented along with conventional twin-propulsor configurations for designs with a C_T value of 0.20. There is less than one-half nautical mile of variation in contour length between counts of 4 and 10. This corresponds to the undertrack observations in which core noise is dominant and propulsor count has less effect on total noise emission. Contour length does not increase substantially until propulsor counts of 14–16, where it grows from 3.5 to 5 nm. At 18 propulsors, the DEP arrangements finally surpass the length of the conventional twin-propulsor design. The departure contours reinforce how much quieter many of the DEP arrangements are compared to the conventional design and emphasize how much variation in propulsor arrangement is possible while still maintaining noise benefits.

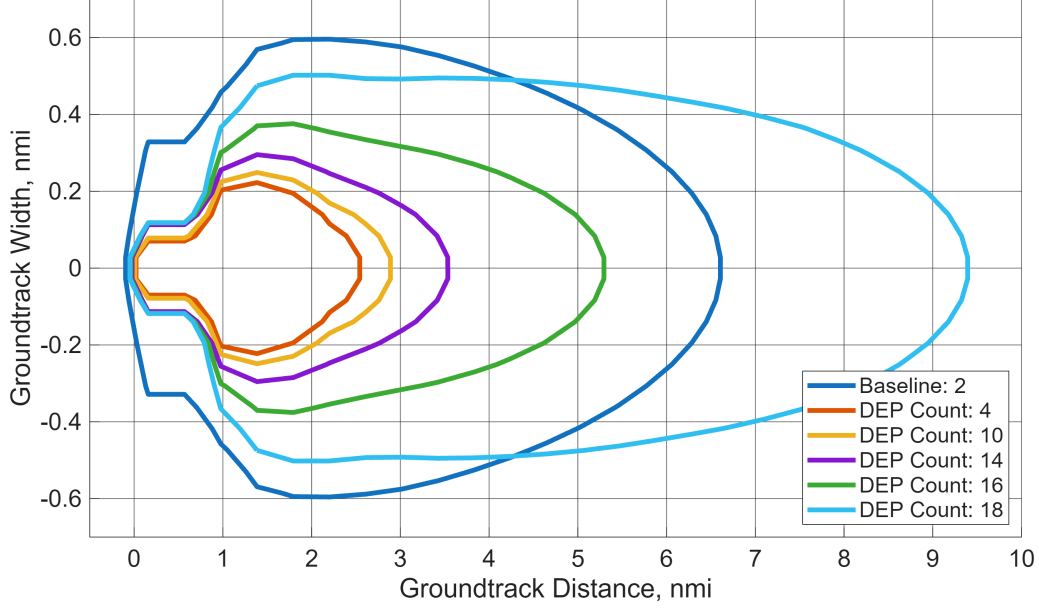


Figure 6.12: Departure L_{AMax} community-noise contours at 65 dB for conventional and DEP arrangements with $C_T = 0.20$.

6.2.3 High-Speed Propulsor Performance

The previous analyses of propulsor efficiency and noise emissions have demonstrated that DEP designs possess potential advantages over existing turboprop aircraft. DEP allows propulsor designs to operate at much lower tip speeds than conventional propulsor designs. This not only reduces noise but also allows higher flight speeds before compressibility effects reduce propulsor efficiency. Table 6.3 lists the aircraft cruise Mach number at which the propulsor tips have an M_{Tip} of 0.80 for designs with a C_T value of 0.20. Many DEP arrangements can operate at speeds higher than those of conventional turboprops, with some cruise speeds within the range of regional turbofan passenger transports. To this end, a high-speed fuel burn analysis is conducted to compare the performance of DEP designs with high-speed turbofan aircraft.

Figure 6.13 presents the weight of fuel consumed per kilometer of the DEP arrangements when operated at Mach 0.75 at an altitude of 10,668 m (35,000 ft). The fuel consumption of two turbofan aircraft, one high-speed conventional turboprop, and one low-speed conven-

tional turboprop is presented for context [19, 5, 6, 3]. Apart from the slower ATR-72, each aircraft has a cruise speed, weight, passenger count, and range similar to those of the DEP designs. Nearly all of the DEP arrangements had lower fuel burn than the most efficient turbofan passenger transport presented. Many DEP designs also had a lower rate of fuel consumption than the relatively fast De Havilland Dash-8, despite the latter operating at Mach 0.60 instead of Mach 0.75. Although all the DEP arrangements had higher fuel burn than the ATR-72 at Mach 0.40, this analysis indicated that high-speed propeller-based DEP operation is possible while retaining the efficiency of turboprop aircraft.

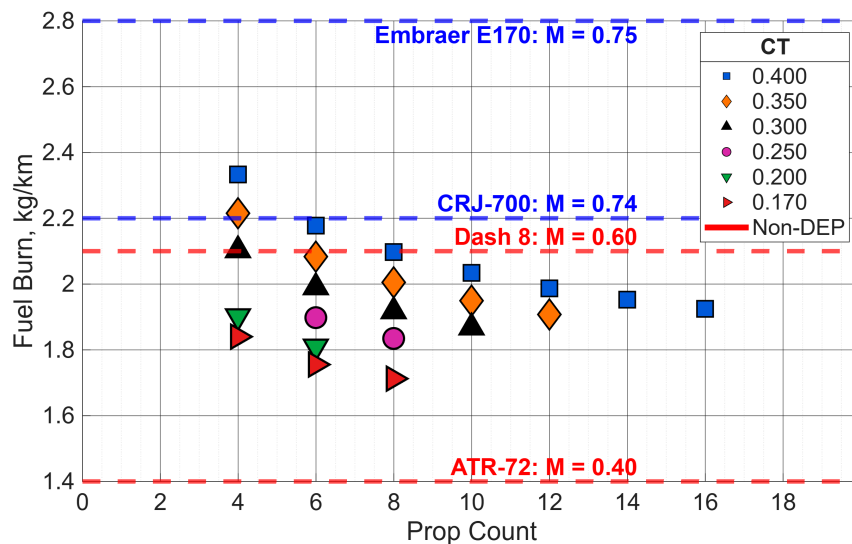


Figure 6.13: Fuel burn of DEP arrangements at Mach 0.75 compared with existing regional turboprop and turbofan aircraft.

6.3 Conclusions

This chapter investigated the impacts of distributed electric propulsion on the performance, noise emissions, and operating capabilities of regional turboprop aircraft. A range of DEP propulsor configurations were developed with variations in propulsor count, blade geometry, and operating conditions. Component-level design also accounted for the impacts on weight and performance of aircraft subsystems as a result of DEP implementation. The results of this study demonstrate that DEP provides significant flexibility in the design and down-

selection of propulsor configurations but also highlight that the resulting benefits in efficiency and noise emissions strongly depend on system-level integration within the aircraft.

Section 6.2.1 indicated that total power was primarily influenced by disk loading at low speeds and blade geometry at high speeds. At low airspeeds, disk loading is directly affected by propulsor count, and designs with larger disk areas required the least total power. At higher airspeeds, the coefficient of thrust had a greater effect, and designs with higher rotational speeds and lower solidity ratios provided the highest efficiency. The addition of motor and nacelle weight increased power requirements for low propulsor count arrangements and reduced power requirements at higher counts. The lowest-power cruise designs have low coefficients of thrust and moderate arrangement counts of 8–14 propulsors. Although not the most efficient configurations during takeoff, these arrangements have lower fuel consumption than conventional non-DEP designs.

The results of Section 6.2.2 reveal that propulsor noise was primarily affected by tip speed, with lower values of C_T and higher propulsor counts increasing noise. Engine core noise, however, was louder than propulsor noise from designs with tip speeds below Mach 0.50. As a result, core noise is dominant for arrangements with propulsor counts from 4 to 12. However, the conventional non-DEP arrangements operate at tip speeds between 0.54 and 0.81 and produce up to 15 dBA more total noise than the DEP arrangements in this range. Additionally, the low-noise DEP design space overlaps with arrangements that minimize fuel consumption. Although low-power propulsor designs favor higher tip speeds to achieve lower solidity ratios, the system-level impacts of integrating additional aircraft subsystems result in arrangements that are efficient while maintaining lower noise emissions than most conventional designs.

Finally, the high-speed analysis in Section 6.2.3 demonstrates how the lower-tip-speed designs achievable with DEP enable turboprop aircraft to operate at higher speeds while potentially achieving lower fuel consumption rates than existing turbofan aircraft of similar size and role.

When considered with the propulsor efficiency and noise emissions analyses, these results demonstrate that DEP significantly expands the design space for regional turboprop aircraft and allows capabilities not available to conventional designs. Propulsor count, geometry, operating conditions, and resulting DEP impacts on motor, nacelle, and engine integration can be simultaneously traded to balance efficiency, noise, and operating speed in ways that are not possible with conventional propulsion architectures.

Chapter 7

Conclusions

7.1 Contributions

This thesis developed a methodology for propulsor design and evaluation, distributed-propulsion architecture design, tonal and broadband source-noise modeling, flight-profile generation, and community-noise assessment for DEP aircraft. The methodology was applied across component-, vehicle-, and system-level studies to evaluate how propulsor design, aircraft operation, and multidisciplinary integration affect propulsor performance, aircraft performance, source noise, and community noise. The main contributions of this thesis are:

1. Development of a methodology capable of designing and evaluating propulsors and distributed-propulsion architectures, modeling tonal and broadband source noise, generating representative flight profiles, and propagating source noise to evaluate resulting community noise.
2. Parametric assessment of how propulsor geometry, operation, arrangement, and flight path affect propulsor performance, aircraft performance, source noise, and community noise for representative DEP STOL and VTOL aircraft architectures.
3. Demonstration of the coupled relationship between propulsor operation, aircraft performance, flight-profile design, source noise, and community noise, showing that changes in propulsor operation can alter both the generated source noise and the flight trajectory through which that noise is propagated.
4. Demonstration of how the methodology can be integrated with additional aircraft sub-

systems and performance models to evaluate how propulsor arrangement, motor and nacelle sizing, aircraft performance constraints, and operating requirements affect efficiency, fuel consumption, noise emissions, and operating capability for a representative regional turboprop aircraft.

7.2 Summary of Key Results

7.2.1 Evaluation of Propulsor Geometry and Operation on Noise and Performance

Propulsor geometry and operating conditions were found to strongly influence both source noise and required power, demonstrating opportunities to trade noise and propulsive performance during preliminary design. The computational efficiency of the propulsor sizing and source-noise methodology enabled a large design matrix to be generated for common thrust and airspeed requirements while varying propulsor geometry and operating conditions. Tip speed, blade count, and propulsor radius were parametrically varied to identify the sensitivity of source noise and required power to these design variables. Because each propulsor was designed for its respective operating condition, the observed trends reflect changes in propulsor geometry and operation rather than differences caused by evaluating a limited number of fixed propulsors at off-design conditions. The lowest-noise propulsor designs favored low tip speeds, large propulsor radii, and high blade counts. These characteristics generally resulted in higher solidity ratios, increased torque, and greater required power for the same thrust, illustrating a direct tradeoff between propulsor noise and power requirements. These results address the component-level portion of the second thesis objective by demonstrating how parametric changes in propulsor geometry and operation affect source noise and propulsor performance before those effects are incorporated into a complete aircraft architecture.

7.2.2 Effects of DEP Arrangements on Performance and Noise

Propulsor arrangement was found to directly affect both the aerodynamic performance and propulsor operating requirements of lift-augmented DEP vehicles. A blown-flap aerodynamic model based on deflected jet-sheet theory was coupled with the DEP arrangement design methodology to account for the effects of propulsor count and disk area on lift augmentation. This coupling accounted for how changes in propulsor count and radius alter lift augmentation and therefore modify the thrust and power required to maintain prescribed takeoff performance. The resulting analysis demonstrated that propulsor count can substantially change the total thrust required to achieve the same representative takeoff performance because the effectiveness of blown lift varies with the DEP arrangement. The DEP arrangement study further demonstrated that the effects of propulsor count cannot be interpreted independently of the associated changes in propulsor geometry and operation. Increasing propulsor count altered disk loading, solidity ratio, rotational speed, torque, power, and noise, and the preferred arrangement depended on the selected propulsor design and operating strategy. These results extended the single-propulsor trends to a complete DEP architecture and demonstrated that propulsor noise evaluation must be coupled with vehicle performance and arrangement design rather than treated as an isolated component-level analysis.

7.2.3 Effects of Aircraft Operation and Flight Profile on Community Noise

The methodology was applied to representative blown-flap STOL and lift-plus-cruise VTOL aircraft to couple propulsor design, flight-profile generation, source-noise modeling, and community-noise propagation. Vehicle performance models and propulsor operating limits were used to generate representative departure profiles spanning feasible operating states for each aircraft architecture. Community noise was found to depend on both the source noise produced at each operating condition and the altitude, ground-track position, and duration of those operating states along the flight profile. Source-noise magnitude varied

strongly with flight segment because thrust, RPM, blade pitch, and inflow changed throughout each departure profile. For the LPC aircraft, transition was a particularly important segment for community noise because the vehicle operated at high thrust while still relatively close to the ground. For the STOL aircraft, source noise and community noise were strongly influenced by propulsor operating strategy during the high-thrust ground-roll, initial-climb, and flap-retraction segments. The resulting community noise depended on the competing effects of source-noise magnitude and flight performance: higher thrust could increase source noise while also increasing climb rate and altitude or reducing the duration of a high-noise segment. For the LPC aircraft, variations in climb angle, transition angle, and hover altitude demonstrated that additional attenuation from increased altitude can offset higher source noise, while reducing the duration of a high-thrust transition segment can also reduce the resulting community-noise footprint. For constant-pitch STOL operation, reducing RPM lowered propulsor power and source noise sufficiently to reduce community-noise contour area despite the resulting shallower and longer departure profiles. In contrast, when power was held constant by increasing blade pitch as RPM decreased, lower RPM did not necessarily reduce source noise because increased blade loading moved the propulsor farther from its nominal design condition. Together, these studies demonstrated that reductions in source noise do not necessarily produce corresponding reductions in community noise when changes in propulsor operation also alter aircraft performance and flight trajectory. These results directly address the third thesis objective by demonstrating the coupled influence of propulsor source-noise prediction, vehicle performance, flight-profile generation, and noise propagation on community noise.

7.2.4 Multidisciplinary Integration and Regional Turboprop DEP Design

The methodology was extended to a representative regional turboprop aircraft to evaluate DEP arrangements within a broader multidisciplinary aircraft design problem. DEP ar-

rangements were designed by varying propulsor count, geometry, coefficient of thrust, and operating conditions for the representative medium-range regional turboprop aircraft. Motor and nacelle sizing were coupled with propulsor design so that changes in propulsor diameter, RPM, and power altered subsystem weight and drag, which modified the aircraft thrust requirement and required the DEP arrangement to be iterated until convergence. Including motor and nacelle weight and drag changed the preferred DEP design space: low-count, high-torque arrangements were penalized, while higher-count arrangements could benefit from reductions in drag and subsystem weight relative to lower-count DEP configurations. These subsystem effects shifted the lowest-power cruise designs toward moderate propulsor counts and demonstrated that propulsor efficiency alone was insufficient to identify the preferred aircraft-level DEP configuration. At low airspeeds, total power was primarily influenced by disk loading, whereas at higher airspeeds, the coefficient of thrust and propulsor geometry became more influential. The lowest-power cruise designs generally combined low coefficients of thrust with moderate propulsor counts, and several DEP arrangements achieved lower modeled fuel consumption than the conventional non-DEP baseline. Noise analysis showed that propulsor noise remained strongly dependent on tip speed, while engine core noise dominated many low-tip-speed DEP configurations. Even after engine core noise was included, the low-noise design space overlapped with arrangements having low fuel consumption, demonstrating that aircraft noise and fuel burn were not necessarily competing design objectives. The high-speed analysis further demonstrated that the lower-tip-speed propulsor designs enabled by DEP could extend the operating-speed capability of a turboprop architecture, with many evaluated arrangements capable of operating at Mach 0.75 while achieving lower modeled fuel consumption than the regional turbofan aircraft used for comparison. Together, these results satisfy the fourth thesis objective by demonstrating that the propulsor and noise methodology can be coupled with additional aircraft subsystem and performance models to evaluate propulsor arrangement, efficiency, fuel burn, noise emissions, and operating capability as interacting aircraft-level design considerations.

The results across Chapters 4–6 collectively demonstrate that propulsor noise cannot be evaluated independently of propulsor design, aircraft performance, or vehicle operation. At the component level, geometric and operational variables establish tradeoffs among source noise, required power, and propulsor efficiency. At the vehicle level, propulsor arrangement alters both source noise and fundamental aircraft performance requirements, while changes in propulsor operation modify both source noise and the flight trajectories through which that noise is propagated to the community. At the aircraft-system level, motor and nacelle integration, drag, weight, engine noise, and operating requirements can shift the preferred DEP propulsor arrangement and design space. The methodology developed in this thesis provides a framework for evaluating these coupled effects from top-level aircraft requirements, addressing the gaps identified in Chapter 2 in the integration of propulsor design, aircraft performance, dynamic flight operation, source-noise modeling, and community-noise assessment. Taken together, the methodology and applications presented in this thesis demonstrate a progression from component-level propulsor design to vehicle-level community-noise assessment and multidisciplinary aircraft design, providing a means to identify noise and performance tradeoffs early enough in the design process for both propulsor configuration and aircraft operation to remain flexible.

Bibliography

- [1] Uber air: Designing for the community. <https://canadianaam.com/wp-content/uploads/2021/05/Uber-Air-Designing-for-the-Community.pdf>. Accessed: 2025-05-29.
- [2] Uberair vehicle requirements and missions. <https://s3.amazonaws.com/uber-static/elevate/Summary+Mission+and+Requirements.pdf>.
- [3] Atr 42 & 72 specifications. *Aircraft Commerce*, (49), Dec. 2006. Aircraft Owner's & Operator's Guide. Accessed: 2026-06-25.
- [4] Acoustic Glossary. Sound exposure terms and definitions, 2022.
- [5] Aircraft Commerce. Owner's & operator's guide: Crj family. *Aircraft Commerce*, October–November 2009. Complete aircraft operating economics guide.
- [6] Aircraft Commerce. Owner's & operator's guide: E-jets family. Technical Report Issue 64, Aircraft Commerce, June–July 2009. Complete Guide, Embraer E-Jets Family.
- [7] Airfinance Journal. Air investor 2021: An airfinance journal special supplement. Special supplement, Euromoney Institutional Investor PLC, 2021. Air Investor special report on aviation finance market outlook.
- [8] E. Alvarado. The vision of advanced air mobility: evtols, drone delivery, vertiports and utm. [https://droneii.com/advanced-air-mobility-aam-evtol-vertiports-utm#:~:text=Index&text=The%20Advanced%20Air%20Mobility%20\(AAM,for%20most%20of%20the%20world.,](https://droneii.com/advanced-air-mobility-aam-evtol-vertiports-utm#:~:text=Index&text=The%20Advanced%20Air%20Mobility%20(AAM,for%20most%20of%20the%20world.,) 2023. Accessed: 2025-05-29.
- [9] R. K. Amiet. Noise due to turbulent flow past a trailing edge. *Journal of Sound and Vibration*, 47(3):387–393, August 1976.
- [10] ATR. *ATR 72-500: The Ultra Efficient Standard*. Aviation Broker, 2005. Accessed: 2026-06-25.
- [11] A. Bacchini and E. Cestino. Electric vtol configurations comparison. *Aerospace*, 6, Feb 2019.
- [12] K. Baskaran, N. S. Jamaluddin, A. Çelik, D. Rezgui, and M. Azarpeyvand. Effects of number of blades on propeller noise. *Journal of Sound and Vibration*, 572, Mar. 2024.
- [13] V. Batteiger, R. DeBreuker, I. Dedoussi, J. Delfs, F. Dinkelacker, A. Elham, S. J. Garcia, J. Friedrichs, S. Goertz, V. Grewe, W. Haije, C. Herrmann, F. Lachaud, A. Mertens, J. Morlier, U. Krewer, R. Radespiel, A. G. Rao, P. Schmollgruber,

- U. Schröder, J. Seume, M. Snellen, A. Strohmayr, L. Veldhuis, I. Fernandez Villegas, R. Vos, H. Werij, and F. Yin. Accelerating the path towards carbon-free aviation. Technical report, CoE Sustainable and Energy Efficient Aviation (SE2A) and Aeronautics Research Centre Niedersachsen (NFL), 2022.
- [14] J. J. Berton. Design of an open rotor, braced-wing electric transport. NASA Technical Memorandum NASA/TM-20220015470, National Aeronautics and Space Administration, Glenn Research Center, Cleveland, OH, USA, December 2022.
- [15] A. Betz. *Windenergie und ihre Ausnutzung durch Windmühlen*. Ökobuch Verlag, 1926.
- [16] L. A. Bonomo, J. A. Cordioli, A. K. Colaciti, J. V. N. Fonseca, and L. G. C. Simões. A comparison of tonal-broadband decomposition algorithms for propeller noise. *International Journal of Aeroacoustics*, 23(7-8):737–762, Nov. 2024.
- [17] K. S. Bretner, G. A. Bres, G. Perez, and H. E. Jones. Maneuvering rotorcraft noise prediction - a new code for a new problem. In *International Specialists’ Meeting on Aerodynamics, Acoustics, and Test and Evaluation January 23-25, 2002, San Francisco, California*. American Helicopter Society, January 2002.
- [18] T. F. Brooks, D. S. Pope, and M. A. Marcolini. Airfoil self-noise and prediction. Technical Report NASA-RP-1218, National Aeronautics and Space Administration, Langley Research Center, Hampton, Virginia, 1989.
- [19] K. W. Bushell. Jet and gas turbine engines. In R. A. Meyers, editor, *Encyclopedia of Physical Science and Technology*, pages 127–152. Academic Press, 3 edition, 2003.
- [20] CAPA - Centre for Aviation. More turboprops coming to the market - maybe. <https://centreforaviation.com/analysis/reports/more-turboprops-coming-to-the-market---maybe-30634>, July 2010. CAPA Analysis Report.
- [21] D. S. Chelemen, I. Bunesu, M.-V. Pricop, and M.-G. Stoican. The effects of distributed electric propulsion on the aerodynamics of a regional aircraft wing. *INCAS Bulletin*, 15(4):65–77, 2023.
- [22] R. T. N. Chen. A survey of nonuniform inflow models for rotorcraft flight dynamics and control applications. In *European Rotorcraft Forum*, Amsterdam, Netherlands, Sept. 1989. National Aeronautics and Space Administration. NASA Technical Memorandum NASA-TM-102219, A-89220.
- [23] C. F. Chien and W. Soroka. Sound propagation along an impedance plane. *Journal of Sound and Vibration*, 43(1):9–20, Mar. 1975.
- [24] W. Choi, I. M. Jeong, J. H. You, J. H. Kim, and I. W. Lee. Performance analysis of turboprop aircraft propulsion system by using gasturb. In *Proceedings of the KSPE Fall Conference*, pages 371–377. Korean Society of Propulsion Engineers, 2009.

- [25] M. A. Clarke and J. Alonso. Evaluating the performance and acoustic footprint of aircraft for regional and urban air mobility. In *AIAA AVIATION 2021 FORUM, VIRTUAL EVENT*, Aug. 2021. American Institute of Aeronautics and Astronautics.
- [26] R. P. Coleman, A. M. Feingold, and C. W. Stempin. Evaluation of the induced-velocity field of an idealized helicopter rotor. NACA Wartime Report NACA-ARR-L5E10, National Advisory Committee for Aeronautics, Langley Memorial Aeronautical Laboratory, Langley Field, VA, USA, June 1945. DTIC Accession No. ADA801123.
- [27] C. Courtin, R. J. Hansman, and M. Drela. Flight test results of a subscale super-stol aircraft. In *AIAA Scitech 2020 Forum*, Reston, VA, June 2020. AIAA, AIAA.
- [28] C. Courtin, A. Mahseredjian, J. Hansman, and M. Drela. A performance comparison of estol and evtol aircraft. In *AIAA AVIATION 2021 FORUM*, Reston, VA, Jul 2021. AIAA, American Institute of Aeronautics and Astronautics.
- [29] C. B. Courtin. *Performance, Stability and Control of Electric Short Takeoff and Landing Aircraft*. Phd thesis, Massachusetts Institute of Technology, Cambridge, Massachusetts, June 2024. Available at <https://dspace.mit.edu/handle/1721.1/155476>.
- [30] J. Czech, M. Rimjha, T. Middleton, D. Cuppoletti, P. Sorensen, and P. Cobb. Noise estimation framework for advanced air mobility. In *Forum Acusticum 2023*, 2023.
- [31] A. DC/E. Atr family. Brochure, 2011. Accessed: 2026-03-05.
- [32] R. de Vries, R. E. Wolleswinkel, J. Exalto, P. van den Berg, R. Vos, and M. F. M. Hoogreef. Conceptual redesign of a 90-seater battery-electric aircraft. In *AIAA Aviation Forum and ASCEND 2025*, number AIAA 2025-3153, Las Vegas, Nevada, 2025. American Institute of Aeronautics and Astronautics.
- [33] M. Drela. QPROP Formulation. https://web.mit.edu/drela/Public/web/qprop/qprop_theory.pdf, 2006.
- [34] J. F. Dugan, B. A. Miller, E. J. Graber, and D. A. Sagerser. The nasa high-speed turbo-prop program. NASA Technical Memorandum NASA-TM-81561, National Aeronautics and Space Administration, Lewis Research Center, Cleveland, Ohio, Oct. 1980. Also published as SAE Paper 801120, presented at the Aerospace Congress and Exposition, Los Angeles, California, October 13–16, 1980.
- [35] R. Dunn. What is sound pressure level (spl) and how is it measured? Pulsar Instruments, Oct. 15, 2019.
- [36] EHang. Ehang 184. <https://www.ehang.com/ehangaav>, 2018. Accessed: 2025-11-04.
- [37] Electric VTOL News. Wisk aero cora (generation 5) (prototype). <https://evtol.news/kitty-hawk-cora>, 2018. Accessed: 2025-11-04.

- [38] European Union Aviation Safety Agency (EASA). *TYPE-CERTIFICATE DATA SHEET No. IM.E.041 for Engine PW100 series engines*, 12 2023. Issue/Revision 07.
- [39] F. Farassat. Derivation of formulations 1 and 1a of farassat. Technical Report NASA/TM-2007-214853, Langley Research Center, Hampton, Virginia, USA, 2007.
- [40] F. Farassat and G. P. Succi. A review of propeller discrete frequency noise prediction technology with emphasis on two current methods for time domain calculations. *Journal of Sound and Vibration*, 71(3):399–419, Aug. 1980.
- [41] Federal Aviation Administration. 14 C.F.R. §23.925, Propeller Clearance, 2016. Airworthiness Standards: Normal, Utility, Acrobatic, and Commuter Category Airplanes.
- [42] J. A. Feight, J. D. Jacob, and R. J. Gaeta. Acoustic characterization of a multi-rotor uas as a first step towards noise reduction. In *5th AIAA Aerospace Sciences Meeting*, Reston, VA, January 2017. AIAA, AIAA.
- [43] J. E. Ffowcs Williams and D. L. Hawkings. Sound generation by turbulence and surfaces in arbitrary motion. *Philosophical Transactions of the Royal Society A*, 264(1151):321–342, May 1969.
- [44] Z. F. Gan. Time-varying rotor noise computations and analysis of electric vertical take-off and landing aircraft. M.s. thesis, The Pennsylvania State University The Graduate School, University Park, PA, USA, 2021.
- [45] A. Gautam, L. Hanson, D. Rezgui, B. Zang, and M. Azarpeyvand. Effect of tip mach number on propeller noise in edgewise flight. *The Journal of the Acoustical Society of America*, 157(6), 2025.
- [46] H. Gesell, F. Wolters, and M. Plohr. System analysis of turbo electric and hybrid electric propulsion systems on a regional aircraft. In *31st Congress of the International Council of the Aeronautical Sciences (ICAS 2018)*, Belo Horizonte, Brazil, 2018. International Council of the Aeronautical Sciences.
- [47] H. Glauert. *The Elements of Aerofoil and Airscrew Theory*. Cambridge University Press, 1926.
- [48] S. Goldstein. On the vortex theory of screw propellers. *Proceedings of the Royal Society of London. Series A*, 123(792):440–465, Jan. 1929.
- [49] V. Gonzalez, S. H. Yeung, J. L. Huynh, and R. J. Hansman. Impact of takeoff trajectory design on performance and noise for advanced air mobility aircraft. *Journal of Aircraft*, 63(2):793–808, Mar. 2026.
- [50] R. Habing, K. H. Lammers, P. Admiraal, R. Bakker, H. Lania, R. Aalmoes, and V. J. M. C. Vos. A noise prediction tool for iam and aam in urban environments. In *50th European Rotorcraft Forum 2024*, Marseille, France, Sept. 2024. Associazione Italiana di Aeronautica e Astronautica (AIDAA).

- [51] J. Hermetz, M. Ridel, and C. Döll. Distributed electric propulsion for small business aircraft: A concept-plane for key-technologies investigations. In *30th Congress of the International Council of the Aeronautical Sciences*, Daejeon, Republic of Korea, 2016. ICAS Paper 2016-0461.
- [52] C. W. Hicks and H. H. Hubbard. Comparison of sound emission from two-blade, four-blade, and seven-blade propellers. Technical Note NACA TN 1354, National Advisory Committee for Aeronautics, Washington, DC, USA, July 1947.
- [53] S.-C. A. Huang. An analysis of air passenger average trip lengths and fare levels in us domestic markets. Working paper, University of California, Berkeley, Feb. 2000.
- [54] F. Hutcheson and T. Brooks. Effects of angle of attack and velocity on trailing edge noise. In *42nd AIAA Aerospace Sciences Meeting and Exhibit*. AIAA, American Institute of Aeronautics, January 2004.
- [55] T. Jardin, R. Gojon, N. Doué, and H. Parisot-Dupuis. Numerical and experimental analysis of the influence of solidity on rotor aeroacoustics at low reynolds numbers. *International Journal of Aeroacoustics*, 22(1–2):85–109, Mar. 2023.
- [56] Jet Propulsion Laboratory. Acoustic noise requirement. Preferred Reliability Practice PD-ED-1259, Jet Propulsion Laboratory, May 1996.
- [57] W. Johnson. Ndac nasa design and analysis of rotorcraft. Technical Report NASA/TP—2015–218751, Ames Research Center, 2015.
- [58] M. Kamruzzaman, T. Lutz, K. Nuebler, and E. Krämer. Implementation and verification of an aeroacoustic wind turbine blade analysis tool. In *Fourth International Meeting on Wind Turbine Noise*, pages 8–10, Apr 2011.
- [59] S. Kaneko, M. Arnson, H. Hajdik, G. Cinar, J. R. R. A. Martins, and A. Uranga. Gradient-based MDAO framework for advanced aircraft concepts. In *AIAA SciTech Forum 2026*, Orlando, Florida, Jan. 2026. AIAA Paper 2026-1302.
- [60] R. J. Keuter, B. Kirsch, J. Friedrichs, and B. Ponick. Design decisions for a powertrain combination of electric motor and propeller for an electric aircraft. *IEEE Access*, 11:79144–79155, 2023.
- [61] H. D. Kim, A. T. Perry, and P. J. Ansell. A review of distributed electric propulsion concepts for air vehicle technology. In *2018 AIAA/IEEE Electric Aircraft Technologies Symposium*, number 2018-4998, pages 1–21, Cincinnati, Ohio, July 2018. American Institute of Aeronautics and Astronautics.
- [62] Y. N. Kim and A. R. George. Trailing-edge noise from hovering rotors. *AIAA Journal*, 20(9):1167–1174, Sept. 1982.
- [63] Kittyhawk. Kitty hawk. <https://www.kittyhawk.aero/>, 2018. Accessed: 2025-11-04.

- [64] J. Ko, B. J. German, and J. Rauleder. Community noise impact of multirotor configurations during landing procedures. In *AIAA AVIATION 2023 Forum*, 2023.
- [65] S. Krishnamurthy, S. A. Rizzi, R. Cheng, D. D. Boyd, and A. W. Christian. Prediction-based auralization of a multirotor urban air mobility vehicle. In *AIAA SciTech 2021 Forum*, 2021.
- [66] R. E. Kuhn. Semiempirical procedure for estimating lift and drag characteristics of propeller-wing-flap configurations for vertical-and short-take-off-and-landing airplanes. NASA Memorandum NASA-MEMO-1-16-59L, National Aeronautics and Space Administration, Langley Research Center, Hampton, VA, Feb. 1959.
- [67] M. C. Lau, C. J. Scarpone, E. R. Boeker, D. A. Senzig, C. J. Roof, C. S. Y. Lee, and G. A. Burdette. Aircraft source noise measurement studies: Summary of measurements, data and analysis for the cessna 182 skylane, cessna 208b grand caravan, dornier 228-202, dornier 328-100, piper pa-42 cheyenne iii, bell 407, robinson r44 raven, and schweizer 300c. Final Report DOT-VNTSC-FAA-10-17 / DOT-FAA-AEE-2010-06, U.S. Department of Transportation, Federal Aviation Administration, Washington, DC, Oct. 2010. Prepared by the John A. Volpe National Transportation Systems Center.
- [68] J. G. Leishman. *Principles of Helicopter Aerodynamics*. CAMBRIDGE UNIVERSITY PRESS, 2000.
- [69] S. K. Li and S. Lee. Prediction of rotorcraft broadband trailing-edge noise and parameter sensitivity study. *Journal of the American Helicopter Society*, 65, Oct. 2020.
- [70] S. K. Li and S. Lee. Prediction of urban air mobility multirotor vtol broadband noise using ucd-quietfly. *Journal of the American Helicopter Society*, 66, July 2021.
- [71] M. J. Lighthill. On sound generated aerodynamically i. general theory. *Proceedings of the Royal Society A*, 211(1107):564–587, 1952.
- [72] L. T. Lima Pereira, S. Wang, and D. Ragni. A preliminary design framework for performance and noise assessment of urban air mobility vehicles. In *30th AIAA/CEAS Aeroacoustics Conference (2024)*, Reston, VA, June 2024. AIAA, AIAA.
- [73] L. N. Long and K. S. Brentner. Self-scheduling parallel methods for multiple serial codes with application to wopwop. In *AIAA 38th Aerospace Sciences Meeting and Exhibit*, number 2000-0346 in AIAA Paper, Reno, NV, January 2000. American Institute of Aeronautics and Astronautics.
- [74] L. V. Lopes and C. L. Burley. Design of the next generation aircraft noise prediction program: Anopp2. In *17th AIAA/CEAS Aeroacoustics Conference*, 2011.
- [75] L. V. Lopes and C. L. Burley. Anopp2 user’s manual: Version 1.2. Technical Report NASA/TM-2016-219342, NASA Langley Research Center, Oct. 2016.

- [76] G. Margalida, T. Sinnige, R. de Vries, J. Exalto, and R. E. Wolleswinkel. Performance analysis of distributed propellers: Efficiency and noise trade-offs in full-electric regional aircraft. In *AIAA AVIATION FORUM AND ASCEND 2025*, Las Vegas, Nevada, July 2025. AIAA Paper 2025-3688.
- [77] J. E. Marte and D. W. Kurtz. A review of aerodynamic noise from propellers, rotors, and lift fans. Technical Report 32-7462, NATIONAL AERONAUTICS AND SPACE ADMINISTRATION, Langley Research Center Hampton, Virginia, 1970.
- [78] E. C. Maskell and D. A. Spence. A theory of the jet flap in three dimensions. *Proceedings of the Royal Society A Mathematical, Physical And Engineering Sciences*, 251, June 1959.
- [79] D. C. Mikkelsen and G. A. Mitchell. High speed turboprops for executive aircraft: Potential and recent test results. NASA Technical Memorandum NASA-TM-81482, National Aeronautics and Space Administration, Lewis Research Center, Cleveland, Ohio, Apr. 1980. Presented at the Turbine-Powered Executive Meeting, Phoenix, Arizona, April 9–11, 1980.
- [80] E. Molatudi, T. J. Kunene, and L. K. Tartibu. A fflowcs williams-hawkings numerical aeroacoustic study of varied and fixed-pitch blades of an h-rotor vertical axis wind turbine. In *12th South African Conference on Computational and Applied Mechanics (SACAM2020)*, Les Ulis Cedex, France, November 2021. MATEC Web of Conferences, EDP Sciences.
- [81] D. M. Nark, M. R. L. R. Burley, and C. R. I. D. Burdis. High-lift propeller noise prediction for a distributed electric propulsion flight demonstrator. In *2020 AIAA/CEAS Aeroacoustics Conference*, number AIAA 2020-2552. American Institute of Aeronautics and Astronautics, 2020.
- [82] M. F. Niřa and D. Scholz. Aircraft design studies based on the atr 72. In *8th International Conference on Recent Research and Design Progress in Aeronautical Engineering and its Influence on Education (RRDPAE)*, Brno, Czech Republic, 2008. Conference presentation.
- [83] V. R. Pellerito, N. H. Yeung, J. L. Huynh, and R. J. Hansman. Impact of flight trajectory design on performance and noise for aam aircraft. In *AIAA AVIATION FORUM AND ASCEND 2024*, number 2024-4076 in AIAA Paper, Reston, VA, jul 2024. American Institute of Aeronautics and Astronautics.
- [84] A. T. Perry, T. Bretl, and P. J. Ansell. System identification of a subscale distributed electric propulsion aircraft. *Journal of Aircraft*, 60(3):702–715, May 2023.
- [85] D. Pitt. *Rotor Dynamic Inflow Derivatives and Time Constants From Various Inflow Models*. Phd dissertation, Washington University, 1990.
- [86] D. Pitt and D. Peters. Rotor dynamic inflow derivatives and time constants from various inflow models. In *Ninth European Rotorcraft Forum*. ASD, September 1983.

- [87] J. Pons-Prats, T. Živojinović, and J. Kuljanin. On the understanding of the current status of urban air mobility development and its future prospects: Commuting in a flying vehicle as a new paradigm. *Transportation Research Part E: Logistics and Transportation Review*, 166:102868, 2022.
- [88] D. P. Raymer. *Aircraft Design: A Conceptual Approach*. American Institute of Aeronautics and Astronautics, Reston, VA, 5th edition, 2012.
- [89] S. Rizzi, J. A. Page, and R. Cheng. Comparison of two community noise models applied to a nasa urban air mobility concept vehicle. In *50th International Congress and Exposition on Noise Control Engineering (Inter-Noise 2021)*. Institute of Noise Control Engineering (I-INCE), May 2021.
- [90] S. Rizzi and M. Rafaelof. Second generation uam community noise assessment using the faa aviation environmental design tool. In *AIAA SCITECH 2022 Forum*, Reston, VA, January 2022. AIAA, AIAA.
- [91] S. A. Rizzi, D. L. Huff, D. D. Boyd, P. Bent, B. S. Henderson, K. A. Pascioni, D. C. Sargent, D. L. Josephson, M. Marsan, H. He, and R. Snider. Urban air mobility noise: Current practice, gaps, and recommendations. NASA Technical Publication NASA/TP-2020-5007433, National Aeronautics and Space Administration, Hampton, VA, 2020.
- [92] S. A. Rizzi, I. LeGriffon, R. Pieren, and L. Bertsch. A comparison of aircraft flyover auralizations by the aircraft noise simulation working group. In *AIAA AVIATION 2020 FORUM*, pages 1–16. American Institute of Aeronautics and Astronautics, June 2020. AIAA Paper 2020-2582.
- [93] S. A. Rizzi, S. J. Letica, D. D. J. Boyd, and L. Lopes. Prediction of noise-power-distance data for urban air mobility vehicles. *JOURNAL OF AIRCRAFT*, 61(1):166–182, Jan. 2024.
- [94] SAE International. Gas turbine jet exhaust noise prediction. Aerospace Recommended Practice ARP876F, SAE International, 2013. Issued 1978-03; Revised 2013-05; Reaffirmed 2021-01; Superseding ARP876E.
- [95] N. A. T. Services. Aircraft noise. Accessed Oct. 1, 2022 [Online].
- [96] C. Silva and W. Johnson. Vtol urban air mobility concept vehicles for technology development. In *2018 AIAA Aviation Technology, Integration, and Operations Conference*, number 2018-3847 in AIAA AVIATION Forum, Dallas, TX, USA, June 2018. American Institute of Aeronautics and Astronautics.
- [97] Z. S. Spakovszky, E. M. Greitzer, I. A. Waitz, and D. Quattrochi. 16.unified: Thermodynamics and propulsion lecture notes. Massachusetts Institute of Technology, n.d. Accessed: 2026-01-07.

- [98] A. Stoll and J. Bevirt. Development of evtol aircraft for urban air mobility at joby aviation. In *Vertical Flight Society's 78th Annual Forum and Technology Display*. Vertical Flight Society, Vertical Flight Society, May 2022.
- [99] A. P. Synodinos, R. H. Self, and A. J. Torija. Preliminary noise assessment of aircraft with distributed electric propulsion. In *2018 AIAA/CEAS Aeroacoustics Conference*, number AIAA 2018-2817, Atlanta, Georgia, 2018. American Institute of Aeronautics and Astronautics.
- [100] A. P. Synodinos, R. H. Self, and A. J. Torija Martinez. Noise assessment of aircraft with distributed electric propulsion using a new noise estimation framework. In *24th International Congress on Sound and Vibration (ICSV 24)*, pages 947–954, London, United Kingdom, July 2017.
- [101] T. Theodorsen. *Theory of Propellers*. McGraw-Hill Book Co., New York, 1948.
- [102] C. S. Thurman and J. D. Baeder. Blade-wake interaction noise for small hovering rotors, part i: Characterization study. *AIAA Journal*, 61(6):2458–2475, June 2023.
- [103] L. Ting, C. Liu, and G. Kleinstein. Interference of wing and multipropellers. *AIAA Journal*, 10(7), 1972.
- [104] B. B. Turhan, H. K. Jawahar, D. Rezgui, and M. Azarpeyvand. Influence of propeller arrangement on aeroacoustic interactions in distributed-propulsion systems in forward flight. *Applied Acoustics*, 254:111443, 2026.
- [105] F. E. Weick. *Aircraft Propeller Design*. McGraw-Hill Book Company, Inc, New York and London, 1930.
- [106] J. Woodcock, S. Hasan, J. Garcia Paje, R. Biziorek, P. Henning, A. Guthrie, V. Jurdic, A. L. Maldonado, and D. Hiller. Development of a noise-based route optioneering tool for advanced air mobility (aam) vehicles. In *28th AIAA/CEAS Aeroacoustics 2022 Conference*, number 2022-2836 in AIAA/CEAS Aeroacoustics Conference. American Institute of Aeronautics and Astronautics, June 2022.
- [107] N. Yeung, J. Cruz, and V. Pellerito. Flight procedure and community noise modeling of advanced air mobility flight vehicles. In *AIAA AVIATION 2023 Forum*, Reston, VA, June 2023. AIAA, AIAA.
- [108] N. Yeung, V. R. Pellerito, J. De la Cruz, M. Emmanouilidi, and J. L. Huynh. Component-based noise modeling for distributed propulsion blown-flap stol vehicle flight procedures. In *AIAA AVIATION 2022 Forum*, Reston, VA, June 2022. AIAA, AIAA.
- [109] S. H. Yeung and J. L. Huynh. Impact of design and operation of distributed electric propulsors to aircraft noise. Manuscript submitted to the Journal of Aircraft, 2026.

- [110] N. S. Zawodny, D. D. Boyd, and D. M. Nark. Aerodynamic and acoustic interactions associated with inboard propeller-wing configurations. In *AIAA SciTech 2021 Forum*, 2021. AIAA Paper 2021-0714.
- [111] B.-E. Zolbayar. Investigation of noise from electric, low-tip-speed aircraft propellers. M.s. thesis, The Pennsylvania State University The Graduate School College of Engineering, State College, PA, USA, 2018.
- [112] V. Štorch, J. Nožička, M. Brada, J. Gemperle, and J. Suchý. Measurement of noise and its correlation to performance and geometry of small aircraft propellers. In *EPJ Web of Conferences*, volume 114, page 02112. EDP Sciences, 2016.