

# Review of Computational Models for Large-Scale MDAO of Urban Air Mobility Concepts

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**The advent of Urban Air Mobility (UAM) has necessitated a paradigm shift in aircraft design from traditional regression methods to physics-based analysis and the use of modern computational methods. This paper explores the intricacies of UAM aircraft design, acknowledging the limitations of historical empirical equations and advocating for the use of physics-based tools in the early stages of the design process. It underscores the importance of Multidisciplinary Design, Analysis, and Optimization (MDAO) as a means to integrate physics-based tools for conceptual design, facilitating decisions on configuration and sizing. The paper presents a comprehensive survey and review of computational models across various disciplines pertinent to eVTOL/UAM/AAM concepts, aiming to guide non-discipline Subject Matter Experts (SMEs) in the application of these models within an MDAO framework. Additionally, we present ongoing work on the Verification and Validation (V&V) of a soon-to-be-released toolset developed under a NASA ULI project toward designing UAM concepts using large-scale MDAO. V&V results for low-to-mid-fidelity tools are presented, demonstrating the accuracy of the tools for UAM concept analysis and design, as validated by industry leaders and comparison with authoritative sources.**

## **I. Introduction**

Aircraft design is a complex and multidisciplinary process. Traditionally, key drivers in aircraft design included considerations of aerodynamics, propulsion with conventional gas engines, structures, and flight dynamics. Historically, the design of aircraft has predominantly relied on empirical equations and basic first-order analysis. Established methods, such as those proposed by Loftin [1], Torenbeek [2], Raymer [3], and Roskam [4], have been widely utilized for the design of conventional tube-and-wing aircraft.

In recent years, there has been a growing interest in electric vehicles for Urban Air Mobility (UAM). This emerging field has captured the attention of various stakeholders, including researchers, policymakers, and industry professionals. The potential benefits of UAM, such as reducing traffic congestion and enabling efficient transportation in urban areas, have spurred significant research and development efforts.

In UAM, emerging concepts have rendered traditional sizing methods and their reliance on historical regressions obsolete. These novel UAM concepts integrate advanced propulsion architectures, vertical take-off and landing (VTOL), and are increasingly influenced by factors such as electrification, state-of-the-art battery technology, and propulsion systems involving motors and rotors. Additionally, considerations like noise reduction have gained prominence in the design process.

To effectively address the complexities of UAM design, the use of physics-based tools has become imperative, particularly in the early stages of the design process. Modern scientific computing methods coupled with physics-based tools are being increasingly employed to design both conventional and novel aircraft concepts. However, these tools are typically utilized in the later stages of the design process, after critical decisions regarding configuration and size have already been made. To be practical and useful for early-stage conceptual design, these tools must be accessible and applicable at the initial stages of the design process.

Multidisciplinary Design, Analysis, and Optimization (MDAO) provides a viable approach to incorporating physics-based tools in the early stages of aircraft design. Physics-based tools often rely on detailed parameters that are typically unknown during the early design phase, especially for advanced configurations. MDAO treats these unknown parameters as design variables within an optimization problem, enabling the use of physics-based tools in early-stage design.

MDAO often uses physics-based models for the disciplines involved in aircraft design. These discipline models come in a wide range of computational runtime and accuracy at representing the true behavior of the aircraft. Typically, low-fidelity tools are computationally inexpensive but inaccurate, whereas high-fidelity tools are accurate but computationally expensive. For aerodynamics, methods range from lifting line, vortex lattice & panel methods to computational fluid dynamics (CFD). For structures, the aircraft is represented either as a thin-wall shell or as beams joined to one another. In the case of rotor-power propulsion, simple tools include the blade element momentum (BEM) method for low-fidelity, lifting-line or vortex-based methods for mid-fidelity, and particle methods or CFD for high-fidelity. The preceding discussion highlights that there exists a large number of physics-based tools available to analyze the disciplines and the choice of the tools used will influence the computational runtime and the results of the optimization.

In this work, we present a survey and review of computational models for use in MDAO of eVTOL/UAM/AAM concepts. The scope of this review is extensive, encompassing a wide array of disciplines and the corresponding levels of fidelity pertinent to early-stage conceptual design. This consolidation of essential data and methodologies provides a

comprehensive primer, particularly beneficial for non-discipline Subject Matter Experts (SMEs), facilitating an informed selection and application of models within the MDAO framework.

Further, this paper presents results for ongoing V&V efforts for low-to-mid-fidelity computational tools developed under a NASA University Leadership Initiative (ULI) project and will be released as an open-source toolset. Within the scope of the ULI project, multiple disciplinary tools of varying fidelity have been developed. The objective is to show the precision and reliability of these tools when applied to the analysis and design of UAM concepts.

The toolset offers several unique capabilities for large-scale MDAO of UAM concepts. Overarching is the CADDEE aircraft design framework (Section II.C). CADDEE is invariant to the vehicle concept, the propulsion system architecture, or the physics-based analysis methods used. Thus conventional aircraft or novel concepts, fuel-based or new propulsion system architectures can be sized. It allows for comprehensive analysis modeling the entire aircraft across disciplines and multiple mission segments. CADDEE has a geometry engine that starts from a baseline OML geometry defined by B-splines and permits differentiable parametric changes to the geometry that can be propagated to each physics-based solver. In addition, the framework features a Solver Independent Field Representation (SIFR). In SIFR, instead of transferring data between solvers directly, all solver input/output is exchanged with the framework. Further, all field data is represented continuously by the framework via a map from parametric space. Such a representation permits modular coupling between solvers. Finally, the framework uses a modeling approach based on a three-stage compiler that fully automates derivative computation required for gradient-based optimization with the efficiency of the adjoint method. For the V&V of the toolset, we define three terms:

- 1) *Code-to-code verification*: Here, we consider tools with the same mathematical formulation, but different implementations. As an example, rotor performance can be evaluated using Blade Element Momentum Theory (BEMT). Two implementations of this mathematical formulation are UCSD/LSDO's rotor tool [5] and BYU/FLOW Lab's CCBlade [6].
- 2) *Code-to-code validation*: Here, we consider comparison to tools considered industry-standard. The underlying mathematical formulation could be different in this case. As an example, the structural weight of the wingbox can be found by representing the wingbox as a shell model. Siemens Femap is considered an industry-standard code that can represent the wingbox as a beam model. UCSD/LSDO's aframe and GT/ASDL's SWIFT tools will be compared against the industry-standard code.
- 3) *Validation*: Here, we compare against data that is considered as 'authoritative sources of truth'. Such data includes comparison to high-fidelity CFD simulations, experimental data, flight-test data, and as-built vehicles.

Collaborating closely with Aurora Flight Sciences, A Boeing Company, this study leverages their Passenger Air Vehicle (PAV) prototype as the testbed for comprehensive analysis (Appendix A). Aurora Flight Sciences' domain expertise in UAM design is complemented by their suite of industry-standard design tools, facilitating a robust comparative framework. This paper presents code-to-code verification results to ensure consistency with equivalent mathematical formulations. Subsequently, the codes are compared to Aurora Flight Sciences' benchmark industry-standard tools to establish their validity (code-to-code validation). Finally, where available, validation is conducted against experimental data and the as-built PAV prototype.

In addition to affirming the accuracy of the tools, the paper will provide a discussion on their relative merits and limitations, with a particular focus on the trade-off between computational efficiency and accuracy. This discourse aims to aid practitioners in the selection of the most fitting computational tool for specific design studies.

## II. Background

### A. Urban Air Mobility (UAM)

Urban Air Mobility (UAM) is an emerging field of aerospace engineering focused on the development of air transportation systems within urban environments. The concept envisions a future where air travel is integrated into the daily commute, offering an alternative to traditional ground transportation. In their comprehensive study, Asmer et al. [7] put forth a classification for UAM missions, segmenting them into distinct categories that reflect the diversity of potential uses and requirements. These categories are as follows:

- 1) **Airport Shuttle Services**: Designed to carry 1-4 passengers over distances around 25 km, these services aim to facilitate quick and efficient transport to and from airports, accommodating substantial baggage payloads.
- 2) **On-demand Air-Taxis**: These vehicles cater to 1-4 passengers for trips up to 50 km, ideal for spontaneous travel with minimal luggage, emphasizing speed and convenience.
- 3) **Mega-city Air Taxis**: With a capacity for 1-6 passengers, these taxis cover urban sprawls with a range of 70-100

km, promising a swift transit solution in densely populated areas.

- 4) **Inter-city/Regional Travel:** This category is tailored for longer distances, exceeding 100 km, and is capable of carrying up to 25 passengers. These missions balance between payload capacity and range, serving broader regional connectivity.

Figure 1 illustrates a typical mission profile for UAMs, depicting the operational parameters.

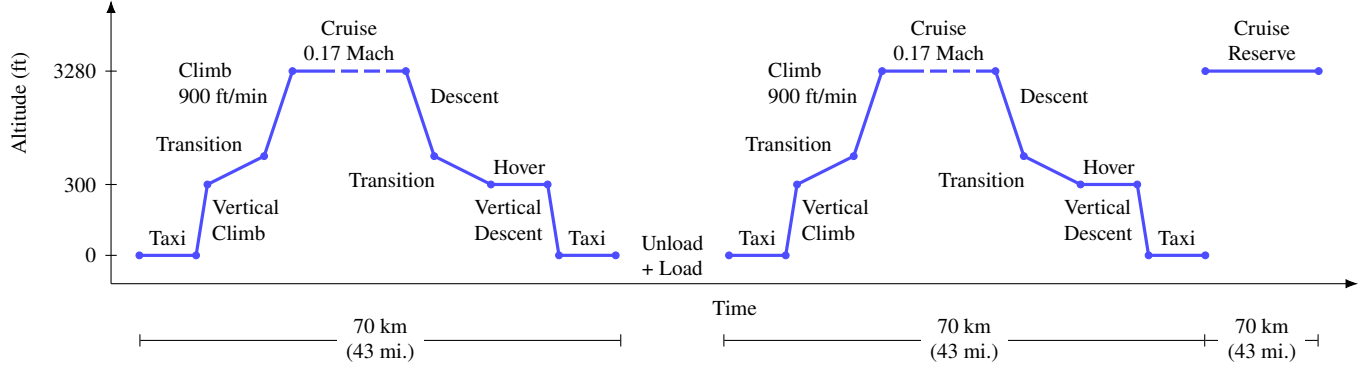


**Fig. 1 Typical UAM mission profile**

A number of concepts have been proposed for UAMs (Fig. 2). Broadly, UAMs can be categorized as either rotary-wing or fixed-wing. The rotary-wing concepts generate lift solely from their propellers. Fixed-wing concepts on the other hand use a combination of rotors and wings to generate lift. Fixed-wing concepts can be further classified into lift+cruise and vectored thrust. L+C configurations have two propulsion systems: one for lift during vertical flight and the other for forward flight. Vectored thrust configurations have propulsion systems that can be manipulated to direct thrust as needed to allow for a single propulsion to be used for both vertical and forward flight.

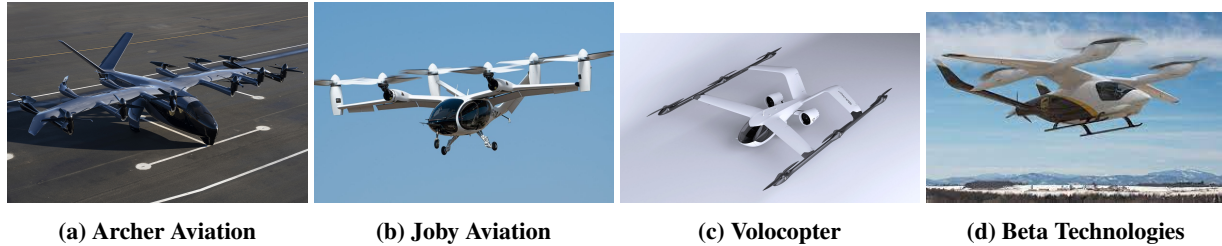


**Fig. 2 Industry eVTOL concepts**

Recently, a compilation of over 35 of the primary industry AAM designs currently under development was presented by Kiesewetter et al. [8]. Other reviews have been published by the Vertical Flight Society in their eVTOL Aircraft Directory, NASA's UAM Reference Vehicles, and Shamiyeh et al. [9]. Fixed-wing designs with traditional runway takeoff or STOL abilities that could be used for UAM applications have been covered previously by Brelje and Martins [10].

Propulsion system architectures are typically fuel-based engines, hybrid-electric engines, and all-electric engines. In this work, we scope to all-electric engines. Section III.E covers an in-depth explanation of the all-electric powertrain system. The readers are directed to Brelje and Martins [10] for a detailed explanation of other types of propulsion systems.

The literature also has articles reviewing various aspects of the current state of research and development in UAM. Straubinger et al. [11] discuss operational range-payload considerations, rotor configurations, propulsion systems, and environmental considerations. Garrow et al. [12] compare eVTOLs against autonomous and electric ground-based transportation. The review paper considers regulatory considerations and addresses external factors that could hinder UAM beyond the scope of vehicle design. Xie et al. [13, 14] discuss a framework for certification-driven design applicable to all categories of vehicles. The paper discusses constraints from 14-CFR SubPart B Flight Characteristics and 14-CFR SubPart C Structures in an MDAO problem. Bauranov et al. [15] discuss the current manner in which

different airspace sections are organized, potential bad weather conditions, specific areas where flying is restricted; and how these considerations affect where UAM vehicles will operate. As safety and reliability are critical in the adoption of UAM, the powertrain architecture must be made redundant and have a high level of reliability. Bendarkar et al. [16] propose a method that uses functional hazard assessment and a Bayesian decision framework to make a compliance assessment.

## B. Design of UAM Concepts

A recent review paper by Kiesewetter et al. [8] suggests a difference in the understanding of optimal aircraft performance between academic studies and industry designs. Academic studies suggest tilt-rotor configurations as optimal for their extended range and payload capacities. On the other hand, many concepts being developed in the industry prefer lift-plus-cruise designs or partially thrust-vectored designs. Specialized roles of multi-rotor and tilt-wing designs in urban short-distance and high-payload missions, respectively, further delineate the need for nuanced design tools capable of traversing the breadth of UAM applications. It has been seen in the literature that different concepts are best suited for different missions. Bacchini and Cestino [17] calculated that for short (7 km) urban missions the multi-rotor representative would require the least energy, whereas tiltrotors would require the least for longer 30 km and 100 km missions. The above underscores the necessity for advanced simulation and design tools that can reconcile these differences, ensuring higher fidelity in simulation outcomes and their applicability to real-world scenarios.

The design of an aircraft involves sizing the concepts to meet the requirements of the stakeholders. These stakeholders include operators, passengers, federal regulatory bodies, and shareholders. The stakeholders specify requirements on the mission, performance, payload, cost, noise, safety, and greenhouse emissions, among other metrics. Additionally, the early stages of design explore the effect of novel technologies (tow-steered composites, boundary-layer ingestion, etc.) on the system to evaluate the benefits and feasibility of the technology. Each sub-section in Section 3 will dive deeper into the design parameters relevant to the discipline, its outputs, metrics from the discipline that affect the aircraft performance, and the metrics from the disciplines that safety and compliance with safety regulations.

Johnson and Silva [18] from NASA have presented multiple reduced-emission rotorcraft concepts. They used CAMRAD II and CHARM to accurately model the wake behind the rotors which is necessary to accurately predict performance and noise. Sarojini et al. [19] recently presented large-scale MDO for the NASA lift-plus-cruise eVTOL concept using low-fidelity tools. The authors considered VLM for aerodynamics, BEM for rotor propulsion, regressions from M4 engineering for structural weight, 3 DoF flight dynamics model to trim the aircraft at its various mission segments, motor model to size and analyze the motors, and Barry-Magliozi & Schlegel-King-Mull models to obtain rotor noise. Nguyen et al. [20] presented an aeroservoelastic flight dynamic modeling approach that uses an unsteady vortex-lattice method for aerodynamics, modal analysis using NASTRAN for structures, and Theodorsen's theory for aeroservoelastic analysis. Chakraborty et al. have demonstrated integrated vehicle and propulsion system sizing and performance analysis for the tilt-wing concept [21], the conventional lift-plus-cruise concept [22] and the ducted lift-plus-cruise concept [23]. They use strip theory-based approach for the lifting surfaces, and lookup tables for the remainder of the geometry. The sectional aerodynamic coefficients at each strip are obtained using FlightStream. Blade element momentum (BEM) is used as the rotor propulsion model, and a 6 DoF flight mechanics model is used to trim the aircraft at various mission segments.

In addition to the physics-based models described above, a number of aircraft sizing frameworks have been proposed that provide the mechanisms to connect the discipline-level tools to one another and perform aircraft-level analysis. An incomplete list of tools is described next. The NASA Design and Analysis of Rotorcraft (NDARC) tool was used by Johnson and Silva [18] to size the aircraft and obtain its performance characteristics. NDARC uses surrogate models for rotor and engine/motor performance, battery weight and efficiency and technology factors for component weights. NASA's Layered and Extensible Aircraft Performance System (LEAPS) tool [24], the successor to FLOPS enables the analysis and design of advanced aircraft concepts and architectures that include electric and hybrid-electric propulsion systems. Stanford's SUAVE toolkit [25] is a conceptual-level aircraft design environment that uses physics-based methods to size novel configurations and unconventional propulsion architectures. Chakraborty et al. [26] developed an energy-based sizing framework named PEACE. The framework describes the disciplines in a functional form that allows for the disciplinary analysis to consist of tables, surrogates, physics-based solvers, or a combination thereof. The University of Michigan has a suite of tools for aircraft configuration optimization: MACH-Aero for aerodynamic shape optimization that uses pyGeo [27] for geometry parametrization and ADflow [28] for aerodynamics, TACS [29] (from Structural and Multidisciplinary Design Optimization group at Georgia Tech) for high-fidelity shell-based

stiffened composite structural optimization, UM/NAST [30] for dynamic nonlinear aeroservoelastic analysis, and OpenAeroStruct [31] for low-fidelity optimization using VLM and beam models. OpenConcept [32] is a new toolkit built on top of NASA Glenn's OpenMDAO framework for the conceptual design of aircraft to model and optimize aircraft with electric propulsion at a low computational cost. FAST-OAD [33] is another toolkit built on OpenMDAO for performing aircraft design. FAST-OAD allows easy switching between models for the same discipline, and also adding/removing/developing models to match the need of your study. AeroSandbox [34] is a package to design aircraft and uses automatic differentiation to obtain the derivatives necessary to optimize the aircraft.

### C. The CADDEE Aircraft Design Framework

The Comprehensive Aircraft high-Dimensional Design Environment (CADDEE) framework is being developed under a NASA ULI project led by UCSD. It is aimed at enabling rapid evaluation of aircraft concepts for a given mission. CADDEE is intended to be used as part of a nested loop. The 'outer-loop' represents the exploration over a low-dimensional space of discrete design parameters, while the 'inner-loop' represents the solution of a large-scale multidisciplinary design optimization (MDO) problem using CADDEE over a high-dimensional space of continuous design parameters. CADDEE encapsulates a holistic approach to the evaluation and optimization of aircraft concepts across a spectrum of mission segments, underpinned by three foundational concepts: a geometry-centric MDAO approach, modular data transfer, and automated derivative computations.

**Geometry-Centric Approach to MDAO.** CADDEE works with a high-fidelity B-spline representation of the aircraft's outer mold line (OML). The OML can be represented using any function space; however, B-splines are currently used in CADDEE because of their compact support and smoothness. During optimization, CADDEE effectively propagates changes in the geometry to the meshes used by physics-based solvers through a linear map.

**Modular Data Transfer.** CADDEE's modular data transfer is facilitated by the Solver-Independent Field Representation (SIFR). SIFR is a technique for unifying the representation of spatially-distributed quantities such as states, design variables, and other quantities of interest. This allows for a high level of modularity, as every solver interacts with a common, framework-level representation of all field quantities. Additionally, SIFR enables modular, invariant-conservative coupling by coupling each solver to the framework representation, rather than directly together.

This is achieved by treating field quantities in a similar manner to the geometry. Each sub-surface in the geometry acts as a map from its parametric space to physical space. This geometry information is then passed to solvers via a mesh of points in space. Similarly, each field quantity is represented by a union of maps from the aforementioned parametric spaces to its state-space. Then, this field information can be passed to solvers via a similar mesh of points in space. Each field can be updated by fitting its map to incoming data. This process is shown in Fig. 3.

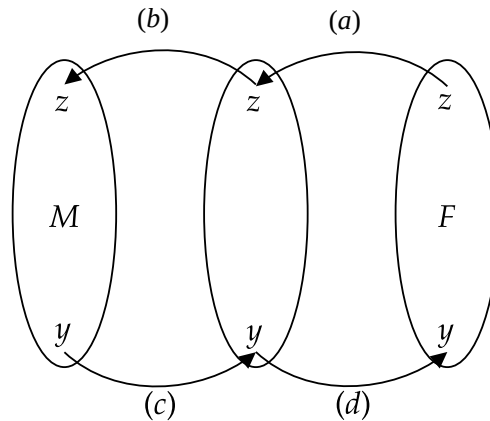


**Fig. 3** Illustration of the SIFR process between framework (F) and model (M) with input  $z$  and output  $y$ : (a) framework evaluation, (b) model input, (c) model output, (d) framework fitting

**Automated Derivative Computations.** A novel modeling approach based on a three-stage compiler is adopted (Figure 4). Using functional programming, users can write scripts as near-Python code. A new modeling language called CSDL (Computational System Design Language) [35] was recently created to enable this. A computational graph is automatically generated by the front end. The computational graph is modified to improve efficiency or add functionality (middle end), and it is then used to generate executable code (back end) that includes the sensitivity analysis model. Overall, this enables automated derivative computations of models/physics-based solvers. Various studies have demonstrated the efficacy of CSDL in large-scale MDAO for diverse applications, including the design of electric vertical takeoff and landing (eVTOL) vehicles [19], fixed-wing UAVs [36], trajectory optimization [37], and spacecraft design [38].

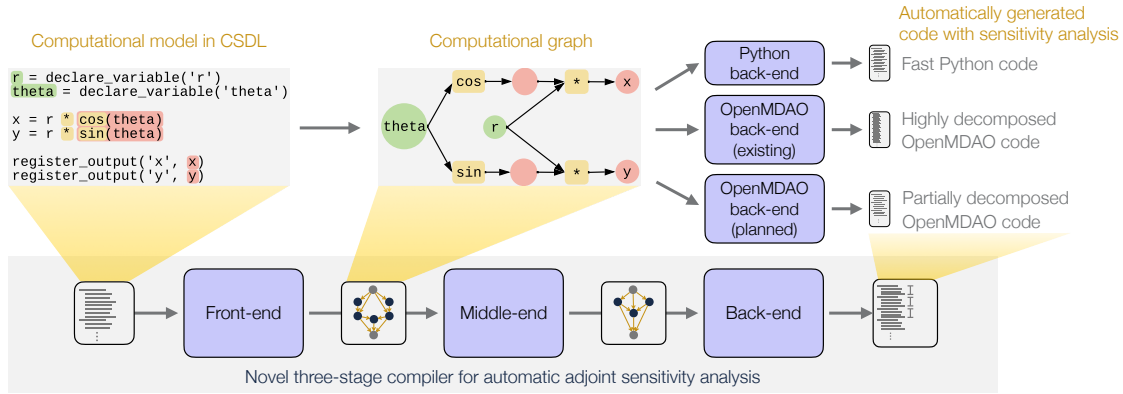


**Fig. 4 A modeling approach based on the idea of a three-stage compiler [35]**

CADDEE is able to model the entire aircraft across multiple disciplines and mission segments. Generally, there are three types of mission segments that can be modeled: trim-state, transient, and off-design. Trim-state segments are steady, on-design conditions such as hover, climb, and cruise and are typically easier to model as they are steady and do not size the aircraft. Transient conditions are generally unsteady (or quasi-steady) in nature and require time integration schemes. Examples are transition maneuvers or gust response dynamics, which are still considered on-design. Such cases may contribute to the aircraft sizing and may require more sophisticated physics-based simulations such as free-wake aerodynamic models and/or nonlinear dynamic structural models. Off-design conditions such as wing flutter, pull-up and push-down maneuvers, and one-engine-inoperative (OEI) scenarios fall under the last type of mission segment that can be modeled with CADDEE. These kinds of scenarios are unintended modes of operation and play a large role in sizing the aircraft structure and other components.

The concepts described above have been implemented in several packages along with physics-based models for disciplines relevant to UAM MDAO:

- **CSDL:** Facilitates model creation and automates derivative computations using the three-stage compiler.
- **M3L:** Implements the SIFR concept for modular data transfer.
- **CADDEE:** Specialized in aircraft design with a parametric geometry engine and comprehensive analysis API.
- **ModOpt:** Optimization package interfacing with optimizers like SLSQP and SNOPT.
- **VAST:** Vortex-based aerodynamic solver.
- **aframe:** Beam-based linear Euler–Bernoulli structural solver.
- **shell-analysis-fenicsx:** Shell-based Reissner–Mindlin structural solver.
- **PENGOLINS:** Shell-based isogeometric analysis (IGA) Kirchhoff–Love structural solver.
- **femo:** Framework for FEA using FEniCSx in PDE-constrained MDAO problems.
- **lsdo\_rotor:** BEMT-based propulsion solver.
- **lsdo\_motor:** ECM-based motor solver.
- **lsdo\_acoustics:** Solver for tonal and broadband noise.
- **ozone:** ODE time-marching solver.

### III. Methods for Modeling UAM during Conceptual Design

The conceptual design phase of UAM systems necessitates a balance between precision and efficiency. This section delves into the array of physics-based methods employed to compute critical performance metrics. A spectrum of modeling approaches, tailored to various design disciplines is explored. Each method undergoes a rigorous comparative analysis based on two criteria: physical accuracy and computational cost. Such a comparison facilitates a nuanced understanding of the trade-offs involved, guiding the selection of the most suitable method for accurately capturing the underlying physics of UAM systems, while also considering the pragmatic constraints of the design process.

#### A. Aerodynamics and Propulsion

Efficient aerodynamic design directly impacts the eVTOL aircraft's range, endurance, and payload capacity, which are critical for market adoption. Furthermore, the aerodynamics indirectly affect other important metrics such as acoustic footprint, aeroelastic behavior, and controllability. For this reason, it is critical that computational tools used in conceptual design capture the relevant aerodynamics.

##### 1. Aerodynamic performance metrics

A combination of performance parameters used in fixed-wing and rotary-wing circles are currently employed when analyzing eVTOL aircraft. These typically include force and moment coefficients. Additionally, aerodynamic efficiency is often described in terms of the lift to drag ratio which typically range from 1 to 6 for helicopters in forward flight. For fixed-wing aircraft these ratios are generally 3 to 4 times higher [39]. For rotors in hover, the figure of merit serves as a dimensionless measure of efficiency. This metric is defined as the ratio of the ideal power required to hover to the actual power required. To ensure meaningful comparisons, assessments should be conducted at similar disk loading conditions. A review of typical numbers for lift to drag ratio and disk loading for various configurations is provided by Kiesewetter et al. [8]. Johnson et al. discusses the aerodynamic performance of a few UAM configurations [40] while Erhard and Alonso has compared multiple propeller wake modelling techniques [41].

##### 2. Challenges for eVTOL aerodynamic modeling

UAM aircraft experience a broad range of flight conditions, from hover to wing-borne flight, including a transition regime that is not well-understood. This is further complicated by the presence of unsteady wake interactions between rotors and wings [42], which may prolong the stall envelope, increase the maximum lift coefficient [43–45], and significantly increase propulsive efficiency [46]. Electric ducted fans may simultaneously increase lift and decrease drag [47]. On the other hand, they may also cause significant loss of thrust in some configurations [48], and alter the space of feasible transition trajectories [49, 50]. Another challenge is determining the tradeoff between cruise and hover efficiency, since increasing the efficiency in one state typically decreases efficiency in the other. This tradeoff is greatly impacted by the cruise speed and duration of take-off and landing, making these critical factors in the economics of ride-sharing services [51]. Furthermore, as in general aviation, the ability to respond to gusts and robustness in diverse weather conditions significantly influences an aircraft's performance in UAM.

Simulating propeller-based aircraft poses a significant challenge, primarily stemming from large differences in the time scales in which the propeller and the entire aircraft operate at. The high rotational speed of the propellers dictates the computational timestep, while the distance convected by the wake or the duration of aircraft maneuvers determines the total simulation time. This combination results in long simulations that require a finely discretised time grid. Although established high-fidelity tools like mesh-based URANS and LES are capable of capturing the relevant physics, they are often prohibitively expensive for use in design optimization due to this reason [52]. Many low-cost modeling are commonly used for modeling fixed wing aircraft and helicopters, respectively; however, it is unclear whether each of these models capture the fidelity required for design of eVTOL aircraft, which are neither truly fixed-wing nor helicopters. A brief discussion on these modeling approaches are provided in the following sections.

##### 3. Computational models

**Blade Element Momentum Theory and Actuator Disk Models** Blade element momentum theory (BEMT) combines basic principles from both blade element theory which assumes blade sections to be quasi-2-D airfoils, and simple one-dimensional momentum theory applied along the propeller streamtube [39]. With the help of accurate airfoil characteristics, aerodynamic loading can be estimated along the radial and azimuthal directions in terms of blade twist, planform distribution, rotor angular speed, and freestream velocities. Correction factors are often incorporated to

account for blade tip-loss and compressibility. This method forms the basis of most modern propeller aerodynamic analyses due to its ease of implementation. Despite the simplicity of the physics underlying BEMT codes, they often give rise to substantial numerical challenges due to the instability of the nonlinear solvers employed in their convergence process. A modified formulation tailored to provide assured convergence has been developed and applied to UAM optimization [6, 53, 54]. There are also limitations to the regimes in which BEMT can be applied. For inclined and edgewise forward flight of the propeller, and in vortex ring state during the descent phase, large discrepancies may be observed. Linear inflow models and wake skew corrections proposed by the helicopter community may be applied to reduce inaccuracies in these regimes [55, 56]. Another limitation is that since the wake geometry is not obtained, the influence of the propeller on other aircraft components cannot be estimated. Actuator disk models provide some respite to these limitations by estimating wake velocities downstream of the propeller disk [57, 58]. Improvements to these models have also been proposed to account for propeller incidence angle and skewed flow [59–61].

**Dynamic Inflow Theory and Prescribed Wake Models** Dynamic inflow theory is a finite-state inflow modeling approach based on an unsteady actuator disk theory capable of handling transient maneuvers and varying flight conditions [62, 63]. While widely accepted as a robust and efficient approach for real-time helicopter simulation [64], it is yet to be applied to UAM modeling. Primarily used with rotors that have collective and cyclic pitch inputs, earlier dynamic inflow models were not capable of predicting flow velocity outside the rotor disk. However, these can also be used for rotors with only angular speed control recent improvements enable estimating wake velocities [65, 66]. It has the potential to improve UAM vehicle models especially for flight dynamics and handling quality studies.

In contrast, prescribed wake models, while computationally efficient, often sacrifice some accuracy by predefining wake characteristics. However, their practical advantages make them well-suited for initial design and performance predictions. Parameters like wake geometry, vorticity distribution, and induced velocities are prescribed based on simplified assumptions or empirical results, facilitating faster simulations for preliminary assessments [67]. They are often used as initial solutions to more involved modeling methods.

**Unsteady Vortex Lattice and Free Wake Methods** Moving beyond prescribed wake methods, the unsteady vortex lattice method allows the wake to deform freely, thereby enabling wake-wake and surface-wake interactions to be captured more accurately [68–70]. While several steady-state or quasi-steady solvers, such as AVL [71], VSPAero [72], and OpenAeroStruct [73] are popular, only a limited number of freely available solvers seem capable of handling unsteady flow, including dynamic motion. For modeling prop-wing interaction effects, the ability to prescribe kinematics is sufficient, but for advanced analysis such as trajectory optimization and controller design, the inclusion of flight dynamics is essential. Within the helicopter community, modeling the wake as a freely deforming vortex sheet is referred to as the free wake method [74, 75]. One issue that is observed while using these methods is the numerical instability of the helical rotor wake in hover; however, several advances have been made to better understand this [76–79]. Several free vortex implementations also roll up tip vortices to improve computational performance and mitigate such wake instabilities [80, 81].

AVL has been used to perform preliminary analysis on an eVTOL configuration; however, rotor-wing interactions were not modeled [82]. CAMRAD II, a commercial rotorcraft aeromechanics and dynamics solver, that utilizes the free-wake method has been used in simulations with high-order CFD solutions on both a quadrotor and a side-by-side rotor UAM configuration [83, 84] but CAMRAD II was primarily utilized for modeling the flight dynamics through a loose-coupling approach in these studies. The effect of vertical separation of fore and aft rotors on power reduction, effect of rotor overlap, and trim solutions were investigated. The acoustic signature of a UAM configuration has been assessed using an unsteady vortex lattice approach [85]. Additionally, approaches like the constant vorticity contour (CVC) method, as utilized in the CHARM solver, have also been employed for fast and accurate modeling of interactional aerodynamics in UAM aircraft [86].

**Vortex Particle Method** The vortex particle method (VPM) discretizes the vorticity field using Lagrangian radial basis functions (the use of radial basis functions is not fundamental to the method, it's one choice), which convect according to the Navier-Stokes equation [87, 88]. It is capable of orders of magnitude decrease in computational cost over mesh-based CFD for comparable fidelity when vorticity is concentrated in a relatively small portion of the flow [89]. It has been shown to experience less numerical dissipation due to vorticity than mesh-based CFD, making it well-suited for modeling wakes [90]. Because VPM expresses the Navier-Stokes equation, it is not surprising that it has been used to model unsteady maneuvers [91], ground effect [92], bluff bodies [93], and even turbulent flows [89], making it a versatile modeling approach.

The Airbus Vahana geometry has been analyzed by several researchers using a vortex particle approach [94–96]. Open-source mid-fidelity aerodynamic tools that utilize the vortex particle method to simulate eVTOL aircraft have been developed by multiple researchers [97, 98]. Typically, these include an additional solver (e.g. lifting line, vortex lattice, actuator line, panel method) to model surfaces, and vortex particles are shed to form the wake.

**Computational Fluid Dynamics** High-fidelity CFD has been used sparingly for UAM analysis owing to large simulation times and meshing effort involved. The overset grid approach stands out as a widely adopted strategy for complex configurations like those seen in the UAM domain. Several small to medium-sized drones and larger UAM configurations have been studied using NASA’s OVERFLOW solver to better understand the effect of under and over-mounting the propeller, and the effect of gust loads [99]. A Quadrotor and Tilt-rotor configuration was analysed using DLR’s FLOWer structured CFD solver [100]. The Rolls-Royce tiltwing eVTOL concept was simulated in hover and forward flight using a RANS-based solver, HMB3 to study the effect of shifting the phase angle between propellers, a method called synchrophasing, on acoustic emissions [101]. Investigations have also been conducted on coupling CFD to simple flight dynamics models to study the effect of tall buildings on small aircraft, effect of gust-induced response, simulate take-off and control’ dutch-roll oscillations in eVTOL aircraft [102–105].

**Hybrid Methods** A few hybrid methods exist that leverage the efficiency of vortex-based Lagrangian or actuator disk methods with the high fidelity offered by classic CFD approaches. RotCFD, a commercial solver that models the rotor using a distribution of momentum sources and couples with an unsteady RANS solver has been used to compare against experimental results from a multi-rotor testbed configuration and was shown to be sufficiently accurate in predicting rotor thrust and torque coefficients [106]. The momentum source coupling approach appears to be referred to the body-force coupling approach in some research literature and have been applied to a distributed propulsion blended wing concept [107], distributed propeller configuration [108] and a propeller wing interaction study [109]. The vortex particle method has also been coupled to high-fidelity CFD; however, it has not been utilized for UAM applications [110].

#### 4. Results, Validation, and Verification

**Fixed-wing** The wing and tail geometry provided in Appendix A were modeled in VAST and FLOWUnsteady and the lift and drag were compared against results from AVL. While VAST is a vortex lattice-based solver similar to AVL, FLOWUnsteady utilizes a reformulated vortex particle-based approach. The geometry were modeled with around 30 spanwise panels on either side, and simulations were run till convergence of both lift and drag for the unsteady solution generated using FLOWUnsteady. A timestep corresponding to  $V_\infty \Delta t / c = 1/16$  was used, where  $c$  is the main wing aerodynamic chord. For computing the lift and drag coefficients, the reference area of the main wing geometry was used except for the isolated horizontal stabilizer case where its own planform area was used. All solvers showed good agreement and results for the isolated components and wing-stabilizer combination are depicted in Figs. 5 to 7. The error bars depicted in these plots are at 5% of the results from AVL. The wake solution and particle field generated using FLOWUnsteady for the wing-tail combination is shown in Fig. 8. The vortex particles are represented using their corresponding circulation strength vectors.

**Propellers** The lift and pusher propeller geometry provided in Appendix A where simulated using two blade element momentum theory based solvers, LSDO Rotor and CCBlade. These propeller geometry were also simulated in FLOWUnsteady. The lift propeller was simulated in hover at an angular velocity of 2800 rpm. An azimuthal timestep of  $10^\circ$  was used to simulate the rotor for 10 revolutions around which convergence of thrust and torque were observed. Airfoil lookup tables generated using XFOIL were used in all solvers. Variation in the Reynolds number along the blade was not significant; however, a Prandtl-Glauert Mach number correction was found necessary and was applied to the airfoil aerodynamic data. The converged thrust and torque coefficients observed from these simulations are shown in Table 1 and the blade loading is depicted in Fig. 9a. The thrust and torque coefficients were computed according to helicopter conventions given by Eqs. (1) and (2).

$$\text{Thrust coefficient, } C_T = \frac{T}{\rho \pi R^2 (R\Omega)^2} \quad (1)$$

$$\text{Torque coefficient, } C_Q = \frac{Q}{\rho \pi R^3 (R\Omega)^2} \quad (2)$$

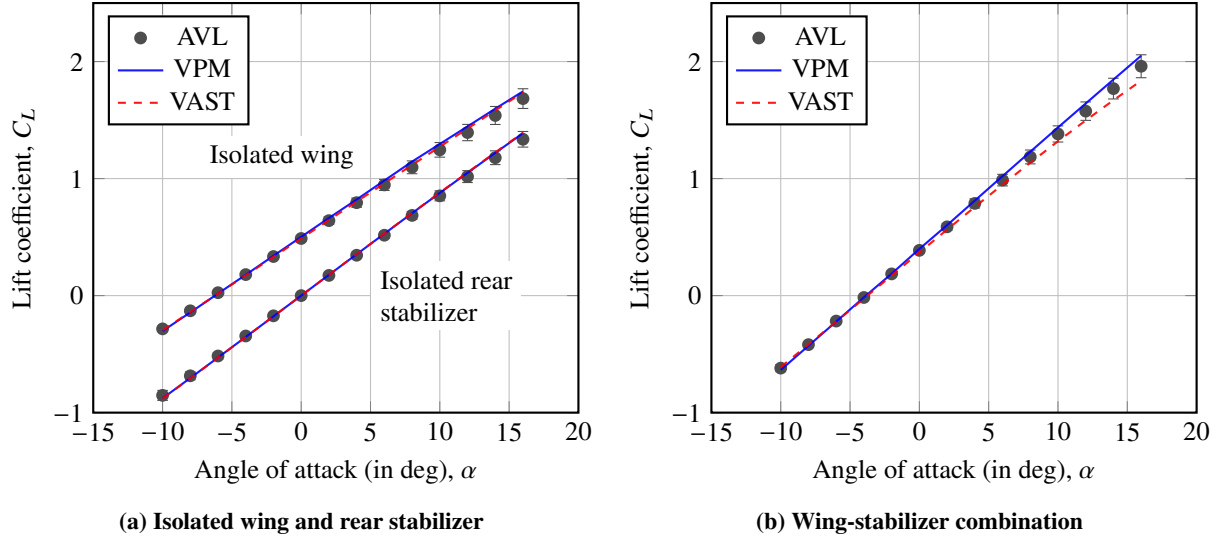


Fig. 5 Lift characteristics of wing and horizontal stabilizer

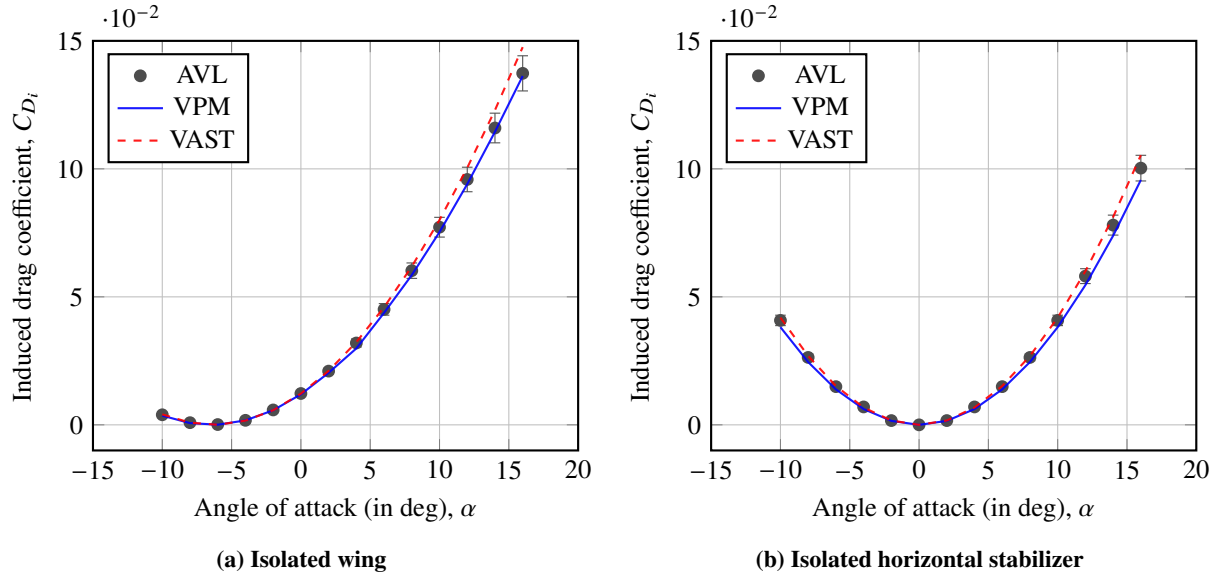


Fig. 6 Induced drag characteristics of isolated wing and rear stabilizer

Table 1 Lift propeller performance

| Solver            | Thrust Coeff. | Torque Coeff. |
|-------------------|---------------|---------------|
|                   | $C_T$         | $C_Q$         |
| <b>VPM</b>        | 0.00749       | 0.00125       |
| <b>CCBlade</b>    | 0.00881       | 0.00067       |
| <b>LSDO ROTOR</b> | 0.00865       | 0.00066       |

Table 2 Pusher propeller performance

| Solver            | Thrust Coeff. | Torque Coeff. |
|-------------------|---------------|---------------|
|                   | $C_T$         | $C_Q$         |
| <b>VPM</b>        | 0.08154       | 0.01025       |
| <b>CCBlade</b>    | 0.08058       | 0.00942       |
| <b>LSDO ROTOR</b> | 0.08153       | 0.00971       |

A rotor in hover is prone to wake instabilities when modeled using vortex-based methods as a freestream velocity is not present and the wake geometry is determined and convected away by self-induced velocities. The reformulated vortex particle approach in FLOWUnsteady presents a method to generate a stable wake [98, 111]. However, similar

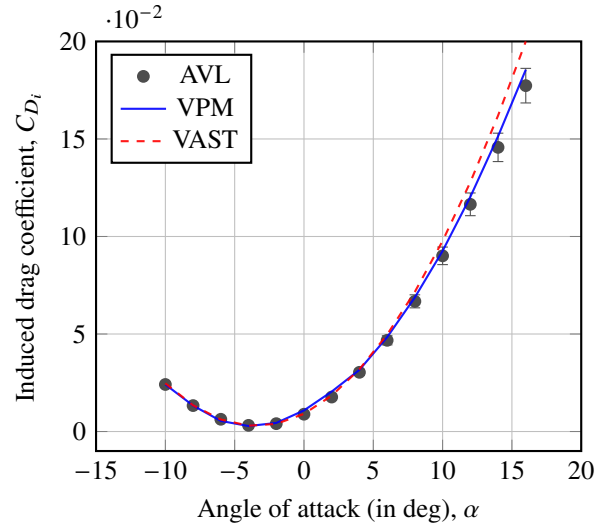


**Fig. 7 Induced drag characteristics of wing and rear stabilizer combination**

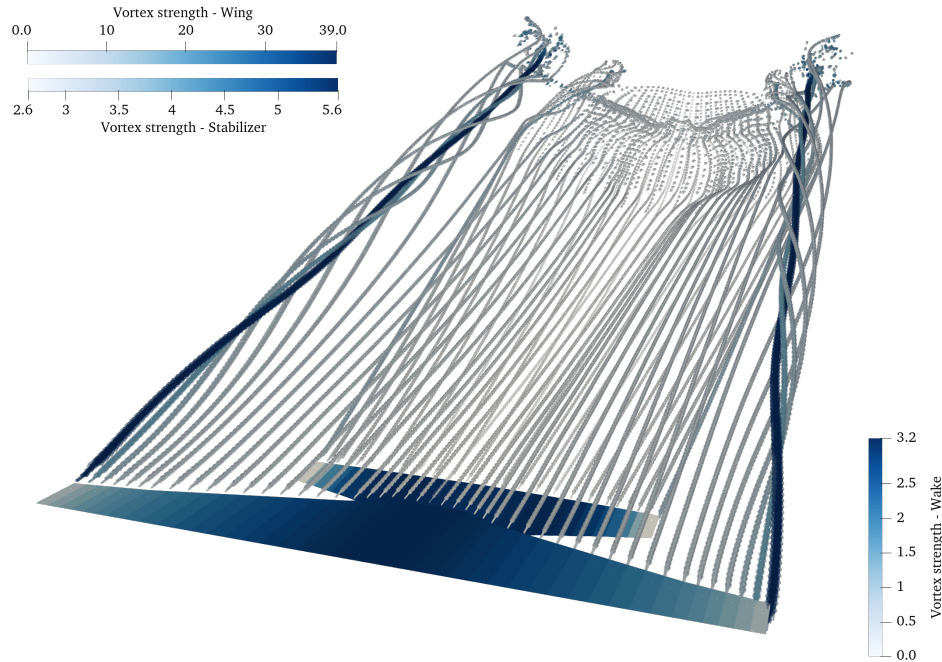


**Fig. 8 Wake solution generated using FLOWUnsteady for the wing-stabilizer combination**

to other actuator line implementations, a tip-loss correction has to be incorporated for accurate results. A modified Prandtl's tip-loss correction was used on the lift rotor and a comparison of the sectional loading with and without the tip-loss correction is shown in Fig. 11. Although the sectional loading is close to that produced by BEMT, the tip-loss factor is largely arbitrary and has to be calibrated using experimental results. Additionally, although the sectional loading produced by both BEMT solvers closely agree, it would be incorrect to assume that the BEMT solutions are closer to experimental results compared to those generated by the VPM solver.

The pusher propeller was also modeled using the BEMT and VPM solvers. Geometry and time discretisations were identical to that of the lift propeller. Figure 9b depicts a comparison of sectional loading between the different solvers and Table 2 shows the converged thrust and torque coefficients reported using propeller conventions given by Eqs. (3) and (4).  $\rho$  is the density,  $D$  the rotor diameter and  $n$  the rotor angular velocity in rotations per second. Unlike the lift

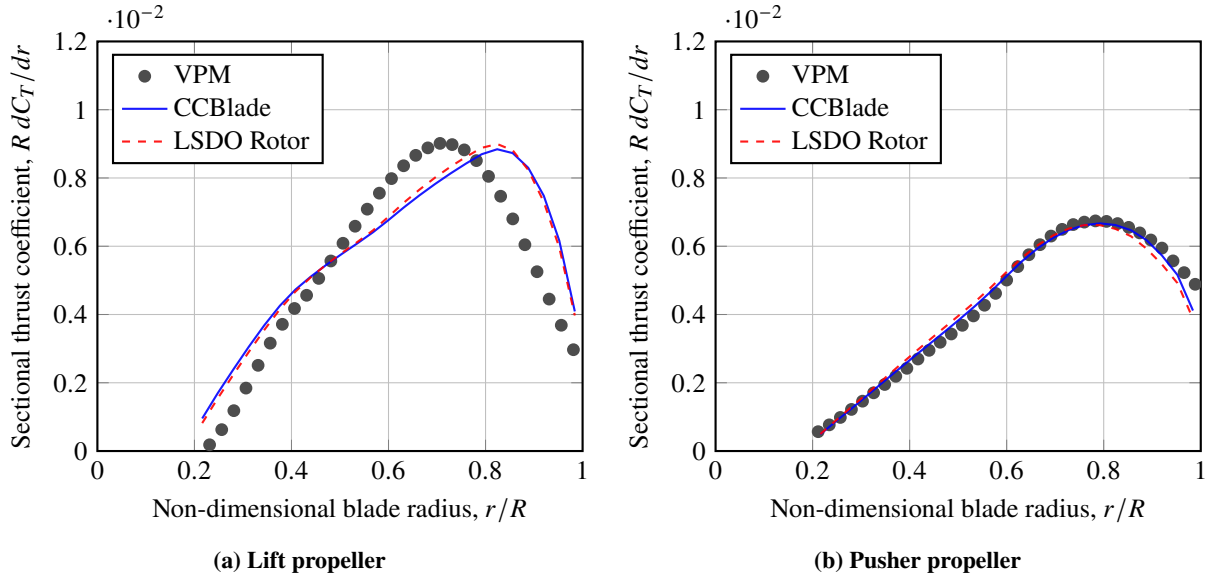


**Fig. 9** Sectional thrust characteristics of lift and pusher propellers

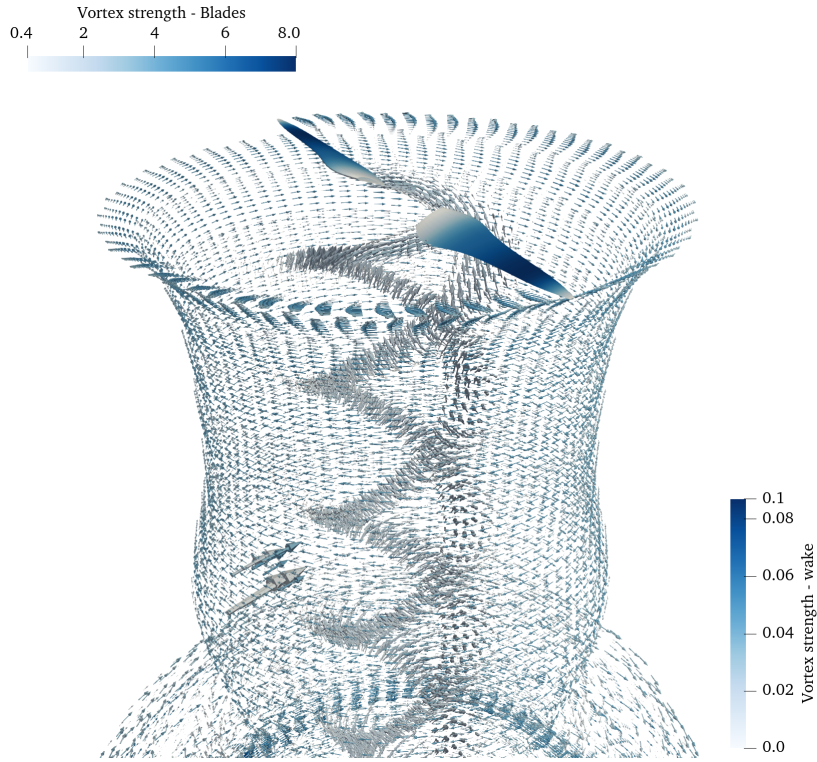


**Fig. 10** Wake solution generated using FLOWUnsteady for the lift propeller in hover

propeller in hover, a tip-loss correction factor was not required and very close agreement was obtained with BEMT solutions regardless. In forward flight, since the dominant influence is from the freestream, the wake is readily convected away and a wake instability does not develop near the rotor disk.

$$\text{Thrust coefficient, } C_T = \frac{T}{\rho D^4 n^2} \quad (3)$$

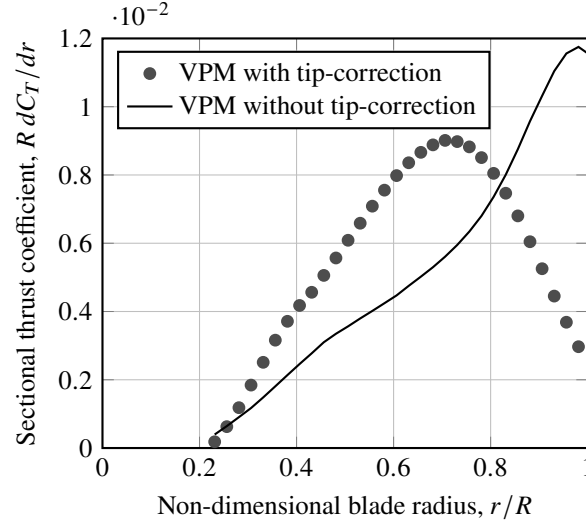


**Fig. 11** Sectional thrust with and without tip correction generated using the vortex particle approach

$$\text{Torque coefficient, } C_Q = \frac{Q}{\rho D^5 n^2} \quad (4)$$

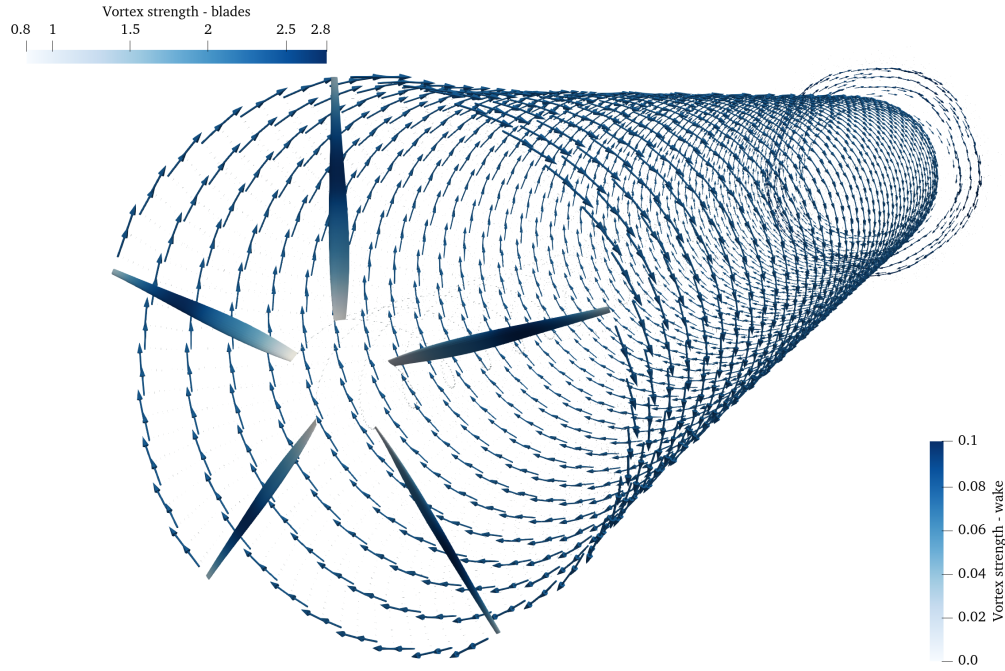


**Fig. 12** Wake solution generated using FLOWUnsteady for the push propeller at an advance ratio of 0.62

While blade element momentum theory, vortex lattice, and vortex particle approaches are valuable tools for analyzing UAM aircraft performance, it is noteworthy that the computational time required by the VPM approach is considerably higher compared to low-order methods like BEMT. BEMT solutions are nearly instantaneous compared to propeller solutions generated using FLOWUnsteady, which took around 30 minutes. This difference in computational times stems from the Biot-Savart computations required for evaluating the influence of the vortex particle field. In FLOWUnsteady, FMM (Fast Multipole Method) acceleration techniques have been utilized to mitigate the computational burden and reduce the order of time complexity from  $O(n^2)$  to  $O(n)$ . For instance, in the case of the wing-stabilizer combination,

VAST, implemented in Python, took around 10 minutes, whereas FLOWUnsteady, coded in Julia, only required 8 minutes of run time to convect three span lengths of the wake. The dissimilarity in the programming language and utilization of FMM enable faster solutions for FLOWUnsteady. In scenarios where complex wake interactions between different aircraft components are involved, the vortex particle method excels. On the other hand, high-fidelity approaches like CFD take an order of magnitude higher computational time compared to the VPM approach.

## B. Structures

Accurate structural weight estimation plays a crucial role in supporting the advancement of novel UAMs. Early-stage design requires reliable estimates of the structural weight to inform decision-making processes and optimize vehicle performance. Traditional approaches, such as historical regression methods, are no longer sufficient due to the unique characteristics of these vehicles. Therefore, the adoption of physics-based methods becomes imperative for accurate structural weight estimation.

Structural weight estimation is performed using a ‘distributed optimization’ process (Fig. 13): 1) dynamic loads are first computed; 2) then, the dynamic loads are converted into a set of critical static load cases, and 3) finally, the structure is sized to sustain those loads [112].

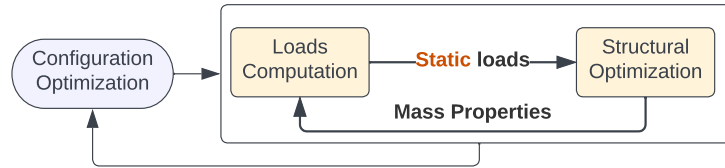


**Fig. 13 Distributed optimization for structural weight estimation**

**Computation of dynamic loads.** Load conditions from both 14-CFR Part 23 and Part 25 affect various components of the UAM structure. An incomplete list of load conditions is given in Table 3. These include the static 3g pull-up and -1g push-down maneuvers commonly used to size aircraft. Note, that for aircraft sized by Part 25, 2.5g is used instead of 3g. The dynamic conditions include gusts and maneuvers: checked-pitch maneuver [113], rudder-kick maneuver [114], and roll maneuver. Accurately executing these maneuvers to mimic the actions of a pilot performing a test flight requires the consideration of a closed-loop control system and aeroelasticity [115].

**Table 3 An incomplete list of load conditions to be considered during structural sizing of UAM concepts**

| Load condition         | Mission Segment   | Load factor | Static/Dynamic | Expected Impact on |
|------------------------|-------------------|-------------|----------------|--------------------|
| Pull-up maneuver       | Cruise            | 3g          | Static         | Wing               |
| Push-down maneuver     | Cruise            | -1g         | Static         | Wing               |
| Gust                   | Cruise            | *           | Dynamic        | Wing               |
| Roll maneuver          | Cruise            | *           | Dynamic        | Wing               |
| Flutter                | High-speed cruise | *           | Dynamic        | Wing               |
| Maximum thrust         | Transition        | 2g          | Static         | Rotor              |
| One-engine inoperative | Hover             | 2g          | Static         | Rotor              |
| One-engine inoperative | Transition        | 1.15g       | Static         | Booms              |
| Checked-pitch maneuver | Cruise            | 3g & 0g     | Dynamic        | Horizontal tail    |
| Rudder-kick maneuver   | Cruise            | *           | Dynamic        | Vertical tail      |
| Hard landing           | Descent           | *           | Static         | Fuselage           |
| Downward crash         | Descent           | 12g         | Static         | Fuselage           |
| Forward crash          | Descent           | 12g         | Static         | Fuselage           |

The simulation involves a tight coupling between aerodynamics, structural dynamics, flight mechanics, and controls; together termed aeroservoelasticity. Each simulation must have a reasonable computation time in order for it to be practical in many-query applications such as optimization. The time-domain nature of this analysis makes directly using high-fidelity tools computationally intractable.

The aerodynamic analysis is usually handled in one of two ways: 1) unsteady vortex lattice methods (UVLM) [116] that are fast but inaccurate at drag prediction and not valid at transonic speeds, or 2) reduced order models (ROMs) of CFD analysis [117, 118]. The latter is an attractive option to replace the high-fidelity tool with a fast-to-run model. However, current approaches for generating ROMs are only computationally feasible for a small number of design variables.

On the structural side, it is common to use modal analysis—a frequency-domain approach that represents the structural deformation using a set of orthogonal modes [119]. Modal analysis, however, linearizes the problem and thus is not valid for highly flexible wings undergoing large deformations. Another approach is the use of beam models to represent the aircraft, the so-called stick model approach [120]. While nonlinear beam models [121] can capture large deformations, they struggle to accurately account for the geometric complexities seen on modern aircraft wing boxes as well as material nonlinearities. Recent work has also seen the use of multi-fidelity ROMs to perform structural analysis [122] and lower the computational cost while retaining the accuracy of shell models.

**Computation of equivalent static loads.** The process of converting the dynamic loads into equivalent static loads typically leads to a large number of load cases. Conventional tube-and-wing aircraft design methods have matured to the extent that thousands of load cases can be systematically reduced to a smaller set of critical load cases [123, 124]. However, for novel aircraft concepts and configurations, such heuristic, albeit systematic reductions, may no longer be valid. Therefore, methods applicable in the early design must account for dynamics explicitly without relying on *ad hoc* conversions to equivalent static load cases.

**Structural sizing to sustain loads.** Structural sizing involves 1) a model to evaluate failure margins on the structure when subjected to dynamic loads, and 2) optimization methods to determine the material and thicknesses of the components to withstand the loads.

The most widely used method for structural analysis and to determine failure margins is to represent the wingbox as a shell model, which has a large number of design variables, e.g., thicknesses, and composite ply orientations. Structural sizing can be efficiently performed using gradient-based optimization along with the adjoint method to efficiently compute the gradients [29]. TACS [125] is one such tool for structural sizing. However, dynamic simulations with a shell model are computationally intractable for many-query applications such as optimization. Hence, contemporary approaches mostly focus on the use of the adjoint method in shell model structural sizing by converting the dynamic loads into a set of critical static loads, as explained earlier.

Recently, there has been interest in using beam models for structural sizing. Structural sizing and optimization of the wingbox by representing it as a beam has been demonstrated by Takahashi and Lemonds [126]. OpenAeroStruct [73, 127], an open-source Python tool, has been shown to provide reasonable weight estimates at the early stages of aircraft design. Sarojini and Mavris [128] sized the PEGASUS concept wingbox subjected to static maneuver conditions and dynamic gust loads. They considered strength-based sizing and compared the results to a shell model and showed a 6% error in weight estimate but with a 7.6x speed-up in computational efficiency. Solano et al. [129] extended the work of Sarojini and Mavris to include buckling constraints and sized the PEGASUS concept as well as the truss-braced wing concept. It should be noted that the references cited earlier used the Euler–Bernoulli beam theory to compute the stresses and failure criteria during the structural sizing process.

In summary, the structures discipline sizes the internal structure to sustain the worst-case loads the aircraft might experience during its operation. Typical input parameters and output metrics are given in Table 4.

### 1. Computational Models

**Femap Shell Model** As part of the Verification and Validation (V&V) effort for the beam and shell models developed by UCSD/LSDO, a shell mesh is created using the industry FEA software Siemens Femap with post-processor Nastran. The Femap shell model is derived from imported surfaces using the OpenVSP wing model created to replicate the PAV prototype. The shell mesh uses hexagonal plate (pshell) elements to represent the midsurfaces of the upper and lower skins, spars, and ribs for the wing. The Femap pshell elements are formulated based on the Mindlin-Reissner shell theory and are read as CQUAD4 elements in the Nastran post-processor. Fixed boundary conditions are placed at all

**Table 4 Design parameters and output metrics relevant for structures disciplines**

| Parameters        |                 | Metrics              |                        |
|-------------------|-----------------|----------------------|------------------------|
| OML               | Material        | Displacement         | Mass                   |
| Load conditions   | Composite layup | Stress               | Center of gravity      |
| Stiffener concept | Thickness       | Strain               | Mass moment of inertia |
| Structural layout |                 | Panel buckling ratio |                        |

the nodes at the root of the wing, material properties, and thickness are applied to the plate elements. Pressure loads representing the 3g lift load case are then applied to the face of the lower skin plate elements. The model is then run through Nastran Linear Static Solution 101 to generate an output of the stresses and displacements of the plate elements and nodes to compare these values to the other computational models.

**Isogeometric Kirchhoff–Love Shells** The Kirchhoff–Love shell model [130] has gained increasing attention since the introduction of isogeometric analysis (IGA). IGA bridges the descriptions between computer-aided design (CAD) models and analysis models by employing non-uniform rational B-spline (NURBS) as basis functions. This approach entirely bypasses finite element mesh generation in the analysis due to the unified description. Additionally, NURBS basis functions inherently satisfy the  $C^1$  continuity requirement of the fourth-order partial differential equation (PDE) governing the Kirchhoff–Love shell formulation. To enable direct analysis of aircraft CAD geometries composed of multiple B-spline surfaces, we adopt a penalty-based shell coupling formulation [131] to maintain the displacement and rotational continuity between different shell patches. This functionality is implemented in an open-source framework PENGOLINS, which streamlines the design-through-analysis workflow [132]. Aircraft CAD models in IGES or STEP formats can be imported into PENGOLINS and used for structural analysis without finite element mesh generation. During the analysis, topologically 1D quadrature meshes are created to integrate penalty terms for shell coupling. Consequently, accurate analysis results depend on the presence of surface intersections in the CAD geometry. Leveraging the automated derivative computation capabilities in FEniCS [133], PENGOLINS can be applied to gradient-based multidisciplinary design optimization of complex shell structures. Nodal aerodynamics loads are applied to the shell mesh through the (non-conservative) load projection approach that is outlined in [134]. Inverse distance weighting is used to project the nodal loads to linear basis functions centered around the shell mesh vertices. This leads to a distributed continuous load of identical magnitude.

**Reissner–Mindlin Shell Model** The Reissner–Mindlin (R–M) shell model adopts a geometrically linear, quadrilateral-element variant of the locking-free shell formulation by Camplello et al. [135]. It considers transverse shear energy, which is typically ignored in Kirchhoff–Love shell formulation. The solution of the shell solver contains 6 parameters per node—3 for displacements and 3 for rotations—and is relatively more accurate for thick shells compared to Kirchhoff–Love shell formulation [136]. We have implemented the R–M shell solver in FEniCSx [137], the next generation of FEniCS, which offers robust support for quadrilateral elements. Similar to the isogeometric K–L shell solver, the R–M shell solver can provide analytical derivatives by leveraging FEniCSx’s automated differentiation capability. We encapsulate the R–M shell solver as a CSDL sub-model using FEMO [138], a recent work integrating FEniCSx and CSDL for PDE-constrained optimization. Notably, a limitation of the R–M solver, common to other existing shell solvers based on classic finite element theory such as Femap, is its dependence on an additional mesh generation step compared to the isogeometric shell solver. Furthermore, due to a constraint in the FEniCSx mesh importing module, this shell solver can only handle meshes comprising pure triangular or quadrilateral elements. In other words, it presently cannot manage meshes containing multiple types of elements, i.e., meshes containing both triangular and quadrilateral elements, which is not an issue for Femap shell solver. In this study, we initiate the shell analysis workflow with aircraft CAD models, and create geometry for additional internal structures in CADDEE, and generate quadrilateral meshes with Gmsh [139], an open-source finite element mesh generator with Python interface.

**Variational Asymptotic Method (VAM)-based Higher Order Beam Model** In the structural analysis of aircraft wingboxes, the Variational Asymptotic Method (VAM) offers a sophisticated and efficient approach, particularly when applied to beam representations. This method splits the analysis into two distinct yet interrelated components:

cross-section analysis and one-dimensional (1-D) beam analysis. Utilizing the Variational Asymptotic Beam Sectional Analysis (VABS) tool, a commercial implementation based on VAM principles [140, 141], we conduct cross-section analysis. This analysis necessitates detailed input on the cross-section, including its geometric configuration represented as a two-dimensional (2-D) mesh, material properties and distribution, and the cross-section's orientation and curvature relative to the beam line. The versatility of VABS allows for accommodating complex shapes such as airfoil curvatures and the use of anisotropic materials, making it highly suitable for composite material analysis. A comprehensive description of input requirements for VABS is elaborated by Yu [142]. Upon solving, VABS yields the sectional elastic and inertia properties in the form of  $6 \times 6$  stiffness and mass matrices, which are then used in the 1-D beam analysis.

**Nonlinear Timoshenko Beam Model** The nonlinear Timoshenko beam model is implemented here in order to accurately capture large deflections and shear deformation of slender aircraft structures. The implementation is a time-domain one and thereby can predict dynamic responses. The nonlinear system comprises force/moment equilibrium equations, stress/strain equations, kinematic equations, and boundary conditions (similar to the implementation by Drela [143]) which are combined to create a differential algebraic equation (DAE) system. The solution scheme uses a backward difference formula (BDF) for time-stepping and Newton's method to solve the nonlinear system at each time step. This formulation has been shown to be accurate in structural weight estimation of aircraft wingboxes [129]. When deflections are likely to be small and dynamic analysis is not needed, further simplifications can be made to use the linear beam model described below.

**Linear Euler–Bernoulli Beam Model** The linear beam model extracts one-dimensional meshes from the top-level aircraft geometry which represent various slender components such as the wing or the fuselage. We assign cross-sectional properties to each element in these meshes and then solve the resulting system for the elemental stresses and displacements. The system is represented using linear Euler–Bernoulli beam theory with a global stiffness matrix  $K$  that is constructed with standard  $12 \times 12$  frame stiffness elements. We then solve the system  $F = KU$  for the displacements  $U$ . To simplify the structural analysis, we approximate the wing-box structure as a hollow rectangular box-beam similar to the analysis performed by Chauhan and Martins [144] and by Sarojini [145]. The width and height of the beams are extracted from the wing meshes as ratios of the chord and the wing thickness. By combining the aerodynamic, inertial, and propulsive loads we create the stiffness matrix  $F$  which completes the linear system. For this the nodal aerodynamic loads can be applied directly to the beam nodes.

**Femap Beam Model** Femap was used as an industry tool as part of the Verification and Validation effort. Femap uses Timoshenko beam theory and conducts a linear static analysis. The model was created with beam elements with material properties and cross-sectional geometry applied at both ends. A fixed boundary condition is placed at the single node at the root of the beam. Ten concentrated point loads are applied at each node representing the lift distribution provided by UCSD. The model is run through Nastran Linear Static Solution 101 that generates an output of von Mises stresses, bending moments, and displacements at all the nodes.

## 2. Comparison of Computational Models

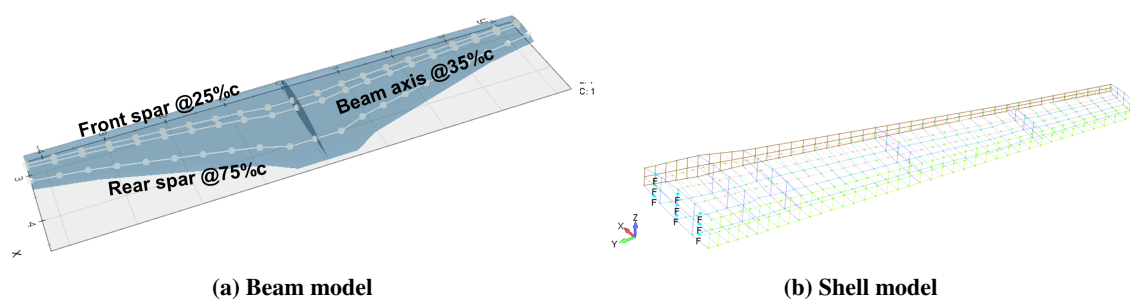


**Fig. 14 PAV structural models**

This study considers only the primary structure inside the wing: the wingbox. From the PAV prototype OpenVSP model, a wingbox is created. The front spar is located at 25% chord and the rear spar at 75% chord. Six ribs are placed:

**Table 5 Overview of structural tools**

| Tool                           | Geometric Assumption | Theory                              |
|--------------------------------|----------------------|-------------------------------------|
| <b>Beam</b>                    |                      |                                     |
| Femap                          | Linear               | Timoshenko                          |
| Aurora Flight Sciences' MATLAB | Linear               | Euler–Bernoulli                     |
| aframe                         | Linear               | Euler–Bernoulli                     |
| FROOTBAT                       | Linear               | Timoshenko                          |
| SWIFT                          | Nonlinear            | Timoshenko                          |
| VABS                           | Nonlinear            | Variational Asymptotic Method (VAM) |
| <b>Shell</b>                   |                      |                                     |
| Femap                          | Linear               | Reissner–Mindlin                    |
| PENGoLINS                      | Linear               | Kirchhoff–Love                      |
| shell_analysis_fenicsx         | Linear               | Reissner–Mindlin                    |

one at the root, one at the tip, and four at each location where the taper of the wing changes. Fixed boundary conditions are applied at the root rib. The shell and beam models are seen in Fig. 14. This study assumes an unstiffened metallic wingbox. The assumption is due to the limitations of the computational models available at UCSD/LSDO and will be relaxed in future work. Properties of aircraft-grade aluminum are assumed:  $E = 7.31 \times 10^{10}$  Pa,  $G = 26 \times 10^9$  Pa,  $\nu = 0.33$ , and  $\rho = 2768$  kg/m<sup>3</sup>. All panels (front spar, rear spar, top skin, bottom skin, and rib) are given a uniform thickness of 0.0013 m (0.05 in). The structure is subjected to a 3g load condition at cruise ( $h = 600$  ft AGL and  $Ma = 0.1474$ ). Table 19 in Appendix A provides all the geometric and loads data to replicate the beam model. Table 5 provides a list of the tools considered in this study along with the assumptions and links to the GitHub repositories (where applicable).

Figure 15a presents the displacement along the spanwise location. Among these, the Femap shell model is the highest fidelity and the industry standard tool, exhibiting the highest displacement predictions and suggesting a greater accuracy due to its comprehensive approach in capturing the complexities of the wing structure. The Femap beam model operates under the assumption of linear Timoshenko beam theory with a box-beam cross-section, which the graph suggests results in a conservative estimation of displacements. This discrepancy points towards an inherent limitation of the box-beam assumption, which does not conform to the actual geometric characteristics of the eVTOL wingbox. The assumption made in the height of the box beam likely lead to an overestimation of stiffness, and consequently, an underestimation of the displacements that would occur under real-world loading conditions. Furthermore, the box-beam models, including those based on linear Euler–Bernoulli and Timoshenko beam theories, as well as their nonlinear Timoshenko counterpart, consistently predict lower displacements than the Femap shell model. This pattern underscores the criticality of model selection in structural analysis, where using oversimplified assumptions can result in designs that may not withstand actual operational stresses. A standout model in the comparison employs the Variational Asymptotic Method (VAM), which does not rely on a box-beam cross-sectional assumption. Instead, VAM is capable of accurately capturing the curvature of the airfoil, a feature that significantly affects the load-bearing and deformation characteristics of the wingbox. The close alignment of the VAM's predictions with the Femap shell model's outcomes underscores its potential for high-fidelity structural analysis in eVTOL applications. The analysis also highlights the importance of considering nonlinearities in structural behavior. The nonlinear models predict higher displacements than their linear counterparts, indicating that linear analysis might be insufficient for capturing the complex stress and strain relationships in eVTOL wing structures, especially under high load or during dynamic maneuvers.

In Figure 15b, which shows the stress distribution along the spanwise location of the wingbox, the Femap shell model, regarded as the industry standard, predicts higher stress values towards the outboard sections of the wing when compared to the beam models, which include the Femap beam and box-beam models employing linear and nonlinear theories. The initial observations reveal a good agreement in stress predictions near the wing root across all models. However, as the analysis progresses toward the outboard sections, a clear discrepancy emerges. The Femap shell model's predictions are consistently higher than those of the beam models, indicating that the beam models may not

**Table 6 Comparison of maximum displacement and von Mises stress from different shell solvers.**

| Solvers                   | Femap               | shell_analysis_fenicsx | PENGoLINS           |
|---------------------------|---------------------|------------------------|---------------------|
| Max displacement ( $m$ )  | 0.0479              | 0.0466                 | 0.0459              |
| Max vM stress ( $N/m^2$ ) | $1.156 \times 10^8$ | $1.097 \times 10^8$    | $1.151 \times 10^8$ |

be capturing the complexity of the stress states as the distance from the root increases. The trend suggests that the simplified assumptions inherent in the beam models — which might not fully account for factors such as torsion, shear distribution, and the wing's changing cross-sectional properties — become increasingly inadequate as one analyzes sections further away from the root. The implications of this finding are significant for the design and safety of eVTOL wing structures. The underestimation of stress by the beam models in the outboard wing sections could result in the under-designing of these critical areas, potentially compromising the wing's ability to withstand operational loads. This disparity underlines the necessity of employing more sophisticated modeling techniques, like the Femap shell model, for a thorough assessment of the wing's structural behavior. We can observe that the Femap beam and the box-beam with the nonlinear Timoshenko assumption have almost identical stress profiles across the span. On the other hand, the implementation of the box-beam with the linear Euler–Bernoulli assumption shows a slightly different stress distribution. However, the authors believe this is an artifact of the implementation. Another implementation using the same box-beam Euler–Bernoulli assumption captures the drop in stress at the location where the sweep of the beam changes.

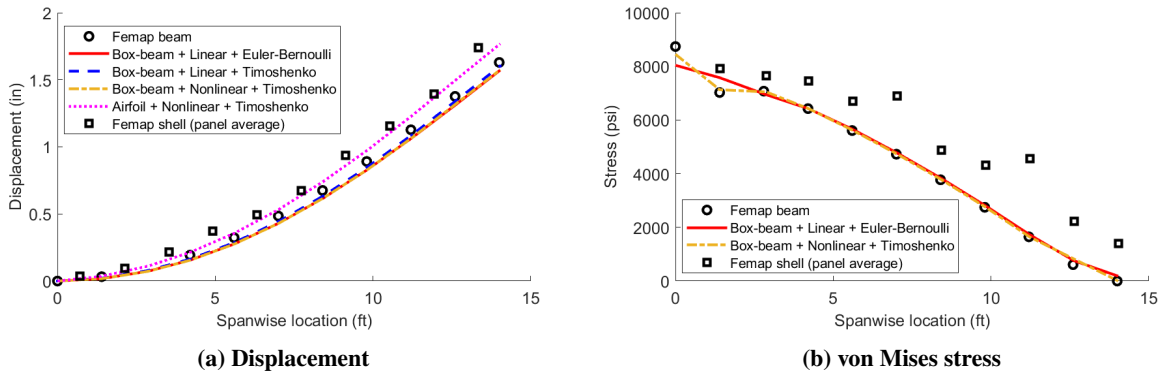
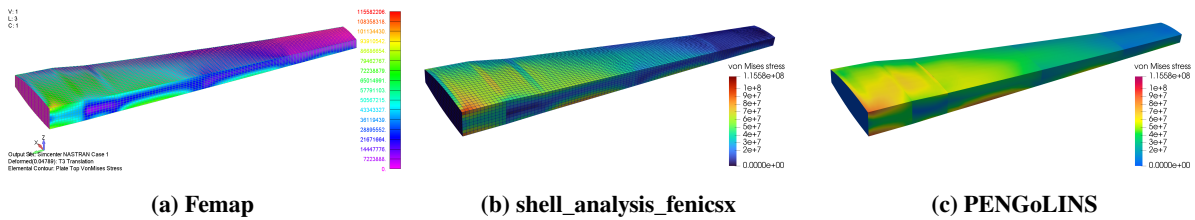
**Fig. 15 Structural responses of the models****Fig. 16 PAV wing von Mises stress distributions.**

Table 6 presents a comparison of the maximum displacement and von Mises stress in the PAV wing, computed using three different shell solvers. In the Femap and shell\_analysis\_fenicsx simulations, a 3g load is applied in the form of point forces. To enhance result consistency, the shell\_analysis\_fenicsx simulation adopts the same mesh and load data as provided by Femap. Both solvers are grounded in Reissner–Mindlin shell theory, incorporating bending energy, membrane energy and transverse shear energy in its formulation. Results in Table 6 indicates a small difference in tip deflection between shell\_analysis\_fenicsx and Femap. This discrepancy may be attributed to differences in the drilling stabilization formulation between Femap and shell\_analysis\_fenicsx. Notably, shell\_analysis\_fenicsx introduces a drilling energy term in its elastic energy formulation to avoid singularity in the stiffness matrix, which inadvertently

brings in a small amount of artificial stiffness to the system [146]. This should not affect the results too much, but in certain scenarios, this may result in a stiffer performance with larger drilling energy scaling factor, as observed we have seen in the results. In the PENGOLINS simulation, which employs an isogeometric Kirchhoff–Love shell formulation with distinct discretization, a load transfer method from [147] is adopted. Nodal forces in Femap are distributed to nodal locations of the isogeometric shell using radial basis functions. Due to the neglect of transverse shear in the Kirchhoff–Love shell theory, PENGOLINS exhibits a smaller maximum displacement, as expected, compared to the other two models that employ the Reissner–Mindlin shell formulation. Both tip deflection and maximum von Mises stress between the three shell models shows good agreement. Figure 16 demonstrates the von Mises stress distribution on the upper surface of the PAV wing. An identical stress pattern is observed among the three solvers.

The choice of model can be guided by the specific requirements of the analysis: when precision in stress prediction is paramount, particularly for failure analysis or when designing to meet stringent safety margins, more accurate analysis methods, such as the Femap shell model or VAM should be used. Given the benefits offered by isogeometric discretization, PENGOLINS can be a viable choice for thin shells in cases where finite element mesh generation is difficult. For conceptual design where computational speed is desired without substantially sacrificing accuracy, the linear/nonlinear box-beam Timoshenko-based model presents a viable option.

**Assumptions of box-beam models.** It should be noted that the height of the box-beam has a significant influence on the structural responses as it plays a role in the moment of inertia computation and the stress computation. Box-beam assumption neglects the curvature of the airfoil, as seen in Fig. 17. If the height is computed at the front spar or rear spar, the height is underestimated, resulting in a lower moment of inertia. On the other hand, if the height is computed near the maximum thickness, the box-beam assumption over-predicts the moment of inertia. In this work, the height was computed at the front spar: 25% chord. This assumption results in a lower moment of inertia and thus greater displacement compared to the shell model where the curvature of the airfoil is taken into account (Fig. 17a).

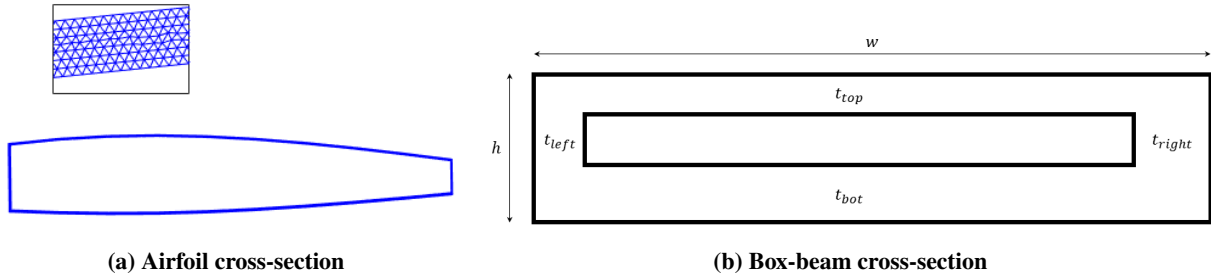


**Fig. 17 Wingbox cross-sectional representation**

**Buckling Failure Criteria** In flight, the top skin of a wingbox is subject to compressive loads and the bottom skin to tensile. The compressive load on the top skin as well as the bending load carried by the spars can cause buckling between rib locations and is a source of critical structural failure. The buckling ratio is defined as the critical stress over the applied stress. A panel with a buckling ratio value of less than 1 will buckle. The critical stress of the skin and spar are calculated using Eq. 5 and Eq. 7 respectively.

$$\sigma_{cr} = K_{skin} \frac{E}{1 - \nu^2} \left( \frac{t_{skin}}{b} \right)^2 \quad (5)$$

where  $E$  is the Young's modulus,  $\nu$  the Poisson's ratio,  $a$  the length of the panel,  $b$  the width of skin panel, and  $K_{skin}$  is a constant that depends on the aspect ratio of the panel.

$$K_{skin} = \begin{cases} 684.89 \left( \frac{a}{b} \right)^4 - 1488.8 \left( \frac{a}{b} \right)^3 + 1244 \left( \frac{a}{b} \right)^2 - 479.76 \left( \frac{a}{b} \right) + 79.168, & \text{if } \frac{a}{b} \leq 0.64 \\ 6.3, & \text{if } \frac{a}{b} \geq 0.64 \end{cases} \quad (6)$$

$$\sigma_{cr} = \eta K_{spar} E \left( \frac{t_{spar}}{b} \right)^2 \quad (7)$$

where  $E$  is the Young's modulus,  $t_{\text{spar}}$  the thickness of the spar,  $a$  the length of the panel,  $b$  the width the of skin panel, and  $K_{\text{skin}}$  is a constant that depends on the aspect ratio of the panel.

$$K_{\text{spar}} = \begin{cases} 1360 \left(\frac{a}{b}\right)^4 - 2911.4 \left(\frac{a}{b}\right)^3 + 2397.4 \left(\frac{a}{b}\right)^2 - 914.45 \left(\frac{a}{b}\right) + 159.95, & \text{if } \frac{a}{b} \leq 0.64 \\ 21.6, & \text{if } \frac{a}{b} \geq 0.64 \end{cases} \quad (8)$$

Figure 18 shows the buckling results of the PAV concept. At minimum gauge, all the skin panels but the furthest outboard will buckle, and the first four spar panels from the root will buckle.

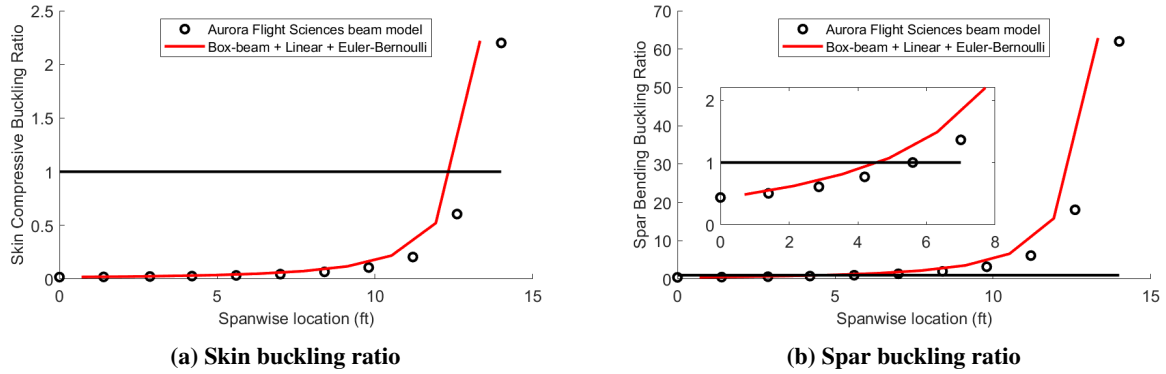


**Fig. 18 Buckling responses of the beam models. A panel with a buckling ratio value of less than 1 will buckle.**

**Structural Sizing** The skins and the spars of the wingbox were sized based on strength and buckling failure criteria. The maximum von Mises stress was constrained to be under the Yield stress and the buckling ratio values of each panel were constrained to be greater than 1. Figure 19 shows the results of the increased thickness of the skin for each panel. Aurora Flight Sciences' skin thickness was slightly higher than the thickness of the UCSD/LSDO box beam model. This is because Aurora Flight Sciences' calculated stress values along the beam were slightly higher and follow the same trend shown in Fig. 19. The thickness of the spars remained at minimum gauge after sizing because the bending stress decreased to below the critical buckling stress of the spars after increasing the thickness of the skins. Table 7 shows the calculated weight of each model after the sizing was complete. The percent error of the weight for the two models is 4.92%.

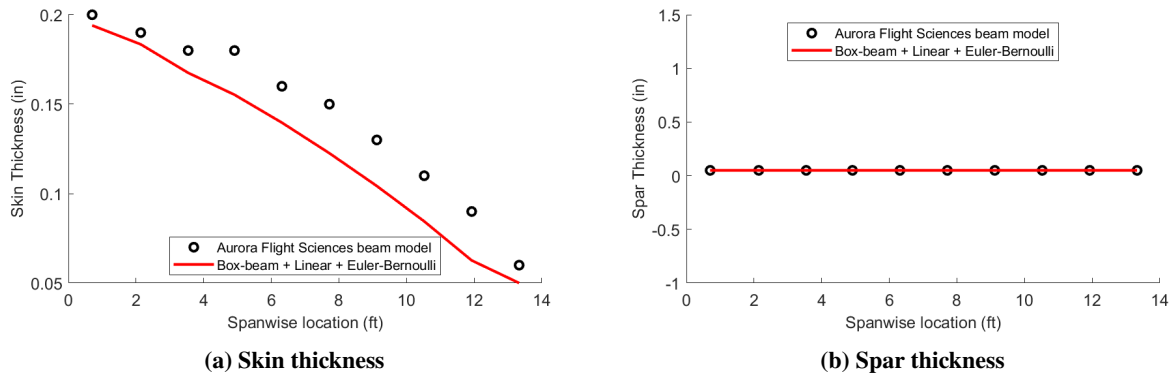


**Fig. 19 Wingbox sized using beam models**

### C. Acoustics

Noise pollution from a helicopter rotor is the reason why the helicopters could not evolve into a mode of daily transportation in urban areas today. Since UAM aims to operate in and out of the cities, it is crucial to reduce rotor noise

**Table 7 Weight of PAV concept wingbox after sizing when represented as a box-beam**

| Model                                                              | Weight (lbs) |
|--------------------------------------------------------------------|--------------|
| UCSD/LSDO box-beam + linear + Euler–Bernoulli                      | 141.61       |
| Aurora Flight Sciences’ MATLAB box-beam + linear + Euler–Bernoulli | 147.95       |

to minimize noise pollution and thus to maintain positive perspective on the UAM operations from the public. The traditional method of optimizing a rotor design based on the aerodynamic efficiency typically does not include noise constraints. However, if the noise constraint is included from the initial optimization stage of the rotor design, the rotor noise is expected to be reduced more effectively. The initial design optimization process typically undergoes hundreds or thousands of iterations. Low-fidelity tonal and broadband noise prediction models facilitate this process by quickly returning the acoustic predictions [148]. The noise level predicted by the models in this study is in decibel (dB), which is the logarithm of normalized pressure. In practice, an A-weighted decibel (dBA) is typically considered to incorporate the sensitivity of human ears at various frequency ranges.

**Table 8 Overview of the tonal and broadband noise prediction models in the present study.**

| Tonal                       |                    |       |               |                  |
|-----------------------------|--------------------|-------|---------------|------------------|
| Models                      | Loading            | Hover | Axial Forward | Edgewise Forward |
| Gutin–Deming (GD)           | Steady             | ✓     | ✓*            |                  |
| Barry–Magliozzi (BM)        | Steady             | ✓     | ✓             |                  |
| Hanson (H)                  | Steady             | ✓     | ✓             |                  |
| Lowson–Ollerhead (LO)       | Unsteady           | ✓     | ✓*            | ✓**              |
| Broadband                   |                    |       |               |                  |
| Models                      | Noise Type         | Hover | Axial Forward | Edgewise Forward |
| Davidson–Hargest (DH)       | OASPL              | ✓     |               | ✓                |
| Schlegel–King–Mull (SKM)    | OASPL              | ✓     |               |                  |
| Stuckey–Goddard (SG)        | OASPL              | ✓     |               |                  |
| Pegg (P)                    | OASPL and Spectrum | ✓     |               |                  |
| Gill–Lee (GL)               | OASPL and Spectrum | ✓     |               |                  |
| Brooks–Pope–Marcolini (BPM) | OASPL and Spectrum | ✓     | ✓             | ✓                |

\* Valid with the correction proposed by Garrick and Watkins [149]

\*\* Valid with an additional correction in the angle between the flight direction and the observer location

### 1. Tonal Noise Predictions

Tonal noise, which is also referred as rotational noise, is a repetitive pressure fluctuation due to the rotational motion of a rotor. An analytical formulation can predict this tonal noise. A list of unsteady and steady frequency-domain tonal noise prediction models investigated in the present paper is provided in Table 8 along with their applicable flight conditions. Gutin’s loading noise model [150] serves as the basis of the other three tonal noise models. Barry and Magliozzi’s model [151] includes the effect of axial forward motion and blade pitch variation, and Hanson’s model [152, 153] includes the effect of blade sweep, propeller pitch, and chordwise non-compactness. However, these models assume constant loads along the azimuth, hence steady loads, which limits the predictive capability of the models to hover and axial forward flight conditions only. Lowson and Ollerhead’s model, on the other hand, accepts azimuthally varying loads or unsteady loads. This unsteadiness is caused by any external influence on the inflow velocity, such as edgewise forward motion, vibration, change in rotation speed, cross wind, or tip vortex induced loading. After thoroughly analyzing the steady and unsteady frequency-domain loading and thickness noise models, the universal form

**Table 9** The collection of constants, phase term, blade pitch effect, and loading terms of the frequency-domain loading noise formulations investigated in the present study.

| Models | $A$                                  | $\Phi$                                          | $\Lambda$                                                                | $T'$                                         | $D'$                                                                       |
|--------|--------------------------------------|-------------------------------------------------|--------------------------------------------------------------------------|----------------------------------------------|----------------------------------------------------------------------------|
| GD     | $\frac{mB^2\Omega}{2\pi cS}$         | 1                                               | 1                                                                        | $\frac{dT}{dR} \cos \theta$                  | $\frac{dD}{dR} \frac{c}{\Omega R}$                                         |
| BM     | $\frac{B\Omega}{\pi cS(1-M_r)}$      | 1                                               | $\frac{R}{b \cos \phi_t} \sin \left( \frac{mBb \cos \phi_t}{2R} \right)$ | $\frac{dT}{dR} \frac{\cos \theta}{(1-M_r)}$  | $\frac{dD}{dR} \frac{c}{\Omega R}$                                         |
| H      | $\frac{imB^2\Omega}{2\pi cS(1-M_r)}$ | $e^{imB[\frac{\Omega S}{c} + (\psi' - \pi/2)]}$ | 1                                                                        | $\frac{dT}{dR} \frac{\cos \theta'}{(1-M_r)}$ | $\frac{dD}{dR} \frac{c}{\Omega R}$                                         |
| LO     | $\frac{imB^2\Omega}{4\pi cS}$        | $e^{-i(mB-\lambda)(\pi/2)}$                     | 1                                                                        | $\frac{dT}{dR} \Big _{\lambda} \cos \theta$  | $\frac{dD}{dR} \Big _{\lambda} \frac{(mB-\lambda)}{mB} \frac{c}{\Omega R}$ |

**Table 10** The propagation, sweep effect, non-compactness effect, and retarded time effect of the frequency-domain loading and thickness noise formulations investigated in the present study.

| Models | $J_\nu$                                                                                                                                                                                                                                            | $S$           | $\Psi$               | $\Gamma$ |
|--------|----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|---------------|----------------------|----------|
| GD     | $J_{mB} \left( \frac{mB\Omega R}{c} \sin \theta \right)$                                                                                                                                                                                           | 1             | 1                    | 1        |
| BM     | $J_{mB} \left( \frac{mB\Omega R}{c(1-M_r)} \sin \theta \right) + \frac{(1-M^2)YR}{2S^2(1-M_r)^2} \left( J_{mB-1} \left( \frac{mB\Omega R}{c(1-M_r)} \sin \theta \right) - J_{mB+1} \left( \frac{mB\Omega R}{c(1-M_r)} \sin \theta \right) \right)$ | 1             | 1                    | 1        |
| H      | $J_{mB} \left( \frac{mB\Omega R}{c(1-M_r)} \sin \theta \right)$                                                                                                                                                                                    | $e^{i\phi_s}$ | $\Psi_L$ or $\Psi_V$ | 1        |
| LO     | $J_{mB-\lambda} \left( \frac{mB\Omega R}{c} \sin \theta \right)$                                                                                                                                                                                   | 1             | 1                    | 1        |

of the loading and thickness noise models are defined as follows:

$$\begin{aligned}
 c_{mB} &= A \int_{\text{hub}}^{\text{tip}} \Phi \Lambda \sum_{\lambda=0}^{\infty} [T' - D'] J_\nu S \Psi \Gamma(R, \psi) dR \\
 d_{mB} &= B \int_{\text{hub}}^{\text{tip}} \Phi \Delta J_\nu S \Psi dR
 \end{aligned} \tag{9}$$

where  $c_{mB}$  and  $d_{mB}$  are the acoustic pressure due to loading and thickness, respectively,  $A$  and  $B$  are the collections of constants,  $\Phi$  is a phase term,  $\Lambda$  is the blade pitch effect,  $T'$  and  $D'$  are the thrust and drag contributions to the loading noise, respectively,  $J_\nu$  is the sound propagation term,  $S$  is the blade sweep effect,  $\Psi$  is the chord-wise non-compactness [152–154],  $\Gamma(R, \psi)$  is the retarded time effect, and  $\Delta$  is the cross-sectional area of an airfoil at each blade section. The formulations of the loading noise models are summarized in Tables 9 and 10.

An important takeaway from Eq. (9) and Tables 9 through 11 is that acoustic predictions can be optimized by using an appropriate model for a given rotor geometry and its operating condition. For instance, GD model is applicable in a hover flight condition since it captures the rotational motion of the rotor blades but not the forward motion of the rotor. On a contrary, the BM and H models take the forward motion into account by including the Doppler effect  $(1 - M_r)$ . Since the BM model considers the pitch variation along the blade span, the influence of the blade pitch on the tonal noise can be observed by using the BM model. Similarly, the H model can be used to understand the effect of blade sweep on the tonal noise as well as the effect of pressure fluctuation along the chord. The BM and H models, however, are limited to the axial forward flight conditions since they do not account for the azimuthally varying loads or unsteady

**Table 11** The collection of constants, phase term, and sectional airfoil area of the frequency-domain thickness noise formulations investigated in the present study.

| Models | $B$                                             | $\Phi$                                          | $\Delta$        |
|--------|-------------------------------------------------|-------------------------------------------------|-----------------|
| GD     | $\frac{-\rho(mB\Omega)^2 B}{3\pi S}$            | 1                                               | $bh$            |
| BM     | $\frac{-\rho(mB\Omega)^2 B^2}{2\pi S(1-M_r)^3}$ | 1                                               | $0.6853bh$      |
| H      | $\frac{-\rho c^2 B^2}{4\pi(S/D)(1-M_r)}$        | $e^{imB[\frac{\Omega S}{c} + (\psi' - \pi/2)]}$ | $M_s(h/b)k_x^2$ |

**Table 12 Rotors used to verify and validate the unsteady and steady frequency-domain tonal noise models at different flight conditions.**

| Rotors        | Blade Number | Radius (m) | RPM  | $M_\infty$ |
|---------------|--------------|------------|------|------------|
| Mejzlik [155] | 3            | 0.3556     | 1500 | 0          |
| SR-3 [156]    | 4            | 0.3240     | 8612 | 0.320      |
| CH-34 [157]   | 4            | 8.5344     | 212  | 0.061      |
| DJI [158]     | 2            | 0.1200     | 6081 | 0.024      |

loads. The LO model resolves this issue by accepting the Fourier transformed unsteady loads as the aerodynamic input. Although the LO model is originally developed assuming a stationary rotor, Garrick and Watkins [149] proved that the formulation's applicability can be expanded to an axial forward flight condition by incorporating the Doppler effect in the observer distance. An additional adjustment on the angle between the direction of forward motion and the observer location can further expand the applicability of the LO model to an edgewise forward flight condition. This conversion is possible for the LO model since it accepts unsteady loads unlike the other three tonal noise prediction models. For the thickness noise models, the GD model remains applicable in hover flight conditions, but the BM and H models can predict the thickness noise even in the axial forward flight conditions. For an edgewise forward flight case, the unsteady loading noise is expected to be significantly larger than the thickness noise, so there is no available frequency-domain thickness noise model in this flight condition. The key to the accurate prediction of the thickness noise based on these models in Table 11 relies on the accurate prediction of the cross-sectional area of the blade sections. Each loading and thickness noise model has its own benefits and limitations, and understanding these attributes of the models and correctly utilizing the models under the most suitable conditions will lead to more accurate tonal noise predictions.

The aforementioned tonal noise models are verified and validated against some experimental data in hover, axial forward, and edgewise forward flight conditions. The rotor information and the operating conditions are provided in Table 12. The SR-3 rotor is in an axial forward flight configuration with Mach number of 0.32, and the CH-34 and DJI rotors are in an edgewise forward flight configuration with Mach number of 0.061 and 0.024, respectively. The tonal noise predictions by the unsteady and steady models are shown in Fig. 20. Figure 20a shows a directivity plot of an experiment with the Mejzlik propeller and measured unsteady loading noise near the rotor axis. The observer distance in this case is 1.5 m away from the rotor hub. The comparison between the unsteady and steady models underscores the limitation of the steady models' predictive capability since they are unable to predict tonal noise near the rotor axis. However, the unsteady frequency-domain loading noise by the LO model in conjunction with the unsteady load modeling in hover as proposed by Kvurt and Stalnov [155] significantly improves the tonal noise predictions at and near the rotor axis. It is important to note that the LO model only accounts for the unsteady loading noise but not thickness noise. To compare with other tonal noise models, the thickness noise by the Deming model is combined with the LO model's loading noise to predict the total tonal noise. Similarly, the models are compared in the axial forward flight condition in Fig. 20b. In this case, the observer is 3.18 m away from the rotor hub. Again, the unsteady model remains as the only model that can predict noise at and near the rotor axis, while the steady models approach zero near the rotor axis. Since the LO model reduces down to the GD model in hover and axial forward flight conditions, the predictions by the two models overlap between 30 and 150 observer angles. The H model shows the best prediction in this case since the SR-3 rotor is composed of swept blades, and the H model is the only model that accounts for the effect of blade sweep. The tonal noise at the fundamental blade passing frequency (BPF) at a single observer is also shown in Fig. 20c. The observer location at which the noise is predicted for the CH-34 rotor is 60.96 m directly below the rotor, and the observer location for the DJI rotor is 2.22 m away from the rotor hub and 60.96 degrees above the rotor plane. Since there is no available thickness noise model for an edgewise forward flight, and it is expected that the loading noise is a dominant source of tonal noise in this flight condition, the tonal noise indicates the loading noise in the edgewise forward flight case. The black bar indicates the measured tonal noise from an experiment, and the red bar indicates the predictions. The predictions show great agreement with the measurements for both rotors that are drastically different in size and operating conditions since the CH-34 rotor is a helicopter rotor and the DJI rotor is a drone rotor. The verification and validation analyses of the unsteady and steady frequency-domain tonal noise models indicate that the LO model is capable of tonal noise predictions at all flight conditions, and the H model is more accurate with rotors with swept blades.

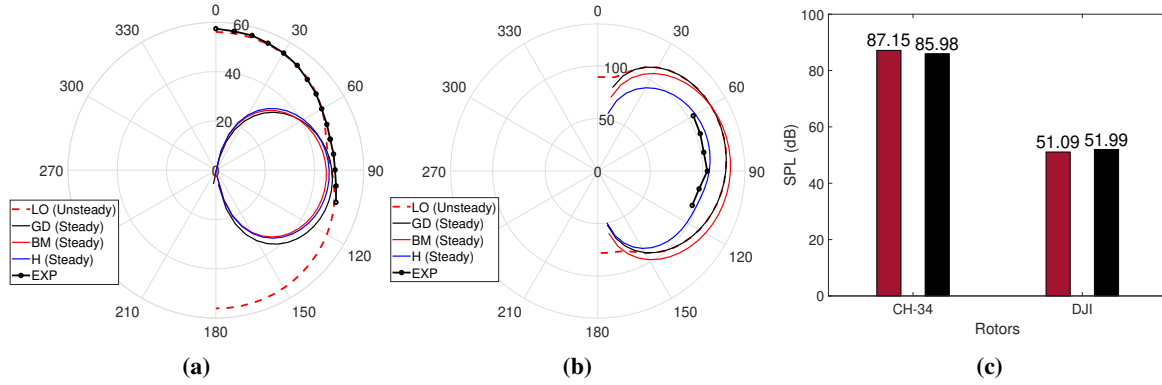


**Fig. 20** Acoustic predictions of the tonal noise at the fundamental blade passing frequency (BPF) by the unsteady and steady frequency-domain tonal noise models in (a) hover using a heavy duty Mejzlik propeller [155], (b) axial forward using the SR-3 [156] rotor, and (c) edgewise forward flight conditions using the CH-34 [157] and DJI [158] rotors.

**Table 13** Scales and constants used in different empirical broadband OASPL models.

| Models                         | $\alpha_1$ | $\alpha_2$ | $\alpha_3$ | $\alpha_4$ | $C$   |
|--------------------------------|------------|------------|------------|------------|-------|
| Davidson–Hargest (DH) [161]    | 6          | 1          | 2          | -2         | -36.7 |
| Schlegel–King–Mull (SKM) [157] | 6          | 1          | 2          | -2         | -42.9 |
| Stuckey–Goddard (SG) [162]     | 6          | 1          | 1.66       | -1.66      | -39.9 |
| Gill–Lee (GL) [163]            | 6.77       | 0          | 2          | -1         | -32.7 |

## 2. Broadband Noise Predictions

Broadband noise, also referred as vortex noise, is high-frequency sound generated by random fluctuations of the forces on the blade due to the interactions with the flow around the blade. It has been accepted that turbulent boundary layer trailing-edge noise is the most dominant noise source in airfoil self-noise [159, 160], which is a significant contribution to the rotor broadband noise. In this section, the traditional and updated overall sound pressure level (OASPL) models for broadband noise predictions are investigated along with the spectrum models. A list of broadband noise prediction models and their extent of capability are provided in Table 8. The OASPL models are empirically derived by scaling the sound pressure level at a reference location. The OASPL models follow a generic formulation as shown in Eq. (10) and the corresponding scales are shown in Table 13.

$$\text{SPL}_{\text{ref}} = 10 \log_{10} [V_t^{\alpha_1} A_b^{\alpha_2} C_T^{\alpha_3} \sigma^{\alpha_4}] + C \quad (10)$$

where  $V_t$  is blade tip velocity,  $A_b$  is total blade area,  $C_T$  is thrust coefficient,  $\sigma$  is rotor solidity, and  $C$  is an empirical constant.

OASPL models are suitable for a quick assessment of the broadband noise contributions; however, the sources of broadband noise contribute over a range of frequencies and most dominant in the high-frequency region. Therefore, predicting broadband noise spectrum provides more detailed insights into the range of frequencies where broadband noise contributions are most significant. Pegg [164] developed a spectrum model that could predict a spectrum of broadband noise based on the rotor geometric and performance parameters:

$$\text{SPL}_{1/3} = 20 \log_{10} \left( \frac{V_t}{c} \right)^3 + 10 \log_{10} \left( \frac{A_b}{S^2} \right) [\cos^2(\theta) + 0.1] + S_{1/3} + f(C_L) + 130 \quad (11)$$

where  $c$  is speed of sound,  $S$  is the observer distance from the hub,  $S_{1/3}$  is a shape function, and  $f(C_L)$  is a lift contribution term with  $C_L$  being the average blade lift coefficient. The maximum noise level is captured by the first two terms in Eq. (11), and  $S_{1/3}$  defines the shape of the noise spectrum by subtracting empirically derived constants at one-third octave band frequencies. The detailed derivations and information about the formulation can be found in the reference [164].

The main limitation of the Pegg model arises in the shape function,  $S_{1/3}$ , since it uses the empirically determined constants to predict the shape of the broadband noise spectrum, which results in the same low- and high-frequency slopes for all rotor types. The lift contribution term,  $f(C_L)$ , is also empirically derived based on a helicopter rotor, thus further restricting the models' applicability in more modern rotors. To resolve this issue with the current broadband noise model, Gill and Lee [163] utilized a variety of rotor experiment data and an evolutionary machine learning algorithm, called Gene Expression Programming (GEP), to formulate a model that can predict broadband noise spectrum using the basic geometric and performance parameters. The basic form of the GEP model follows a universal wall pressure spectrum model [165, 166] since this wall pressure spectrum determines the shape of trailing-edge noise, which is an important broadband noise source of rotorcraft [167]. Gill and Lee's (GL) spectrum model has shown more improved spectrum prediction capability not only in helicopter rotors but also in propeller and drone rotors as well.

Another broadband noise model that is widely accepted among the rotorcraft community is Brooks-Pope-Marcolini (BPM) model [159] for its versatility in applicable flight conditions and its relatively fast computing time. The model is empirically derived through curve fitting the broadband noise data obtained from an extensive experimental study on the NACA 0012 airfoil and relating the noise with the boundary layer parameters, such as boundary layer and displacement thicknesses. The BPM model uses these boundary layer parameters to predict turbulent boundary layer trailing-edge noise, flow separation noise or stall noise, laminar boundary layer vortex shedding noise, trailing-edge bluntness noise, and tip vortex noise. These boundary layer parameters can be either obtained through the empirical models which Brooks, Pope, and Marcolini also provide in their reference [159] or an aerodynamic solver such as XFOIL [168] or a computational fluid dynamic (CFD) solver for higher-fidelity predictions. Since the turbulent boundary layer trailing-edge (TBLTE) noise is the most dominant noise source and blade sections are assumed to not stall in the context of rotor optimization, the TBLTE noise is only considered in this paper when predicting broadband noise spectrum.

In a case of hover flight, an amplitude modulation must be considered for each blade section to account for the shift in the noise frequency due to the rotational movement of a rotor. Equation (12) shows the azimuthally averaged power spectral density,  $S_{pp}(R)$ , for each blade section.  $SPL_{BPM}$  is the sound pressure level from a single blade section obtained from the BPM model, which is converted into the sectional power spectral density,  $\bar{S}_{pp}$ .

$$S_{pp}(R) = \frac{N}{2\pi} \int_0^{2\pi} \left( \frac{\omega}{\omega_d} \right)^2 \bar{S}_{pp}(R, \psi) d\psi$$

$$\bar{S}_{pp}(R, \psi) = 10^{SPL_{BPM}/10}$$

$$\left( \frac{\omega}{\omega_d} \right) = 1 + M_r \left( \frac{X}{R_s} \right)$$
(12)

where  $M_r = \Omega r/c$ ,  $R_s$  is the distance between the blade section and an observer,  $\frac{\omega}{\omega_d}$  is the ratio between the source frequency and the Doppler-shifted frequency, and  $X$  is the transformed x coordinate. For the brevity of the acoustics section, the coordinate transformation is not explicitly discussed, but the detailed description of the coordinate transformation and the proper implementation of the coordinate transformation into the multidisciplinary optimization program is discussed in the references [169]. Once the azimuthally averaged  $S_{pp}(R)$  is determined for all spanwise sections, we sum them up:

$$S_{pp, Rotor} = \sum_{i=1}^n S_{pp}(R_i)$$
(13)

Finally, the broadband noise of a rotor in hover flight is defined as follows:

$$SPL_{Rotor} = 10 \log_{10}(S_{pp, Rotor})$$
(14)

In a case of edgewise forward flight, retarded time or observer time must be computed first. using the following expression:

$$|\vec{x}(t) - \vec{y}(\tau)| = \sqrt{[(X - r \cos \psi \cos \alpha_{sh}) + V_{\infty}(t - \tau)]^2 + [Y - r \sin \psi \cos \alpha_{sh}]^2 + [Z - r \cos \psi \sin \alpha_{sh}]^2}$$
(15)

where  $\psi$  and  $\alpha_{sh}$  are the rotor azimuth angle and shaft angle of attack, respectively. To calculate the observer time of each blade section, the quadratic equation in Eq. (16) is solved using the Newton-Raphson root finding method.

$$t - \frac{|\vec{x}(t) - \vec{y}(\tau)|}{c} - \tau = 0$$
(16)

**Table 14 Rotors used to verify and validate the empirical broadband noise models at different flight conditions.**

| Rotors                | Blade Number | Radius (m) | RPM   | $M_\infty$ | $C_T$  | $s_0$ (m) | $\theta_0$ (deg.) |
|-----------------------|--------------|------------|-------|------------|--------|-----------|-------------------|
| W4STD8 [170]          | 4            | 0.6096     | 1612  | 0          | 0.0117 | 3.658     | 90                |
| Ideally Twisted [171] | 4            | 0.1588     | 5508  | 0          | 0.0182 | 2.270     | -35               |
| COPR-3 [172]          | 3            | 0.3048     | 3645  | 0.125      | 0.0170 | 7.780     | 57                |
| BO-105 [173]          | 4            | 2          | 435.6 | 0.055      | 0.0044 | 7.132     | 90                |

where  $c$  is speed of sound. The distance between the observer at the computed retarded time and the noise source at the source time is used in the BPM model to predict broadband noise at far-field. Similar to the hover case, the total sound pressure level of a rotor is computed by logarithmically summing up the noise contributions from the blade sections. Iterating this process returns the  $\text{SPL}_{\text{Rotor}}(\psi, r, f)$  as a function of azimuth angle, radial section in source time, and noise frequency, which is interpolated at the user-defined observer time array and logarithmically summed up. The final SPL is a function of observer time and frequency, which is averaged by the number of time steps in the observer time that is arbitrarily chosen

$$\text{SPL}_{\text{Final}}(f) = \frac{\sum_{i=1}^{t_N} \text{SPL}_{\text{Rotor}}(t_i, f)}{t_N} \quad (17)$$

where  $t_N$  is the number of time steps.

The empirical broadband noise models are also validated against experiment data at all flight conditions, and the rotor information and operating conditions are provided in Table 14, in which  $M_\infty$  represents the forward Mach number (axial or edgewise),  $C_T$  is the rotor thrust coefficient,  $s_0$  is the hub-observer distance, and  $\theta_0$  is the elevation angle of an observer from the rotor plane. Figure 21 shows the predictive performance of the OASPL and spectrum models with respect to the measurements. In Fig. 21a, the Pegg model over-predicts the amplitude of the broadband noise throughout the frequency range and under-predicts the peak frequency. The BPM model, on the other hand, accurately predicts the maximum noise amplitude and the peak frequency. The GL model slightly over-predicts the peak frequency, but the maximum noise amplitude and the spectrum shape agree with the measurement. The GL model accepts the similar rotor performance parameters as the Pegg model does but shows comparable performance as the BPM model which accepts more detailed flow parameters. The overall sound pressure level (OASPL) of an ideally twisted rotor case is shown in Fig. 21b.  $\text{GL}_s$  denotes the GL spectrum model, and P denotes the Pegg model. All helicopter-based empirical models (DH, SKM, SG, and P) over-predict the experiment data, while the SKM model predicts the closest to the data among the four. The BPM model under-predicts the OASPL by 3.37 dB, and both GL OASPL and spectrum models predict the measured OASPL within 2 dB error. Figures 21c and 21d represent an axial and edgewise forward flight condition, respectively. Both the Pegg model and GL spectrum model are included in the figures to observe how they would perform in these flight conditions. While the Pegg model still over-predicts in the axial forward flight condition, the GL spectrum model surprisingly performs well in the axial flight condition. The strong predictive performance of the GL spectrum model in the axial forward flight case, as shown in Fig. 21c, can be anticipated given that the COPR-3 rotor, which was utilized for training the machine learning algorithm in hover conditions, was used. More validations in this flight condition are required. For the edgewise forward flight case in Fig. 21d, both the Pegg and GL spectrum models employ the input RPM corresponding to the minimum blade tip velocity on the retreating side to better match with the experimental data. However, the rationale for selecting the minimum tip velocity as the input parameter has not been fully justified yet. The GL model consistently predicts the peak frequency higher than the Pegg model regardless of the flight conditions but is more robust in predicting noise magnitudes and high-frequency spectral shapes. Since the BPM model accepts more detailed input parameters, the BPM model performs the best among the three models in the edgewise forward case. The noise peak at high frequency represent the trailing-edge bluntness noise; the geometric parameters to predict the trailing-edge bluntness noise is approximated since they are not explicitly known from the experiments.

### 3. Application: Aurora Flight Sciences' Passenger Air Vehicle (PAV) Prototype

**Aircraft-level noise prediction** In the subsequent acoustics analysis, we have employed the notional rotors specifically designed for this study (Appendix A), rather than the actual rotors used on the PAV prototype. The primary objective of utilizing these notional rotors is to demonstrate the applicability of our acoustic models at the airplane level, providing a

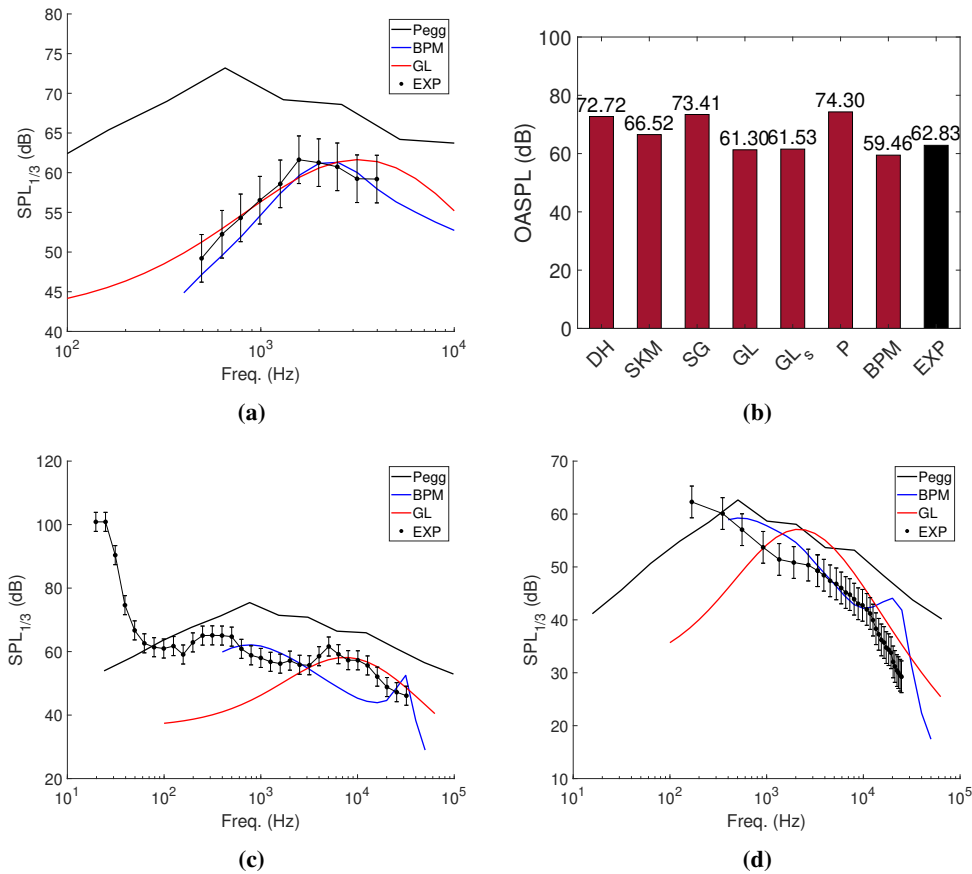


**Fig. 21** (a) Broadband noise spectrum predictions in hover using a W4STD8 propeller [170] with 1612 RPM, (b) OASPL predictions by all empirical broadband noise models in hover using an ideally twisted rotor with  $10^\circ$  blade tip pitch and 5508 RPM, (c) broadband noise spectrum prediction in axial forward flight using a COPR-3 propeller [172], and (d) broadband noise spectrum prediction in edgewise forward flight using a BO-105 rotor [173].

comprehensive noise prediction for the entire aircraft.

The LO model for tonal noise and GL OASPL and spectrum models for broadband noise are selected to analyze the acoustics of Aurora Flight Sciences' PAV prototype in decibels. Since the gross weight of PAV prototype is 800 kg as shown in Table 18, thrust coefficient of each rotor is determined assuming that the weight is evenly distributed among the eight rotors. The rotor radius that is considered in this application is 0.9144 m, operating at 2400 RPM. It is important to note that the acoustic analysis of the rotor excludes various interaction noises. These omitted interactions include blade-vortex interaction, rotor-to-rotor interaction, and rotor-to-airframe interaction.

Figures 22a and 22b show three-dimensional surface plots of the total tonal and broadband noise predictions on the ground from eight rotors of the PAV prototype, respectively. Figure 22c shows the total noise spectrum at a single observer located at 45 degrees below the global aircraft x-y plane at a distance of 21.55 m behind the nose of the aircraft. The zero altitude indicates the height at which the rotors are operating, and the ground is 15.24 m below the aircraft. A different noise propagation pattern on the ground is observed between the tonal and broadband noise in Figs. 22a and 22b. The tonal noise directly below the aircraft is relatively low, but it increases as an observer moves away from the aircraft until the observer location is approximately 53 degrees below the global aircraft x-y plane. Beyond this location, the tonal noise continues to decrease. On the other hand, the broadband noise prediction shows the maximum noise amplitude directly below the aircraft and decreases as an observer moves away from the aircraft on the ground. Additionally, a total acoustic spectrum from all eight rotors is shown in Fig. 22c with the tonal noise at the first four BPFs predicted by the LO model and the broadband noise predicted by the GL spectrum model. For the given configuration,

the acoustic predictions indicate the total rotor noise from this aircraft is expected to be significantly large in both tonal and broadband components, and a design optimization to reduce rotor noise is required to successfully operate in the urban areas. The application of these noise models into lift+cruise aircraft multidisciplinary optimization is presented in a companion paper [174].

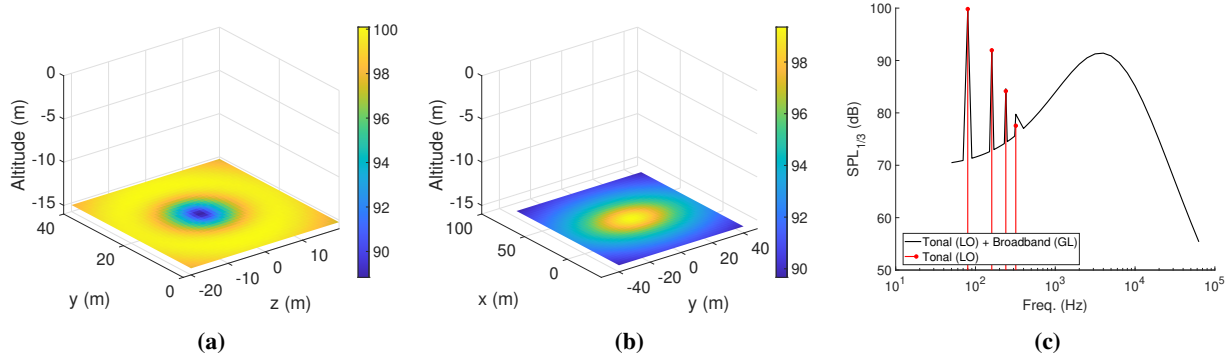


**Fig. 22 Total rotor noise predictions: (a) tonal noise using the LO model, (b) broadband noise using the GL OASPL model, and (c) total acoustic spectrum at a single observer location [15.24, 0, 15.24] m away from the nose. Note, the rotors used in this study are notional ones described in Appendix A.**

It is important to acknowledge that, due to the proprietary nature of the PAV's design and the use of notional components in our model, direct comparisons for V&V purposes are not feasible at this stage.

## D. Motor

### 1. Current status of the motor in eVTOL

Electric motors are crucial components in eVTOL aircraft. Motors in electric aircraft have stringent requirements for reliability, power density, and safety. Common electric motor types used in electric aircraft include permanent magnet synchronous motors (PMSM), switched-reluctance machines (SRM), induction motors (IM), and wound field synchronous motor (WFSM). [175] presents a comprehensive overview of various electric motor topologies by different power levels and applications.

PMSMs are increasingly gaining attention in aerospace applications for their high power density and high efficiency. Airbus and Rolls Royce have developed a 2.5 MW PMSM with a 3 kV DC bus system for high-altitude operations in [176]. Furthermore, the axial flux permanent magnet motor (AFPM), one type of PMSM, has been recently applied in medium-speed, high-torque applications owing to its superior torque density. The topologies of AFPMs have been summarized in [177]. [175] developed a 4 kW/liter AFPM with a 100 kW power capacity. However, PMSMs have limitations in the performance at varying temperatures due to the high temperature sensitivity of the Neodymium magnet.

SRMs have advantages of easy manufacturing, high reliability, and robust operation at high temperatures and speeds, due to the lack of permanent magnets and windings on the rotor [176]. However, SRMs have lower torque density and power density compared to PMSMs.

Similarly, IMs are widely used in industry and benefit from high reliability and robustness in high-temperature and high-speed operations due to the lack of permanent magnets. However, IMs have high copper losses and lower efficiency due to the winding on the rotor.

WFSMs have been used in aerospace applications in decades, due to their complete field controllability, simple and practically autonomous control strategy, and high flexibility in terms of architecture configuration [178]. Nevertheless, WFSMs generally have less efficiency than PMSMs because they require additional energy to excite the field windings on the rotor. Additionally, WFSMs are heavier than PMSMs of the same power due to the windings on the rotor.

### 2. Motor electromagnetic modeling

There are many different approaches to electromagnetic modeling of the electric motors, with varying levels of time-efficiency and fidelity, as reviewed in [179]. The most prevalent methods include the Analytical Model (AM),

Magnetic Equivalent Circuit (MEC), and Finite Element Analysis (FEA).

**Analytical model** The AM primarily employs mathematical equations to describe the physical performance of electric motors [180–182]. These models generally simplify the motor's geometry and magnetic field, analyzing motors using theoretical equations. For example, the subdomain model (SD) proposed in [183, 184] utilizes 2-D analysis in polar coordinates. It segments the motor into various subdomains, solving differential equations within each, in conjunction with boundary conditions, to derive the vector potential in the form of a Fourier series. Subsequently, based on the continuity of the magnetic field, the harmonic components of each subdomain can be calculated. The analytical results have a good match with comparisons to other numerical modeling methods. Additionally, Conformal Mapping (CM) [185] and Schwarz-Christoffel (SC) [186] mapping are widely used in analytical modeling for analyzing the tangential magnetic flux density distribution along the air gap. However, AMs generally do not consider nonlinear effects in electric motors, focusing on calculating electromagnetic variables from the average magnetic flux field in the air gap. This approach can result in significant errors in post-processing and iron loss estimation. Therefore, although AMs offer good computational efficiency and relative accuracy in preliminary motor design, their practical application faces major accuracy issues due to the negligence of nonlinear effects. To improve the accuracy of analytical models and incorporate nonlinearity, several new methods have been introduced [187–189]. In [187], local saturation effects are addressed by dividing the stator and rotor regions into elementary subdomains. The study in [188] introduces a Nonlinear Semi-Analytical Model (SAM) for spoke-type PMSM. This model, employing a nonlinear iterative algorithm, addresses magnetic saturation in magnetic bridges, achieving high accuracy and reducing calculation time by 20% compared to the FEA model.

**Magnetic equivalent circuit model** The MEC method is an effective tool for analyzing and designing electric motors [190, 191]. The essence of MEC lies in converting a motor's magnetic field problem into an equivalent circuit issue. This is achieved by creating an electrical circuit network that represents the magnetic paths or permeance in each motor segment, with the current and voltage at each node simulating the flux and magnetic forces, thereby modeling the motor's magnetic field distribution and performance. The main advantages of this method are simplicity and intuitiveness, making it particularly suitable for preliminary design and performance estimation. Furthermore, the MEC method could account for nonlinear effects, including magnetic flux density saturation, leakage, and iron losses. In the MEC approach, the iterative methods such as fixed-point iteration [192, 193] and the Newton-Raphson formulation [194, 195] are commonly used to resolve nonlinear variables.

In fact, the accuracy of simulation results in MEC modeling can be determined by the level of refinement in the circuit network division. For example, Fig 23 shows a basic example of the low-fidelity MEC method, where  $\phi_r$  represents the residual flux of each pole;  $\lambda_\delta$ ,  $\lambda_f$ ,  $\lambda_s$ , and  $\lambda_\sigma$  represents the total conductance of stator, air gap, and rotor, the conductance of magnetic bridge, the total leakage conductance through nonmagnetic material, and the conductance of the magnet, respectively. Fig. 23a depicts the equivalent circuit corresponding to the motor presented in Fig. 23b. By solving this circuit, various electromagnetic quantities of the motor can be derived. Fig. 23b also illustrates the distribution of the magnetic circuit in the motor under no-load conditions. The impact of leakage flux, occurring in magnetic bridges I and II, is considered in this MEC approach. This low-fidelity MEC method, characterized by a single circuit loop, is primarily utilized for the preliminary motor design, emphasizing speed over accuracy.

Additionally, establishing a sufficiently detailed equivalent circuit network could ensure more accurate results, but it would also result in a relatively larger computational burden. Fig. 24 demonstrates an example of a high-fidelity MEC method. In this method, each segment of the motor is subdivided into multiple elements to more accurately evaluate the motor's performance, such as the distribution of magnetic flux density and the presence of any regions that are overly saturated.

**Finite element analysis model** The FEA is the most commonly used and sophisticated method for designing and accurately analyzing electric motor performance, employing a variant of Maxwell's equations on an intricately dense mesh of the motor, as shown in Eq. (18). For most current FEA software, the general workflow and methodology includes building a geometric model, mesh generation, assigning material and physical properties, setting boundary conditions, solving Maxwell's equations, and conducting post-processing. This workflow enhances the FEA method's flexibility, particularly in analyzing new materials or innovative motor geometries, including novel slot-pole combinations, magnet configurations, winding distributions, and dimensional geometries. Moreover, the FEA method addresses nonlinear variations, including magnetic saturation and temperature-dependent material properties, while enabling dynamic

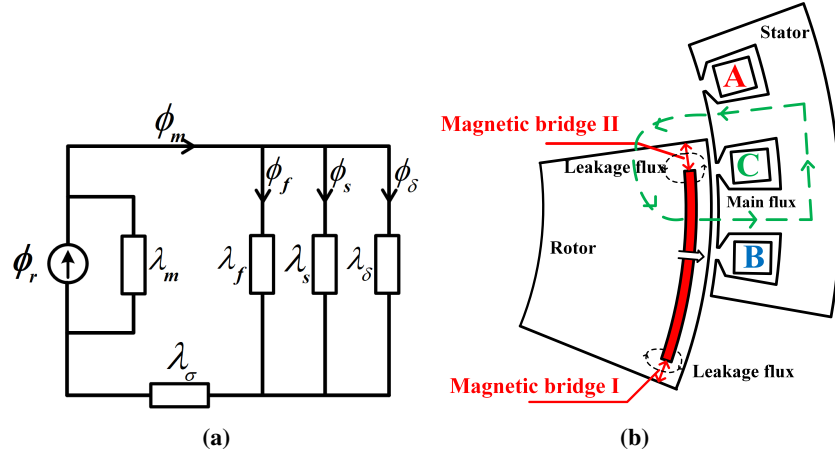


**Fig. 23** Magnetic equivalent circuit of the baseline PMSM (a) Equivalent circuit for the PMSM and (b) Illustration for no-load flux distribution of the PMSM [196].

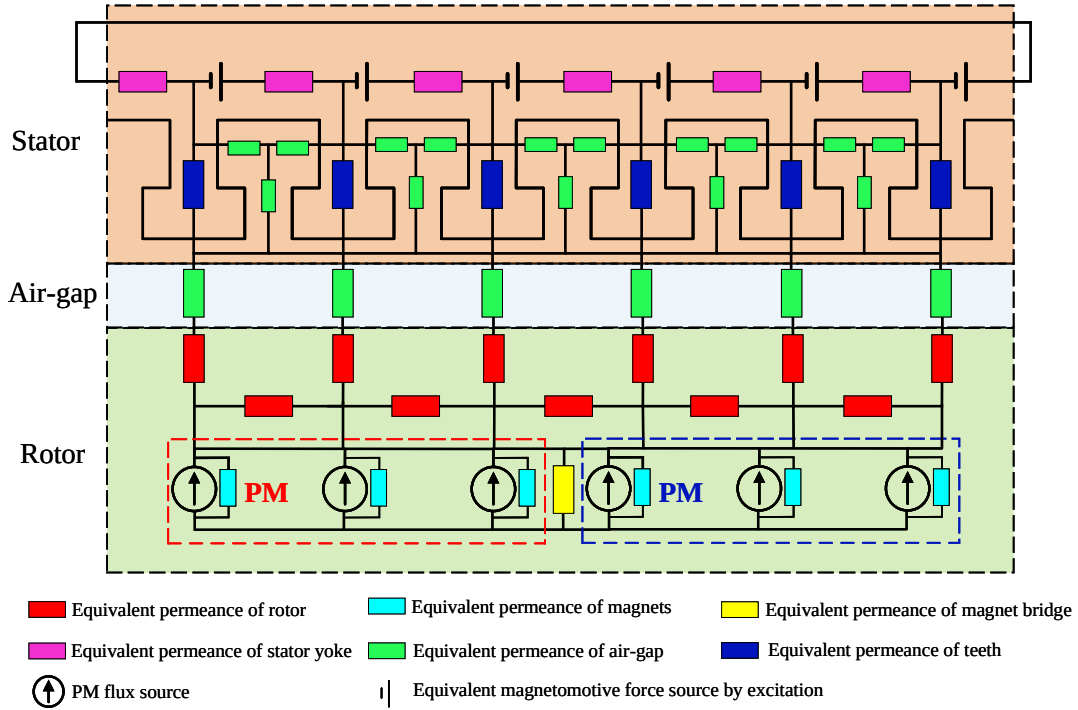


**Fig. 24** High-fidelity MEC with refined meshing.

analysis of electric motors. It also couples easily with other physical fields. Nevertheless, FEA lacks computational efficiency regarding solving time. Generally, simulating a few cycles of practical motor dynamic performance requires several hours. Additionally, similar to the MEC method, the core losses calculations in FEA heavily rely on the power loss coefficients of materials, potentially resulting in inaccuracies in loss analysis. The relations of the field parameters can be expressed as follows:

$$\begin{cases} \nabla \times \mathbf{H} = \mathbf{J} + \frac{\partial \mathbf{D}}{\partial t} \\ \nabla \cdot \mathbf{B} = 0 \\ \mathbf{B} = \mu \mathbf{H} \end{cases} \quad (18)$$

where  $H$  is the magnetizing field;  $B$  is the magnetic flux density;  $\mu$  is the permeability;  $J$  is the current density;  $D$  is the displacement field.

Fig. 25 illustrates an example of calculating the flux linkage and performing post-processing in the FEA method. The flux linkage is determined based on the quantity of flux passing through a defined area in space, which is dictated by the magnetic flux density field. In PMSM geometry, a pseudo-surface connecting adjacent positive and negative slots of the same phase winding is employed to represent the surface through which the magnetic flux density passes. The length of this surface should be equal to the effective length of the motor. The formula for calculating the flux linkage could be expressed as follow:

$$\lambda = \int_S \mathbf{B} \cdot d\mathbf{S} = \int_S (\nabla \times \mathbf{A}) \cdot d\mathbf{S} = \int_{\partial S} \mathbf{A} \cdot d\mathbf{l} = ((\mathbf{A}_z)_{s+1} - (\mathbf{A}_z)_s)l, \quad (19)$$

where  $S$  and  $I$  represent the surface and line in pseudo-surface, respectively;  $A$  is the vector potential field;  $s$  and  $s + 1$  represent adjacent slots of the same phase. Once the flux linkage of each phase is measured through Eq. (19), the three phase flux linkage can be transformed into  $\psi_d$  and  $\psi_q$  in the d-q coordinate system. Then, other variables could be solved by post-processing.

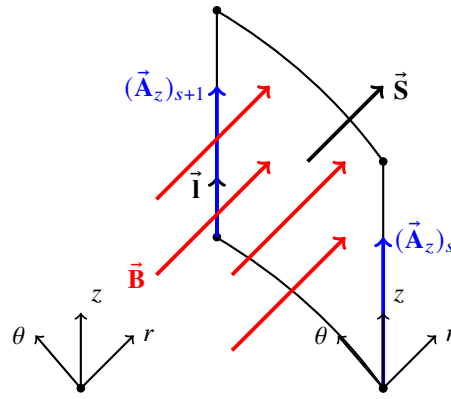


**Fig. 25 Flux linkage pseudo-surface representation.**

**Hybrid model** The respective advantages of AM, MEC, and FEA could be leveraged through a combined approach to reduce the computational effort while maintaining flexibility in motor geometry and relative accuracy of the results. In [197–199], the combined solution of AM and MEC has been developed to obtain a fast and accurate solution efficiently. Normally, AM analyzes the magnetic distribution in the air-gap, while MEC is employed for post-processing and computing electromagnetic variables. This combined method enables the analysis of the motor's nonlinear behavior, offering advantages in motor geometry flexibility and reduced computational load. Additionally, the integration of FEA and AM has been explored in [200]. The FEA model predicts the slotless magnetic field, taking into account the saturation effect within the rotor iron. AM, the second model, employs the conformal mapping (CM) technique to address the slot effect and utilizes the subdomain method for simulating the armature reaction. Consequently, this allows for precise calculation of the magnetic field distribution and other electromagnetic variables. However, although this method reduces the computational burden, it provides limited design insights since it solely focuses on the rotor's finite element analysis. In recent years, new hybrid models have been continuously studied, with the aim of improving the accuracy and speed of modeling through a combination of aforementioned modeling approaches [201, 202].

Table 15 presents a comprehensive comparison of the electric motor modeling techniques discussed in this section. The advantages and disadvantages of each method are highlighted in this table.

## E. Powertrain System

The primary role of the powertrain system is to obtain the mission power profile. The power profile is dependent on the aircraft concept and the intended mission. Senkans et al.[Ref: A first-principle power and energy model for eVTOL vehicles] developed a power and energy model tailored to eVTOL vehicles that accounts for the energy dynamics across varied rotor configurations throughout distinct flight phases: VTOL, transition from vertical to forward flight, and

**Table 15 Overview and comparison of different methodologies used in electric motor electromagnetic modeling (where '+' represent advantages, '0' represents neutral rating, '-' represents disadvantages).**

| Method           | Accuracy | Time | Advantages                  | Disadvantages                  |
|------------------|----------|------|-----------------------------|--------------------------------|
| FEA              | ++       | -    | High-level of accuracy      | High computational resources   |
| MEC[190, 191]    | +        | 0    | Medium computational burden | Difficult of dynamic analysis  |
| AM(SD)[183, 184] | 0        | +    | High-level of accuracy      | Neglect nonlinear effect       |
| SAM[188]         | +        | 0    | Consider nonlinear effect   | High computational resources   |
| AM+MEC[197]      | 0        | +    | Less computational burden   | Difficult of dynamic analysis  |
| AM+FEA[200]      | +        | 0    | Medium computational burden | lacks the insight of the motor |

sustained cruise flight. Their work shows that for rotary-wing concepts, the energy distribution encompasses 31% for cruise, 40% for VTOL, and 29% for the transitional flight phase. Conversely, when examining fixed-wing concepts, the energy utilization is markedly disparate, constituting a mere 4% during cruise, 23% throughout VTOL, and a predominant 73% during the transition phase. These findings are essential in understanding the energy allocation and its implications on the design and operation of eVTOL systems.

### 1. Powertrain Architecture

The electric propulsion system has been received widespread attention and research over the past decades due to the multiple benefits of aircraft electrification, including environmental friendliness, cost savings, low acoustic noise, and high efficiency. Depending on the aircraft's energy storage systems and structural configurations, electric propelled aircraft (EPA) is typically classified into three categories: turboelectric, hybrid-electric, and all-electric configurations. Development trends and analyses of these EPA configurations for electric aircraft have been reviewed in [203–209]. In the aforementioned papers, Distributed Electric Propulsion (DEP) concept has garnered widespread attention and research due to its adaptability to any of the configurations and notable enhancement of system redundancy. The DEP concept provides distributed thrust capability, leading to enhancements in system-level efficiency, capability, safety, and the overall performance of the aircraft. The X-57 Maxwell, developed by NASA, has incorporated the DEP structure [210]. As depicted in Fig. 26, the X-57 traction system comprises of two independent battery packs. Each battery pack connects to two separate buses, which collectively power two 72 kW cruising motors on the wingtips and twelve 10.5 kW motors distributed along the underwing pods. This configuration offers redundancy for the system, which helps mitigate the severity of single-failure modes, ensuring that the aircraft can continue to operate normally even if a system component fails during a mission. Joby Aviation has adopted a similar powertrain architecture in [211].

In this architecture, the battery is directly connected to the DC-link of the inverter without a DC/DC converter. This approach benefits from a higher power density, enhanced system efficiency, reduced costs, and a minimized probability of failure. However, this powertrain architecture also introduces design complexities for the electric motor, which must operate across the entire range of battery output voltages. Moreover, to prevent DC bus oscillation, in general, a large bus capacitor or an active power filter (APF) should be placed between DC bus and inverter. Consequently, this reduces power density and increases costs [212, 213]. It can also produce strongly pulsating currents being injected into the battery during braking and landing, which may reduce the battery's lifespan [214].

Furthermore, the DC bus voltage levels in electric aircraft are typically elevated to above 1 kV, which benefits system optimization by increasing efficiency, reducing conduction losses, and wire mass. However, the peak battery voltage in the aerospace industry is 800V, as indicated by [215]. Higher battery output voltage may increase safety risks and complicate the design of thermal management systems. Therefore, DC/DC converter should be considered at MW-scale powertrain architecture, which directly connects the battery and DC bus. In [212–214, 216], a powertrain architecture incorporating a DC/DC converter between the battery and the HVDC bus is proposed and studied, as shown in Fig. 27. This design allows the battery to be powered during braking and landing through a bi-directional DC/DC converter, reducing the injected current to preserve battery life. In addition, the DC/DC converter can regulate a constant DC bus voltage, which is particularly beneficial in propulsion systems with multiple energy storage options like lithium-ion batteries, supercapacitors, and fuel cells.

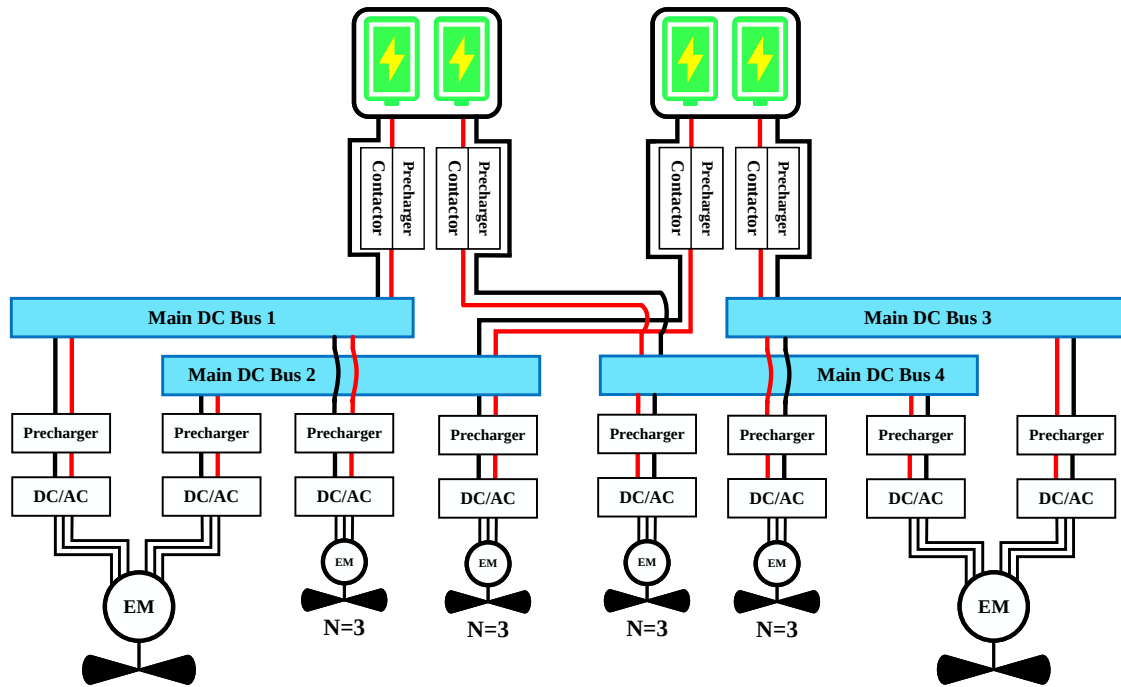


Fig. 26 Traction system architecture of NASA's X-57 (Mod IV) AEA [210, 212].

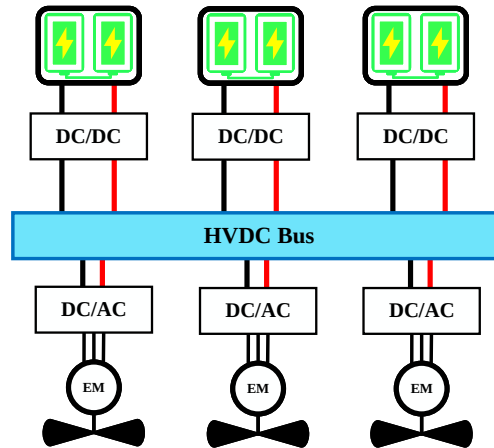


Fig. 27 DEP powertrain architecture with the DC/DC converter.

## 2. Current Status of Power Electronics Converter

Regardless of UAM configurations, each aircraft's powertrain architecture consists of both AC and DC electric systems. Power electronic converters, therefore, are vital for aircraft operation, ensuring proper power management between these networks. The topology of these converters must be adapted according to the aircraft's type, configuration, dimensions, and the required power and voltage levels.

**DC/DC converter** DC/DC converter, in traditional aircraft, is mainly connected between the low-voltage DC bus and secondary loads. However, with the advancement of EPA, the demand for DC/DC converter has increased due to the higher power requirements of electric aircraft systems. Depending on the power level and the type of energy storage system used, a DC/DC converter becomes essential for stepping up the voltage to a high-voltage DC bus and for integrating various energy storage systems into a unified DC bus.

There are several isolated DC/DC converter topologies which are widely used in DC/DC conversion due to the safety

benefits of galvanic isolation. Dual active bridge (DAB) is one of the most popular isolated topologies in aerospace applications, known for its high efficiency, high power density, and safety [217, 218]. The Interleaved Boost with Coupled Inductors (IBCI) converter is designed to minimize the filter capacitor requirement, making it well-suited for high-power and low-voltage applications [219, 220]. The Active Bridge Active Clamp (ABAC) aims to maintain high power transfer capability and efficiency across a broad operating range while delivering high power quality without any AC components [221, 222]. A five-level DAB (5LDAB) topology reduces the voltage stress on switches and is expected to extend the primary voltage range [223].

Although isolated DC/DC converters are widely used in the aerospace field, non-isolated DC/DC converters still hold potential. Non-isolated converters, lacking a transformer, can achieve higher power density in an ultra-lightweight form and higher efficiency. The Four-Switch Buck-Boost (FSBB) converter is one of the promising non-isolated DC/DC converter solutions [224–227]. Utilizing a constant-frequency Zero-Voltage Switching (ZVS) modulation strategy, the FSBB converter can operate with low losses across a broad output power range, thereby achieving high efficiency and power density [225, 226]. In high-voltage and high-power applications, the Multi-level Flying Capacitor (ML-FC) [228–230] is a feasible solution because it has less voltage stress on switches and requires smaller inductor components, leading to high efficiency and power density. However, it is also crucial to take into account the impact of increased weight from the additional switches and the complexity of the sophisticated control strategy.

**Inverter** In electric aircraft, DC/AC converters, commonly known as inverters, play a crucial role in the powertrain architecture. The design requirements for these inverters are essentially the same as those for the DC/DC converters mentioned earlier. In addition, the inverters must be bidirectional to charge battery during braking and landing.

There are several inverter topologies commonly used in electric aircraft applications. The main topology used in aircraft is the two-level inverter, featured for its simple control strategies and fewer switches. However, with the rising demand for increased DC bus voltage, the multi-level inverter is gradually gaining attention in electric aircraft applications due to its higher efficiency, reduced voltage stress, and lower harmonic distortions. The Neutral Point Clamped (NPC) multi-level inverter is particularly studied. The voltage stress on each switch of NPC inverter is lower compared to the two-level inverter, which make it has capability to handle higher DC bus voltages. However, the NPC inverter requires a larger number of switches and involves more complex control strategies. Another notable development is the multi-level active neutral point clamped (ANPC) inverter, an advancement of the NPC inverter that replaces passive diodes with active MOSFETs. A significant advantage of the ANPC inverter is that in each phase, two MOSFETs switch at the same frequency as the motor's electrical frequency, which significantly reduces the switching losses and improves efficiency [231, 232]. Furthermore, the flying capacitor multilevel (FCML) inverter is attracting attention for its ability to significantly reduce the inverter's power loss and weight, thanks to its smaller filters and a compact energy transfer mechanism [233].

Table. 16 summarizes the popular converter topologies in UAM applications.

**Table 16 Summary of popular converter topologies in UAM applications**

|          | Topology       | Isolated | Transformers | Switches | Diodes | Capacitors* | Inductors* |
|----------|----------------|----------|--------------|----------|--------|-------------|------------|
| DC/DC    | DAB            | Yes      | 1            | 8        | -      | -           | -          |
|          | 5L-DAB         | Yes      | 1            | 12       | -      | 2           | -          |
|          | IBCI           | Yes      | 2            | 8        | -      | 2           | -          |
|          | ABAC           | Yes      | 1            | 8        | -      | 2           | 2          |
|          | FSBB           | No       | -            | 4        | -      | -           | 1          |
|          | 3L-FC          | No       | -            | 8        | -      | 2           | 1          |
| Inverter | 2L-inverter    | No       | -            | 6        | -      | -           | -          |
|          | 3L-NPC         | No       | -            | 12       | 6      | 2           | -          |
|          | 3L-ANPC        | No       | -            | 18       | -      | 2           | -          |
|          | 3L-FC inverter | No       | -            | 12       | -      | 5           | -          |
|          | 5L-NPC         | No       | -            | 24       | 18     | 4           | -          |

\* Capacitor or inductor without considering filters

### 3. Future Trends, Challenges, and Emerging Technologies

**Future trends and challenges** In [234], an overall review of the power rating growth in More Electric Aircraft (MEA) over recent decades is presented. Indeed, the trends towards electrifying the more conventional aviation systems, coupled with the addition of secondary electric load demands and the scaling up of aircraft dimensions, leads to a substantial increase in power rating demand. Furthermore, the need for more substantial thrust in larger aircraft and the use of backup electric systems or improved safety further amplifies the power rating demands of All-Electric Aircraft (AEA).

With the increasing power rating demands and progress in electrification, the next generation of on-board power electronics systems must take into account objectives such as high power density, high efficiency, enhanced reliability, and safety. However, the current levels of high power density have not been able to the desired targets. To achieve these goals, significant efforts need to be directed towards designing for higher frequencies, optimizing power electronic components, and improving thermal management solutions.

A higher DC bus voltage is preferred to improve the power density and efficiency of the system. The DC bus voltage significantly impacts the mass of power electronics components, including motors, converters, and cables. Although raising the DC bus voltage brings these benefits, it also introduces complex design challenges for the insulation layer. According to the Paschen's Law, the higher the altitude, the less breakdown voltage, primarily due to the reduced air density, which diminishes its insulating properties in aerospace applications. Consequently, unlike in electric vehicle design, it becomes crucial to place significant emphasis on factors related to insulating materials in the design of electric aircraft. Restrictions on clearance distances must be implemented to ensure safety during high-altitude operations.

**Emerging technologies** As previously pointed out, in order to reach these performance requirements and face these challenges, a number of new technologies are studied and proposed by researchers. These technologies include new semiconductor devices, advanced converter topologies, magnetic component design, and novel thermal management techniques.

In recent years, Wide Bandgap (WBG) devices have seen extensive use in various power electronics applications, due to their superior performance compared to traditional silicon (Si) devices, especially in high-frequency, high-voltage, and high-temperature conditions. This enhancement allows power converters with WBG devices to achieve superior performance, operate at higher frequencies, handle higher voltage, and exhibit lower losses. Among semiconductor materials, Silicon Carbide (SiC) and Gallium Nitride (GaN) have demonstrated the most impressive performance. A detailed review of the properties of both materials is presented in [235]. However, commercially available GaN semiconductors are limited in voltage range, with a maximum of up to 650V. Furthermore, the cost of WBG devices still remains high and WBG devices have not demonstrated significant performance improvements over traditional Si devices in low-voltage, low-power, and low-frequency applications. In [236], a comprehensive study is presented on the impact of WBG devices on power electronic converters. A component cost model for these converter is derived in [237].

Magnetic component design is critical for enhancing the high power density of converters since the mass of inductor significantly contributes to the system's overall mass. With the emergence and development of WBG devices, converters can feasibly operate at switching frequencies above hundreds of kilohertz, paving the way for a significant reduction in the volume of magnetic components. However, higher switching frequencies also present design challenges for magnetic components, including increased core losses, saturation effects, and thermal management issues. The inductor design can be broadly segmented into two critical components: core selection and wire selection. In recent years, planar-wound structures have gained widespread adoption in electric aircraft due to their compact size, good thermal characteristics, cost reduction, and high power density. A overview of planar magnetic technology, which details its advantages and disadvantages, is presented in [238]. Magnetic material selection is crucial in the design of magnetic components as it significantly influences their overall performance, including core losses, mass, and thermal characteristics. A comparison of different magnetic material is presented in [238, 239]. Generally, amorphous cores are the lightest and have the highest saturation flux density. Nanocrystalline cores, with their ultra-thin crystals, can achieve high permeability and flux density. However, both materials are typically used in medium-frequency applications (up to 100 kHz) due to high core losses at higher frequencies. MnZn ferrite materials are commonly used in high-frequency applications (100 kHz to 1 MHz) due to their low core losses. Litz wire has advantages for high frequency application, offering low AC resistance and minimized skin effect at high frequencies [240].

Thermal management of power electronic components is a significant concern in electric aircraft due to their stringent temperature constraints. Most components, such as magnetic components, semiconductor devices, and capacitors, are highly sensitive to temperature variations because temperature has the significant impact on their performance and lifespan. Therefore, in Urban Air Mobility (UAM), effective temperature control is crucial due to the wide range of temperatures, requiring each component to operate within its designated temperature range for optimal performance and

durability.

In [241, 242], main available thermal management methods have been discussed. In most applications, liquid cooling method is preferred due to its high thermal conductivity and capacity for high heat density. Several liquid cooling methods have been developed in [243, 244]. A cryogenically-cooled MW ANPC inverter was developed by General Electric Corporation [244]. However, in eVTOL applications, a forced-air cooling method is feasible, benefiting from lower temperatures and faster wind speeds at high altitudes. Furthermore, when the propellers rotate, the natural wind generated by propeller rotation assists in cooling the heatsink. A 47 kW/L forced air-cooled inverter for electrical aircraft was developed in [245]. A weight optimization method for an air-fan cooling system has been developed in [246].

In addition to the aforementioned new technologies, there are also other methods that are continuously being proposed and researched, targeting to a high power density and high-efficiency powertrain architecture in electric aircraft.

## F. Battery

### 1. Power and Energy Requirements

To size a battery pack for the aircraft, the power requirements and corresponding flight profile have to be determined. The power demand largely depends on aerodynamic features and the gross takeoff mass (GTOM) of the aircraft, where battery pack weight is an important factor to minimize. A typical flight path includes a taxi, takeoff, transition, climb, cruise, descent, landing, and reserve component as discussed in prior works [247–249] and depicted in Figure 1. In a real-life flight, the power requirements for these different phases can be variable depending on environmental factors like weather, headwinds, or required detours. In the simplest case, power requirements for various phases can be estimated and lumped per each defined segment. The highest power requirements are for the takeoff and landing phases and are of the highest concern for the battery pack capabilities. To conduct battery pack sizing in this work, a power profile provided by Aurora Flight Sciences was used (Figure 28), where the total flight mission is 11 minutes. This power profile was modeled after the Aurora Flight Sciences' Pegasus PAV prototype which is a 2-passenger aircraft, with a designed range of 50 miles and a TOGW of 800 kg. The max power is 143.6 kW which is during the hover takeoff/landing phases. The landing phase is of most concern due to the low state of charge and low voltage of the battery pack which means the battery has to deliver more current to meet the power requirements.

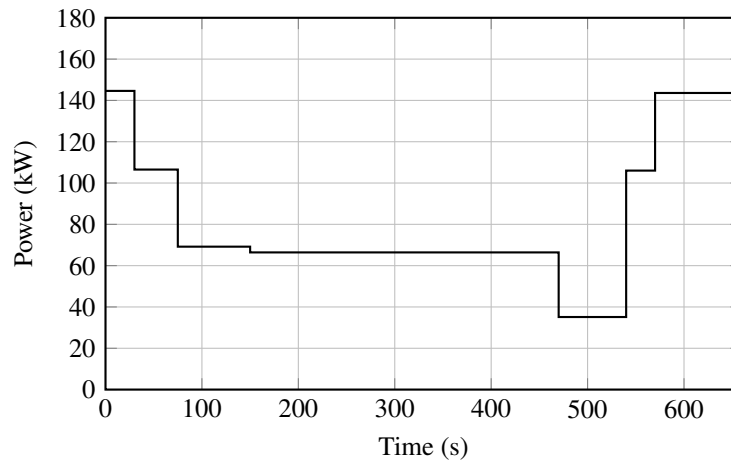


**Fig. 28 Power profile of the PAV prototype mission**

From the power profile, we can estimate the energy requirements for each phase by multiplying the required power by its corresponding time duration. The total required energy for the trip can then be obtained by summing all the components. For this flight profile, the total energy requirement is  $Energy_{trip} = 15.5$  kWh. Using the energy balance method discussed in Section III.F.4, we can estimate the minimum energy requirement of the battery pack. In order to prolong the health of the battery pack and increase the cyclability of the cells by mitigating structural defects within the cathode and irreversible lithium plating on the graphite anode [250], a constraint for the depth of discharge (DoD =

80%) is implemented as shown in Eq. 20.

$$Energy_{\text{pack}} = \frac{Energy_{\text{trip}}}{DoD\%} = 19.34 \text{ kW h} \quad (20)$$

## 2. Battery Pack Sizing

**System voltage** The energy requirement for one flight path is estimated to be 19.34 kW h after upsizing the battery pack energy to prolong the state of health (SoH). This was done by reducing the DoD to 80% rather than subjecting the battery cells to a full discharge. The battery pack capacity can then be determined by dividing the energy by the total system voltage. The total system voltage is chosen from the motor requirements taking into account variables like rotor's rpm and velocity. A typical system voltage for eVTOL aircraft can range from 400 V to 900 V.

**Battery pack capacity** The battery pack capacity required for the mission can be determined by dividing the battery pack energy by the system voltage (Eq. 21). Using this capacity, C-rates for each phase can be estimated from the power and current requirements (Eq. 22 and Eq. 23). Since the trip duration, energy, and therefore capacity are quite low, the resulting C-rates the battery pack has to deliver are very high (Figure 29, beyond the rate capability of most high-capacity cells). Therefore, to reduce the C-rates to more reasonable discharge currents, the battery pack capacity needs to be increased. The battery pack capacity should be increased to accommodate multiple trips to reduce aircraft downtime from charging.

$$Capacity_{\text{pack}} = \frac{Energy_{\text{pack}}}{Voltage_{\text{system}}} \quad (21)$$

$$Current_{\text{phase } i} = \frac{Power_{\text{phase } i}}{Voltage_{\text{system}}} \quad (22)$$

$$C - rate_{\text{phase } i} = \frac{Current_{\text{phase } i}}{Capacity_{\text{pack}}} \quad (23)$$

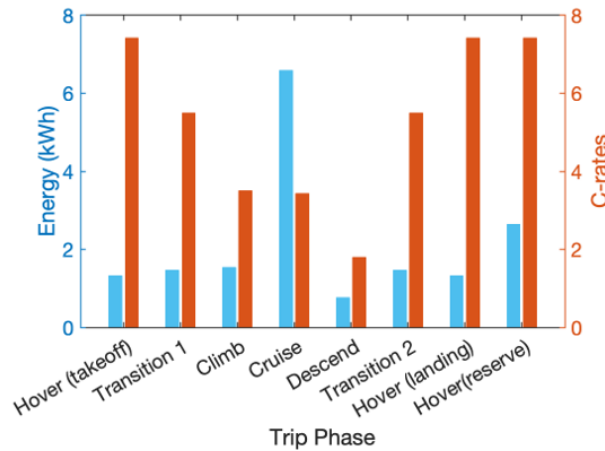


**Fig. 29** Energy and C-rates for each flight phase

In this work, the impact on battery sizing, C-rates, and battery mass fraction with both the upper (900 V) and lower (400 V) voltage limits were evaluated as shown in Figure 30. For these calculations, two different types of cells were chosen for the sizing; energy-optimized (LG M50) and power-optimized (LG HB3) cylindrical 21700 cells which possess  $260 \text{ W h kg}^{-1}$  and  $155 \text{ W h kg}^{-1}$  energy density, respectively. Energy optimized means that the cell possesses high capacity but can only deliver C-rates up to 3C whereas the power optimized cells can deliver C-rates up to 10C albeit with a lower capacity. To estimate the battery pack mass fraction of the aircraft,  $\omega$  (Eq 26), the cells were sized to meet the voltage and capacity requirements by calculating the number of cells in series (Eq. 24) and parallel (Eq. 25).

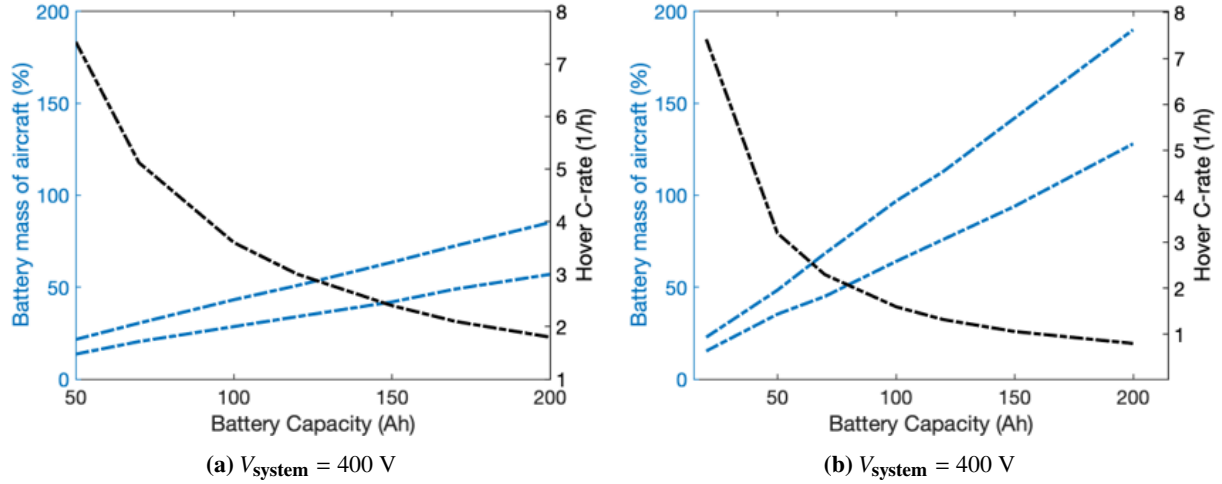


**Fig. 30** Battery mass fraction and maximum C-rates during the hover phase as a function of battery capacity using two different types of cells (LG M50 and LGHB3)

The chemistry used in the chosen cells is NMC cathodes with graphite anodes, where the nominal voltage is 3.7 V. The cell capacity is 5 A h and 3 A h for the energy and power-optimized cells.

$$Cells_{\text{series}} = \frac{Voltage_{\text{system}}}{Voltage_{\text{nominal}}} \quad (24)$$

$$Cells_{\text{parallel}} = \frac{Capacity_{\text{pack}}}{Capacity_{\text{cell}}} \quad (25)$$

$$\omega(\%) = \frac{\frac{Mass_{\text{cells}}}{TOGW}}{CTP} \times 100 \quad (26)$$

To build the battery pack, the cells are packed in modules and arranged to meet the voltage and capacity requirements. The battery pack structure consists of packing material, structural material, and the battery management system, introducing additional weight for the pack. This is called cell-to-pack efficiency (CTP) which can vary depending on the battery pack engineering design. For this work, a CTP of 70% is used.

As expected, using power-optimized cells for sizing can result in higher battery pack mass fraction due to their lower energy density. For the system voltage of 400 V as shown in Figure 30a, the overall battery mass fraction of the aircraft is lower compared to the 900 V system (Figure 30b) due to fewer cells arranged in series to meet the 900 V voltage requirement. This is an advantage of a lower voltage system but is a design constraint implied on the battery pack depending on the motor requirements.

For the higher voltage case, lower capacities can be used to meet the energy demands where 20 A h battery capacity results in the maximum C-rates compared to the 50 A h battery pack capacity for the 400 V case. The ideal sizing scenario is to size the battery pack to satisfy all constraints which are mainly C-rate and battery mass fraction of the aircraft. For reference, the battery mass fraction for a conventional electric vehicle (Tesla Model 3) is noted in Figure 30, which nears around 40%. For electric vehicles, battery mass fraction is not as critical of a factor due to the variance in mission and power requirements which are namely peak power and peak power duration [249]. Therefore, the advancement in battery technology to satisfy energy density and power requirements remains the largest challenge for eVTOLs.

### 3. Challenges for eVTOLs

**High power requirements** As discussed in Section III.F.2 and shown in Figure 28, eVTOLs require large amounts of power during the takeoff and landing components of their flight in respect to the other flight phases. During the takeoff component, large amounts of current is required to satisfy the power demand. In this work, that would be 143.6 kW for 30 seconds. This equates to 359 A needed to be drawn from the battery. At this stage, the battery state of charge is high,

meaning that the voltage is relatively stable to satisfy the takeoff power. However, in respect to the landing phase, this phase is of most concern due to the expected low state of charge of the battery after the flight is near completion. This raises concern for insufficient power during the most critical component of the flight with respect to safety [248, 250]. In addition to safety concerns, not all cell chemistries are equipped to deliver high power. There is often a C-rate limitation associated with high-capacity cells.

**Battery pack mass** The mass of batteries poses a significant challenge for eVTOL aircraft. They require a high power-to-mass ratio and energy-to-mass ratio to ensure efficient vertical takeoff and landing, a crucial factor in their design for urban air mobility and short-distance travel. Batteries, which are essential for storing the electrical energy needed for propulsion, have a relatively low energy density compared to traditional fossil fuels [247]. This means that a substantial amount of batteries is required to provide the necessary power for eVTOL flight, often resulting in a trade-off between battery capacity and aircraft mass. As a consequence, the mass of batteries not only reduces the payload capacity of eVTOLs but also directly impacts the aircraft's overall efficiency, range, and maneuverability. It is worth noting that in the context of eVTOLs, the batteries accounts for roughly 20-25% of the overall design gross weight, a stark contrast to traditional VTOL vehicles, where the fuel comprises just 2.5-5% [247, 248]. This stark difference underscores the unique challenge posed by battery mass in the development of eVTOLs.

**Thermal considerations** Thermal considerations play an essential role in the domain of battery pack design, given their implications for battery performance, safety, and lifespan [251]. Thermal management is indispensable to prevent thermal runaway, which can result in irreversible cell degradation and, in extreme cases, catastrophic failure [252]. Batteries inherently generate heat during charge and discharge cycles, and the accumulation of this heat can lead to accelerated cell degradation and reduced cycle life [253]. Furthermore, elevated temperatures can pose safety hazards, including fire and explosion [254]. Consequently, thermal management systems are imperative, as they enable effective heat dissipation, maintain cells within their specified operating temperature range, and ensure the safe and reliable operation of the battery pack across diverse environmental and operational conditions. This can be accomplished, for example, using passive cooling, heat pipes, or phase change materials [255–257].

#### 4. Battery Modeling

**Approaches to battery modeling** Battery modeling is an essential tool to predict the behavior of batteries during flight. These models can be used, for instance, to predict terminal voltage, state of charge, state of health, and/or temperature increase. Two common approaches are traditionally used to develop models for lithium-ion batteries: either an empirical approach or a physics-based approach [258]. The empirical approaches encompass techniques such as machine learning or equivalent circuit models and are based on experimental data. These models are generally more simple and faster than physics-based models. However, they rapidly lose accuracy for extrapolation outside of the available experimental data points, e.g., as the battery ages [259]. In addition, there is no explicit relationship between the empirical model parameters and the electrochemistry of the battery which limits the amount of meaningful information that can be obtained from these models. On the other hand, physics-based models such as the single particle model (SPM) or Doyler Fuller Newman model (DFN) are derived from conservation laws and intercalation kinetics due to the charge and discharge process [260]. The physics-based models have shown good agreement with experiments [261, 262] but they come at the cost of complexity and computational time.

**Energy balance** As a basic approximation, one can also employ an energy balance method to determine the total energy available in the system, denoted as  $Energy_{\text{pack}}$  (J). This total energy can be calculated as follows:

$$Energy_{\text{pack}} = \eta_{\text{battery}} \rho_{\text{battery}} m_{\text{battery}} \times 3600 \quad (27)$$

where  $\eta_{\text{battery}}$  is the number of cell,  $\rho_{\text{battery}}$  the energy density of the battery, and  $m_{\text{battery}}$  the mass of the cell. Using this computation, you can also determine the remaining energy after a flight, denoted as  $E_{\text{rem}}$ , by using the following formula

$$E_{\text{rem}} = Energy_{\text{pack}} - \int_0^{t_f} P(t) dt \quad (28)$$

where  $P(t)$  is the power profile and  $t_f$  the mission's end time. It is important to note that this method offers a simplified approach and does not provide detailed insights into cell behavior, which can result in significant errors. Nevertheless, it possesses the advantage of simplicity and can be used for initial estimations and sizing purposes before more sophisticated and accurate techniques like equivalent circuit models or physics-based models are employed.

**Equivalent circuit models** Equivalent circuit models (ECMs) serve as simplified representations of battery behavior, offering a practical alternative to physics-based models. ECM are usually selected for their ease of implementation and speed, making them suitable for battery management systems [263, 264]. The underlying concept involves creating an electrical circuit that mimics the battery's response, with the number of electrical components chosen based on the desired level of accuracy. The Thevenin ECM consists of a voltage source with one electrical resistance and a resistor-capacitor block and has for instance been used to model the batteries onboard NASA X-57 Maxwell aircraft [265]. The electrical components can vary with factors like temperature, state of charge, and state of health. However, incorporating more parameters necessitates additional experimental testing to tune the components, leading to increased costs and data collection time. Typically, pulse tests involving discharge and rest cycles are conducted to gather the required data from the battery. Subsequently, optimization techniques [263] or analytical methods [265], which can be automated with tools like MATLAB or Python, are used to extract the circuit parameters from each pulse test, facilitating the construction of the ECM.

**Physics-based models** Physics-based battery models are rooted in fundamental principles, specifically the conservation laws governing molar conservation of lithium, electronic and electrostatic charge conservation within the system, and the description of electrochemical kinetics that describe reaction rates. These models include notably the Doyle-Fuller-Newman model (DFN) [266, 267] and the single particle model with or without electrolyte (SPMe/SPM) [268]. While the DFN model is considered a high-fidelity model standard for physics-based battery modeling, the SPM and SPM<sub>e</sub> offer simplified alternatives. The DFN model is computationally intensive, involving the solution of a nonlinear system of equations. On the other hand, the SPM and SPM<sub>e</sub> models can be up to two orders of magnitude faster than the DFN with the same discretization, with a root mean square voltage error of less than 20 mV in comparison to DFN results for constant discharge rates equal to or less than 1 C [268]. Despite their inherent complexity, physics-based models offer valuable insights into the electrochemical behavior of the studied battery and can be extended, for instance to incorporate degradation mechanisms. They are accessible through commercial software such as COMSOL Multiphysics [269] and open-source software like PyBaMM (Python Battery Mathematical Modelling) [270].

## 5. Results, Validation, and Verification

The physics-based electrochemical model was used to run three different test cases with increasing battery pack capacity and their corresponding battery mass fraction (Figure 31). The electrochemical models are calibrated for the LGM50 21700 cylindrical cell, which is an energy-optimized cell used in Section III.F.2 to complete the battery sizing. The starting parameters for this particular cell were tuned to match constant current discharge experimental data. Mainly the SEI resistance and thickness were altered to match the cell polarization at various discharge currents. In addition, for these simulation cases, it is assumed that the cells are at 100% SOC. In practice, this is experimentally done by charging to 4.2 V and applying a voltage hold until a leakage current of 250 mA is measured. It is also assumed that the cells are in air with no additional cooling. Therefore, the heat transfer convection coefficient is just for ambient conditions.

As battery pack capacity increases, the C-rates subjected on the cells decrease as shown in Figure 30. This is because as the battery pack capacity increases, less current and time is required to discharge the battery to meet the power requirements. In addition to the three test cases, three different electrochemical models with varying levels of fidelity were used to evaluate the cell voltage and thermal responses under the flight protocol. The voltage and thermal response were chosen as critical parameters to evaluate the performance of the cell where the voltage demonstrates its capability to complete the mission and thermal response measured.

For the 100 A h case shown in Figure 31a, we can see that the DFN and SPM<sub>e</sub> model was unable to complete the flight. Since both DFN and SPM<sub>e</sub> models capture electrolyte degradation dynamics, this resulted in higher polarization and cell resistance simulated during the takeoff phase. The maximum C-rate for this test case was 3.6 C which is beyond the rated capability of the LGM50 21700 cylindrical cell. The SPM model was able to "complete" the flight but we can see that even the SPM model predicts the cell hitting the lower voltage cutoff during the takeoff phase. Therefore, the 100 A h case subjected to 3.6 C is beyond the capability of the cell even though the thermal response is negligible.

Both 150 A h and 200 A h battery pack capacity cases result in low enough C-rates that the models can simulate the mission being completed. For the 150 A h case (Figure 31b), 2.4 C is used during the takeoff and landing phases. Both the DFN and SPM<sub>e</sub> models deliver comparable results, meaning that the SPM<sub>e</sub> is sufficient to simulate the cell response compared to the higher fidelity model. The thermal response for both DFN and SPM<sub>e</sub> result in a 18 °C increase in the cell surface temperature after the mission is completed. It is clear that higher the C-rate, higher the temperature rate. For the SPM model, the cell polarization and thermal response is lower than the DFN and SPM<sub>e</sub> models. This is again

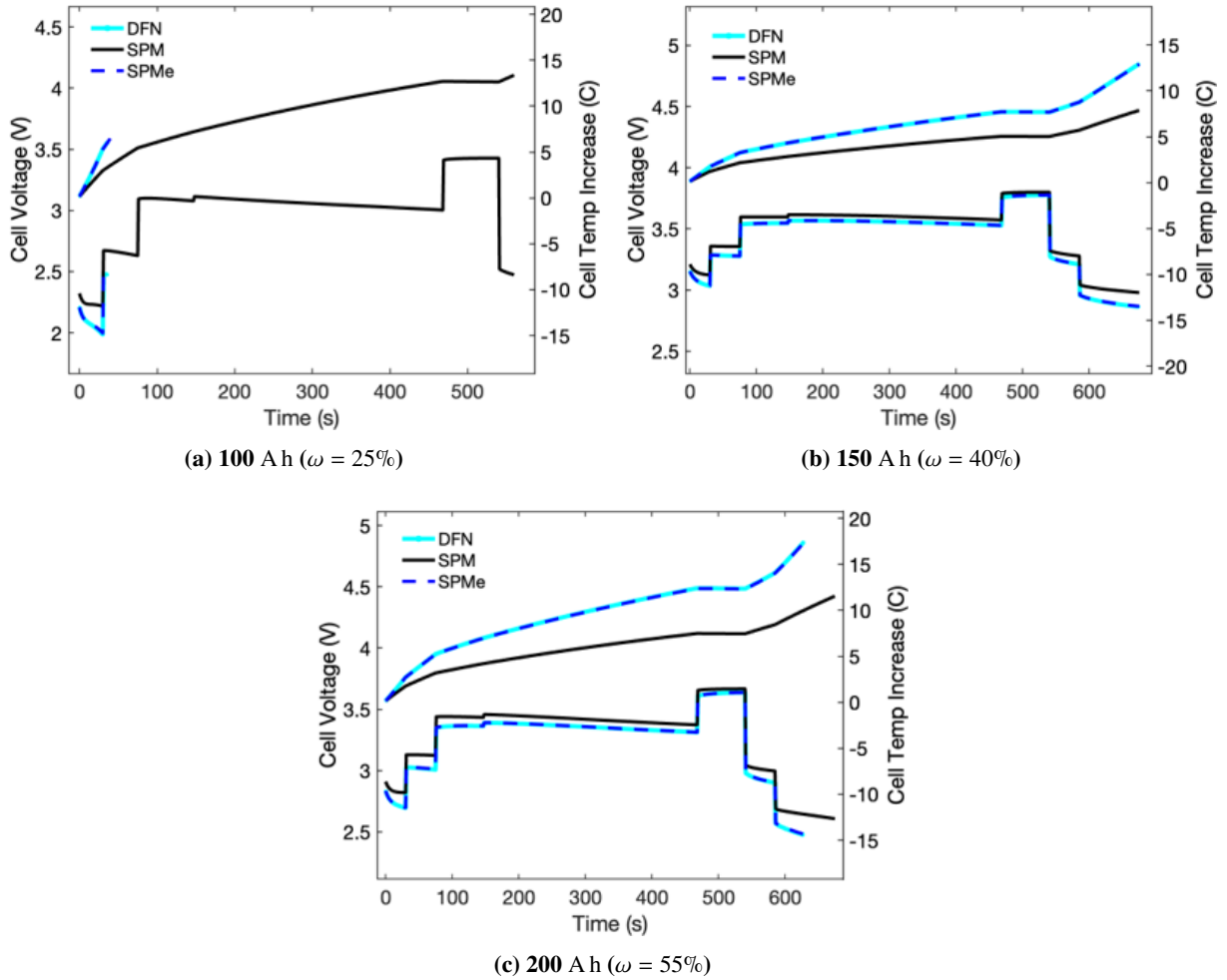


**Fig. 31** Simulation cases using the DFN, SPMe, and SPM electrochemical models of the battery undergoing three different discharge protocols where cell voltage and thermal response were evaluated

due to the fact that SPM does not capture electrolyte dynamics and therefore underpredicts the  $iR$  drop during the high C-rate phases. Lastly, the 200 A h battery pack capacity case albeit with the highest battery mass fraction is the least intense discharge protocol with only 1.8 C required during the takeoff and landing phases (Figure 31c). This is also evidenced by the minimal cell surface temperature increase which is near 13 °C.

These results validate the battery sizing using the LG M50 21700 cylindrical cell with either the 150 A h or 200 A h case, resulting in a battery mass fraction of the aircraft of 40% and 55% respectively. The DFN, SPMe, and SPM electrochemical models simulate the corresponding discharge protocol, demonstrating that 2.4 C is near the maximum allowed C-rate for this particular chemistry. In addition to the voltage response, the thermal effects were also considered which were below the safety threshold. Future work requires additional calibration for other cell types to provide full screening capability. It must be noted that these models are modeling the behavior of a single cell where battery management systems were not included but should be considered for real implementation.

#### IV. Discussion and Future Work

The present study has provided a critical examination of the various computational models that underpin the design and optimization of eVTOL aircraft within the rapidly evolving sector of Urban Air Mobility (UAM). The use of Multidisciplinary Analysis Design and Optimization (MDAO) successfully addresses the limitations of historical empirical equations, establishing a foundation for the application of physics-based tools in the early design stages.

**Aerodynamics and Propulsion** This study highlights the breadth and complexity of eVTOL aerodynamic analysis and presents preliminary results of individual eVTOL aircraft components. This complexity is further amplified by the interactions among various components. The application of the vortex particle method, as shown in our study, offers a promising compromise—delivering accurate physical representations at significantly reduced computational costs compared to traditional methods. A comprehensive examination of the entire aircraft, involving challenging flight regimes such as hover and transition flight, and wake interaction between aircraft components is necessary to fully characterise the aerodynamic performance of the UAM aircraft.

The current V&V have concentrated on the lifting surfaces and rotor blades, showcasing the reliability and accuracy of our low-to-mid-fidelity tools. Future endeavors will extend this V&V to encompass comparisons with higher fidelity CFD models and experimental data, thereby enhancing confidence in the simulated results. Additionally, the intricate wing-propeller interactions present a fertile ground for further study, promising to refine our understanding of eVTOL performance and operational envelope.

**Structures** In addressing the structural intricacies of eVTOL design, this study explored modeling approaches: beam and shell models. Beam models have demonstrated their suitability for initial structural sizing of eVTOLs, particularly for designs with higher aspect ratios. Their computational efficiency enables the evaluation of dynamic load conditions, an essential factor in eVTOL design. However, this approach shows limitations when applied to low aspect ratio configurations or when attempting to model complex structural features such as ribs and cutouts. Although advanced beam models can incorporate features like stiffened composite skins and spars, they do so at the cost of increased computational resources. For the conceptual design stages, where quick estimations are often required, beam models might suffice to provide a baseline understanding of the structural behavior. However, these models should be validated against more detailed analyses as the design progresses, ensuring that any discrepancies can be addressed before finalizing the structural configuration.

Conversely, shell models stand out in the preliminary stages of design, where precision in weight estimation becomes critical. Despite their higher computational demands, which necessitate the simplification of dynamic loads into static critical loads, they offer superior accuracy for detailed structural analysis.

Our current V&V work, which focused on an unstiffened aluminum wingbox under static loads, confirmed the efficacy of beam models for early-stage design. Moving forward, our work will test more dynamic load scenarios and explore stiffened composite structures. We will also include considerations for strength and buckling failure criteria to provide a more comprehensive understanding of eVTOL structural integrity.

**Acoustics** Acceptance of eVTOLs is deeply intertwined with the societal need to mitigate noise pollution, particularly in urban environments where these aircraft are intended to operate. Tonal and broadband noise are both important in the context of UAM operations. This work presents low- and medium-fidelity noise prediction models appropriate for use within a MDAO framework. The findings from our work indicate that LO model for tonal noise prediction, along with the GL spectrum model and the BPM model for broadband noise, effectively spans the various operational regimes of eVTOLs, from hover to forward flight conditions. These models demonstrate good agreement with experimental data, reinforcing their viability for preliminary design assessments. Future work will continue to refine these models, especially the GL spectrum model, to improve their applicability across various flight conditions. Tonal and broadband noise predictions can also be further enhanced by physics-based and acoustic analogy models such as Ffowcs Williams and Hawkings (FW-H) equation and Amiet's model, respectively.

**Motor and Powertrain** The propulsion of eVTOLs relies fundamentally on electric motors, with the design and optimization of these motors being central to achieving efficient thrust generation. This work has reviewed multiple motor modeling approaches, each with its distinct set of trade-offs between fidelity and computational demands. An analytical model (AM) combined with a magnetic equivalent circuit model (MEC) emerges as an effective hybrid strategy. The MEC model approach is specifically tailored for radial Permanent Magnet Synchronous Motors (PMSM). This model, while effective for PMSMs, does not apply to other motor types such as Switched Reluctance Motors (SRM), Induction Motors (IM), or Axial PMSMs, delineating its scope within the broad spectrum of electric motor technologies. Currently, the model is adept at analyzing the average performance of PMSMs over one electrical cycle in a magnetic-static context. However, it does not delve into the transient performance characteristics of the motor, such as the detailed distribution of magnetic flux density or flux distribution maps. These aspects of transient performance are critical for a comprehensive understanding of motor behavior under varying operational conditions and are earmarked

for future development within our model. Additionally, the current iteration of the MEC model assumes a solid rotor configuration, thereby not accounting for the potential effects of rotor holes which can significantly influence the motor's performance. This simplification, while facilitating the current analysis, points to an area for enhancement in future iterations of the model to encapsulate a broader range of rotor designs and their respective impacts on motor performance. Overall, the MEC strikes a balance between detailed physical representation and computational tractability rendering it particularly suitable for inclusion in early-stage MDAO frameworks.

The powertrain system's role extends beyond mere thrust generation; it is also pivotal in defining the mission power profile, which, in turn, informs the sizing of the battery. The necessity for system redundancy is also highlighted, underpinning the reliability and safety of eVTOL operations. Our current modeling approach for the powertrain components leverages equivalent circuit models to deliver estimations of system efficiency and weight that align well with real-world data.

Looking forward, the research will delve into higher-fidelity powertrain component modeling within the MDAO framework. Further, this will include a thorough investigation into the impact of component failures and the development of strategies to mitigate their severity, ensuring the resilience and reliability of eVTOL systems.

**Battery** eVTOL batteries present unique challenges that stem from the operational profile of these aircraft. High discharge currents, prolonged peak power demands, fast charging capabilities, and intensive utilization cycles distinguish eVTOL batteries from their automotive and consumer electronics counterparts. This work presents a simplified methodology for battery sizing that aligns with the power profile of the mission—this method stands as a practical tool for early-stage design considerations.

For a deeper dive into battery behavior, our study has assessed both the SPM and DFN methods. The validation against experimental data for low C-rate cells confirmed the accuracy of our models, providing a solid foundation for their application in UAM concept analysis. Yet, the high C-rate cells that are typical of eVTOL applications pose additional challenges.

**Summary and Future Work** Future research is thus directed towards optimizing these battery models for high C-rate scenarios to better reflect the demanding conditions of eVTOL usage. By comparing model predictions with high C-rate experimental data, we aim to refine the fidelity of our battery models, ensuring they not only contribute to the safety and efficiency of eVTOLs but also support the rapid progression of UAM initiatives.

Our study has not only provided a comprehensive survey of the computational models pertinent to eVTOL design but has also contributed to the verification and validation of a toolset under the NASA University Leadership Initiative (ULI) project. Table 17 provides a summary of the models.

**Table 17 Summary of computational models for eVTOL MDAO**

| Discipline   | Current Models                         | Future Models     |
|--------------|----------------------------------------|-------------------|
| Aerodynamics | VLM, VPM                               | UVLM, CFD         |
| Structures   | Beam, Shell                            | Higher-order beam |
| Propulsion   | BEMT, VPM                              | Pitt-Peters       |
| Acoustics    | GD, BM, H, LO, DH, SKM, SG, P, GL, BPM | FW-H, Amiet       |
| Motor        | AM, MEC, FEA, Hybrid                   | -                 |
| Powertrain   | AM, ECM                                | -                 |
| Battery      | Energy sizing, SPM, SPMe, DFN          | ECM               |

This project has set the groundwork for future efforts, which will extend beyond discipline-level V&V to include sub-system and system-level evaluations. At the sub-system level, our V&V efforts will explore trim at mission segments, structural sizing, rotor-acoustic optimization, and trajectory optimization. Meanwhile, at the system level, we aim to advance mission analysis and vehicle sizing.

Our collaboration with Aurora Flight Sciences has set the stage for an extensive V&V campaign. This endeavor will entail the rigorous comparison of our toolset with industry-standard tools, experimental data, and the as-built Aurora Flight Sciences' PAV prototype. This will not only validate the effectiveness of our models but will also serve to benchmark our findings against proven industry concepts.

## V. Author Contributions

- **Conceptualization, Methodology, Writing - Original Draft, Writing - Review & Editing:** John Hwang, Darshan Sarojini, Lauren Wolfe
- **Software, Formal Analysis, Investigation, Validation, and Writing - Original Draft: *Discipline-level***
  - **Aerodynamics and Propulsion:** Ryan Anderson, Cibir Joseph, Marius Ruh, Lauren Wolfe, Jiayao Yan
  - **Structures:** Robin Despains, Rachael Doring, Joshua J. Krokowski, Nicholas Orndorff, Darshan Sarojini, Sebastiaan PC van Schie, Michael Warner, Ru Xiang, Han Zhao
  - **Aeroacoustics:** Hyunjune Gill, Luca Scotzniovsky
  - **Motor and Powertrain:** Zeyu Cheng, Luca Scotzniovsky
  - **Battery:** Ashley Cronk, Alexandre Guibert
- **Supervision and Project Administration:** Jeffrey T. Chambers, Jiun-Shyan Chen, John Hwang (lead-PI), Alicia Kim, Seongkyu Lee, Shirley Meng, Chunting Mi, Andrew Ning, Christopher Silva

## A. Description of Test Case Concept: Aurora Flight Sciences' Passenger Air Vehicle (PAV) Prototype

This work was done in collaboration with Aurora Flight Sciences, leveraging their expertise in eVTOLs. A key aspect of this collaboration has been the development of a test case model inspired by Aurora Flight Sciences' Passenger Air Vehicle (PAV) prototype. It is important to note that our model does not replicate the actual PAV prototype, but rather draws inspiration from it. To ensure the protection of Aurora Flight Sciences' proprietary information, our approach has been to utilize publicly available data and images of the PAV prototype as a foundation for creating an analogous yet distinct model. This approach allows us to explore design and optimization strategies while respecting intellectual property considerations.

The PAV prototype is a battery-powered electric VTOL concept. It was first flown on January 22, 2019. It's a lift-plus-cruise concept with eight fixed-pitch variable-RPM propellers during vertical segments, and one fixed-pitch variable-RPM pusher-propeller for horizontal segments (Figure 32). The specifications of the PAV concept are provided in Table 18.

| Parameter             | Value         | Units |
|-----------------------|---------------|-------|
| Passengers            | 2             |       |
| Payload               | 225           | kg    |
| Range                 | 80            | km    |
| TOGW                  | 800           | kg    |
| Cruise altitude (AGL) | 182.88        | m     |
| Cruise velocity       | 50.07         | m/s   |
| Total length          | 9.144         | m     |
| Motor output          | $8 \times 75$ | kW    |

**Table 18** Concept specifications

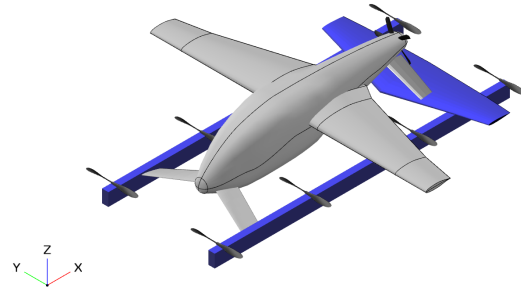


**Fig. 32** Isometric view of the Aurora Flight Sciences' PAV prototype

### A. Structures

This section provides the details of the structural wingbox inside the main wing. The front spar is located at 25% chord, the rear spar at 75% chord, and the beam axis at 35% chord (Figure 14a).

For the box-beam, the width of the wingbox is half the chord length and its height is computed at 25% chord based on the NASA Langley LS-0417. The structure is subjected to a 3g load condition at cruise ( $h = 600$  ft AGL and  $Ma = 0.1474$ ). Table 19 provides the data at each node of the beam.

**Table 19 Wingbox beam model**

| x (ft) | y (ft) | z (ft) | Width (in) | Height (in) | Web thickness (in) | Cap thickness (in) | z-force (lbf) |
|--------|--------|--------|------------|-------------|--------------------|--------------------|---------------|
| 11.05  | 0.00   | 1.98   | 38.22      | 12.29       | 0.05               | 0.05               | -8.47         |
| 11.05  | 1.40   | 1.98   | 38.21      | 12.27       | 0.05               | 0.05               | 316.48        |
| 10.79  | 2.87   | 1.98   | 33.79      | 10.78       | 0.05               | 0.05               | 310.45        |
| 10.62  | 4.21   | 1.99   | 30.92      | 9.93        | 0.05               | 0.05               | 285.83        |
| 10.47  | 5.61   | 1.99   | 28.37      | 9.11        | 0.05               | 0.05               | 275.54        |
| 10.32  | 7.02   | 1.99   | 25.81      | 8.29        | 0.05               | 0.05               | 268.93        |
| 10.17  | 8.42   | 1.99   | 23.25      | 7.47        | 0.05               | 0.05               | 259.23        |
| 10.02  | 9.82   | 1.99   | 20.70      | 6.65        | 0.05               | 0.05               | 249.49        |
| 9.89   | 11.22  | 1.99   | 18.34      | 5.89        | 0.05               | 0.05               | 251.26        |
| 9.79   | 12.63  | 1.99   | 16.69      | 5.36        | 0.05               | 0.05               | 222.15        |
| 9.69   | 14.03  | 1.99   | 15.03      | 4.83        | 0.05               | 0.05               | 175.07        |

### B. Wing and Tail Geometry

In developing the wing and tail geometry of our model, we aimed to approximate the planform of the PAV as closely as possible within the constraints of publicly available information. The images of the PAV provided a basis for our design, allowing us to infer a reasonable similarity in planform. However, we diverged from the PAV in our choice of airfoil, opting for a standard profile rather than the custom airfoil used in the actual PAV. Additionally, due to the lack of available data on the PAV's twist and dihedral angles, these aspects were not incorporated into our model.

**Table 20 Wing planform parameters**

| Parameter          | Value   | Unit           |
|--------------------|---------|----------------|
| Aspect ratio       | 6.39    |                |
| Span               | 8.53    | m              |
| Leading-edge sweep | 0.0     | deg            |
| Taper ratio        | 0.39    |                |
| Planform area      | 11.39   | m <sup>2</sup> |
| Dihedral           | 0.0     | deg            |
| Geometric twist    | 0.0     | deg            |
| Airfoil            | LS-0417 |                |

**Table 21 Tail planform parameters**

| Parameter                                        | Value     | Unit           |
|--------------------------------------------------|-----------|----------------|
| Aspect ratio                                     | 5.82      |                |
| Span                                             | 5.17      | m              |
| Leading-edge sweep                               | 30.0      | deg            |
| Taper ratio                                      | 0.43      |                |
| Planform area                                    | 4.6       | m <sup>2</sup> |
| Dihedral                                         | 0.0       | deg            |
| Vertical separation<br>(from wing LE to tail LE) | 0.85      | m              |
| Airfoil                                          | NACA 0010 |                |

### C. Pusher and Lift Rotor Geometry

The PAV prototype has eight fixed-pitch variable-RPM lift propellers and one fixed-pitch variable-RPM pusher propeller. The rotor geometry presented a unique challenge, as there is no public information available about the specific rotor designs employed in the PAV. To address this, we designed notional lift and pusher rotors, which were then used for subsequent rotor (Section III.A) and acoustic (Section III.C) analysis. The development of these notional rotors, while inspired by the PAV, was conducted independently, ensuring that our model remains a distinct entity, separate from Aurora Flight Sciences' proprietary designs.

The geometric characteristics of the lift rotor are given in Table 22.

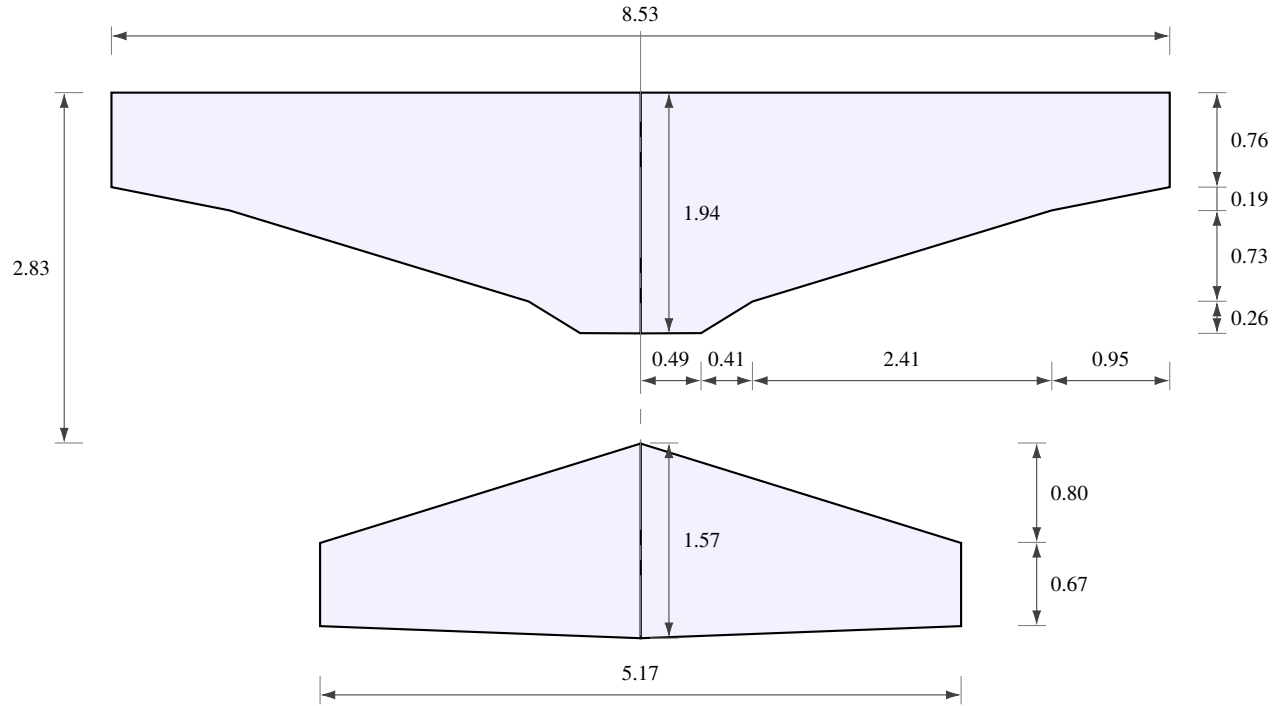


**Fig. 33 Wing and rear stabilizer geometry. All dimensions in meters.**

**Table 22 Lift propeller specifications (in hover)**

| Parameter                 | Value     | Unit |
|---------------------------|-----------|------|
| Number of blades          | 2         |      |
| Rotor radius              | 0.911     | m    |
| Hub radius                | 0.182     | m    |
| Figure of merit           | 0.75      |      |
| Thrust coefficient, $C_T$ | 0.0086    |      |
| Solidity                  | 0.0524    |      |
| Airfoil                   | NACA 4412 |      |

**Table 23 Pusher propeller specifications**

| Parameter                 | Value     | Unit |
|---------------------------|-----------|------|
| Number of blades          | 5         |      |
| Rotor radius              | 0.6096    | m    |
| Hub radius                | 0.1219    | m    |
| Rotor efficiency          | 0.8       |      |
| Thrust coefficient, $C_T$ | 0.0105    |      |
| Solidity                  | 0.0615    |      |
| Airfoil                   | NACA 4412 |      |

The following optimization problem was solved to obtain the chord and twist distributions for the lift rotor:

$$\begin{aligned}
 &\max_{\text{chord}(r), \text{twist}(r)} \quad \text{Figure of merit} \\
 &\text{s.t.} \quad \text{thrust} = 2000 \text{ N} \\
 &\quad \quad \text{torque} \leq 160 \text{ Nm} \\
 &\quad \quad \text{rpm} = 2800
 \end{aligned} \tag{29}$$

The propeller geometry obtained as a solution to the optimization problem is given in Table 24. Similar information about the pusher propeller is given in Table 23.

$$\begin{aligned}
 &\max_{\text{chord}(r), \text{twist}(r)} \quad \text{Efficiency} \\
 &\text{s.t.} \quad L/D = 8 \\
 &\quad \quad \text{torque} \leq 160 \text{ Nm} \\
 &\quad \quad \text{rpm} = 4000
 \end{aligned} \tag{30}$$

**Table 24 Lift and pusher rotor geometry**

| <b>Lift Rotors</b> |                   |                    | <b>Pusher Rotor</b> |                   |                    |
|--------------------|-------------------|--------------------|---------------------|-------------------|--------------------|
| <b>r/R</b>         | <b>Chord (in)</b> | <b>Twist (deg)</b> | <b>r/R</b>          | <b>Chord (in)</b> | <b>Twist (deg)</b> |
| 0.22               | 4.55              | 7.78               | 0.22                | 1.10              | 49.63              |
| 0.25               | 5.42              | 8.93               | 0.25                | 1.23              | 46.17              |
| 0.28               | 6.03              | 10.08              | 0.28                | 1.33              | 42.95              |
| 0.31               | 6.37              | 11.23              | 0.31                | 1.42              | 39.96              |
| 0.34               | 6.44              | 12.38              | 0.34                | 1.49              | 37.20              |
| 0.38               | 6.25              | 13.54              | 0.38                | 1.54              | 34.68              |
| 0.41               | 5.80              | 14.69              | 0.41                | 1.58              | 32.40              |
| 0.44               | 5.25              | 15.84              | 0.44                | 1.60              | 30.36              |
| 0.47               | 4.77              | 16.99              | 0.47                | 1.59              | 28.55              |
| 0.50               | 4.35              | 18.14              | 0.50                | 1.57              | 26.97              |
| 0.54               | 4.01              | 19.30              | 0.54                | 1.54              | 25.64              |
| 0.57               | 3.73              | 20.45              | 0.57                | 1.48              | 24.54              |
| 0.60               | 3.52              | 21.60              | 0.60                | 1.41              | 23.67              |
| 0.63               | 3.35              | 22.75              | 0.63                | 1.32              | 22.91              |
| 0.66               | 3.20              | 23.90              | 0.66                | 1.24              | 22.13              |
| 0.70               | 3.05              | 25.06              | 0.70                | 1.16              | 21.32              |
| 0.73               | 2.92              | 26.21              | 0.73                | 1.09              | 20.49              |
| 0.76               | 2.79              | 27.36              | 0.76                | 1.01              | 19.63              |
| 0.79               | 2.68              | 28.51              | 0.79                | 0.94              | 18.75              |
| 0.82               | 2.54              | 29.66              | 0.82                | 0.87              | 17.85              |
| 0.86               | 2.33              | 30.82              | 0.86                | 0.80              | 16.92              |
| 0.89               | 2.06              | 31.97              | 0.89                | 0.73              | 15.96              |
| 0.92               | 1.72              | 33.12              | 0.92                | 0.66              | 14.99              |
| 0.95               | 1.31              | 34.27              | 0.95                | 0.60              | 13.99              |
| 0.98               | 0.83              | 35.42              | 0.98                | 0.54              | 12.96              |

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