

Establishing Fluid Dynamics Scales Critical to Dynamic Interface Applications and their Impact on Handling Qualities

Final Report

Period of Performance: 3/4/2021 – 3/3/2024

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Under Contract No. N00014-21-C-1044

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May 3, 2024

Abstract

Understanding of the aeromechanics, flight dynamics and handling qualities of a rotorcraft flying through a disturbance field, such as a ship airwake, is limited. Research suggests that the rotorcraft's response is fundamentally different to that of fixed wing aircraft, but very little is understood about the relationship between the disturbance field's length and time scales and the rotorcraft's response. The goal of the effort was to improve our understanding of this interaction.

Please note: This is the Distribution A version of CDI Report 24-01; it has had all Distribution D tiltrotor results removed to make it Distribution A.

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Summary

The ship-helicopter dynamic interface (DI) represents a complex environment where a helicopter operates near a moving ship that is excited by sea state and sheds highly unsteady airwake, which interacts with the airframe and rotor wake and can significantly increase pilot workload. This complex environment is typically analyzed using flight simulation. However, application of flight simulations tends to be ship-helicopter configuration specific and a fundamental understanding of what is “good enough” when choosing between modeling fidelity levels for such simulations is currently lacking. This effort aimed to investigate both ship airwake and rotorcraft response from a fundamental physics perspective to obtain generalizations that can provide high-level quantitative insights into modeling fidelity requirements for the DI associated with any given rotorcraft-ship configuration without resorting to expensive simulations. We examined both ship airwake and rotorcraft response to simplified disturbances.

Ship airwake characterization was approached using two methodologies that used CFD simulations of flow over a cube, which is a simplified superstructure representation, as the basis. The first approach entailed fitting two-dimensional parametric analytical models onto flow features obtained from the CFD simulations. Model input parameters provided insights into physical phenomena such as recirculation and could be nondimensionalized and applied to other rectangular configurations not considered in the parameter derivation. In the second approach, three-dimensional flow features were directly characterized as vortical structures in terms of geometry (position, size) and strength. We characterized the arch vortex typically seen aft of rectangular obstacles in headwind flow, as well as a horseshoe vortex that adequately captured vertical velocity profile downstream of the obstacle using this characterization. For the latter, we demonstrated the applicability of the characterization to flow over generic ship geometry derived using publicly available ship geometry data. Both models provided avenues for fast and representative reconstruction of time-averaged velocities associated with flow over rectangular surfaces. These, in turn, could be combined with unsteady predictions from a stochastic model such as the von Karman or Dryden turbulence model to obtain representative time-dependent airwake data. For unsteadiness, the nondimensional vortex shedding frequency or the Strouhal number is an important scaling parameter.

Rotorcraft responses to simplified disturbances were considered for varying levels of fidelity for both rotor dynamics and disturbance modeling. Modeling fidelity for rotor dynamics ranged from analytical blade element theory-based responses where disturbance effects were integrated, to full aircraft dynamics where the disturbances were modeled both as a frozen field (fixed in space and not interacting/distorting with the aircraft) and as a distorting field. Two simplified disturbances were considered: an isolated vortex tube representative of the trailing wake from a fixed-wing aircraft and a horseshoe vortex derived from the ship airwake characterization investigation. Inclusion of full aircraft dynamics was found to attenuate rotor thrust and flapping response for both disturbance types, when compared to isolated analytical responses. Results also pointed to the existence of scaling parameters that can “collapse” responses of different types of rotors onto each other. With regards to disturbance modeling fidelity, rotorcraft response vary depending on the type of disturbance considered: for the isolated straight vortex, the frozen assumption results in more severe responses at low forward speeds and is only deemed adequate at advance ratios greater than or equal to 0.1, and for the horseshoe vortex, rotor responses in the distorting cases were less than those from the frozen cases for all dimensions considered.

Introduction

One of the most demanding tasks for naval aviators is landing on a moving flight deck in high sea-states (i.e. the dynamic interface (DI) problem - see Figure 1). This task is made even more difficult by aerodynamic disturbances at the landing spot from the flow around the ship's bow, superstructure and deck edges. This highly unsteady ship airwake can lead to significant pilot workload. Flight simulation has long been recognized as a valuable tool for augmenting engineering development and pilot training in DI operations, however, it is most effective when the underlying simulation model has appropriately characterized the complex aerodynamic interactions between the rotorcraft and ship airwake. Given the complexity of the problem, a fundamental difficulty when assessing the simulation approaches is the quantification of "good enough", both from the standpoint of understanding and predicting the underlying physics and also with regards to trainer fidelity (i.e. can the pilot feel/tell a difference). The premise of the proposed effort is to quantify "good enough" with regards to understanding the fundamental aero-physics of a rotorcraft interacting with an external disturbance field to quantify which length and time scales - such as those present in a ship airwake or in the wake of a an upstream aircraft during formation flight/refueling - directly impact the aircraft's fundamental response and flying qualities along with the aeromechanics modeling fidelity required to simulate interactions adequately.



Figure 1: DI scenarios: landing on a moving deck (left), night operations (center) and approaching a pair ships (right)

Several ONR funded efforts that seek to quantify "good enough" have recently completed, or are underway or have recently concluded, at the University of Maryland (UMD), Georgia Institute of Technology (GIT), and NAVAIR, where the academic researchers are focusing on understanding the response of a wing to the wake shed by canonical structures [1] and the latter (Generalized Airwake Goodness Evaluation) program seeks to represent the entire DI scenario with a variety of methods. This effort does not seek to duplicate that work, rather to complement it by focusing on several fundamental unknowns in the rotorcraft community, namely:

1. What spatial and temporal scales, present in a disturbance field, matter from flight dynamics and flying qualities standpoints? Beyond the obvious constraints (i.e. larger than a rotor radius and those that induce velocities larger than the wake induced velocity), how does a rotor respond to different size disturbances and how does the rotor type influence this? For example, in general, a rotor acts to filter the influence of a disturbance field, but the level of filtering will vary significantly between an articulated flexible rotor (H-60) and a gimbaled stiff one (V-22). Moreover, rotors typically respond $\sim 90^\circ$ out of phase of the disturbance, whereas fixed aerodynamic surfaces response directly in-phase, which results in a fundamentally different response between rotorcraft types (i.e. helicopter vs. tiltrotor).

2. How do these scales vary when the effects on the full aircraft are accounted for? The primary response from the helicopter will be dominated by that of the rotor, though there may still be significant response associated with the disturbance induced flow on the fuselage, empennage and tail rotor.
3. How do these scales vary with aircraft configuration/type (i.e. conventional helicopter vs. tiltrotor)? The primary response from the helicopter will be dominated by that of the rotor, whereas the tiltrotor may respond primarily in a fixed wing manner associated with the induced flow on the wing. Of course, the tiltrotor's response will also vary with nacelle angle, and it is well known that the V-22's response to wake disturbances (i.e. during formation flight) is quite different to other rotorcraft in the Navy's inventory [2].
4. How do these scales vary with aircraft flight condition, and can valid modeling simplifications be made (i.e. distorting vs. classical frozen disturbance field)? Work by Whitehouse and Brown for helicopter rotors [3-7] suggests that for high speed flight, the traditional frozen field (superposition) assumption may be adequate, but that at the low speeds associated with DI operations, a distorting disturbance field and wake are required because the response is critically different.

Developing an understanding of the fundamental aircraft aeromechanic response, flight dynamics, and flying qualities to the unknowns outlined above would provide great utility to the community with regards to establishing the level of modeling fidelity required to accurately simulate disturbance interactions, the level of fidelity required to be output by Computational Fluid Dynamics (CFD) simulation generated ship airwake databases for training scenarios, and the quantification and specification of handling qualities to types of disturbance fields that can be used to define future and ongoing training and aircraft performance requirements and specifications, such as ADS-33 [8].

Future Naval Relevance

This effort directly supports ONR's long term goals for the Naval Air Warfare and Weapons (Code 35) Department's Sea-Based Aviation National Naval Responsibility – Air Vehicle Technology goals by developing a detailed understanding of fundamental aircraft aeromechanic response and handling qualities to unsteady disturbances of known fluid dynamics scales. This information would provide great utility to the Navy and the Flight Dynamics, Handling Qualities, Dynamic Interface modeling and simulation community with regards to establishing the level of modeling fidelity required to accurately simulate disturbance interactions (dynamic interface and formation flight), the level of fidelity required to be output by CFD simulations generated ship airwake databases for training scenarios, and the quantification and specification of handling qualities to types of disturbance fields that can be used to define future and ongoing training and aircraft performance requirements and specifications.

Methods, Assumptions, and Procedures

Rotorcraft Flight Dynamics

Rotorcraft flight dynamics models are typically based on simplified component aeromechanics, requiring tuning to capture realistic response characteristics. DI introduces other difficulties such as partial ground effect and interactions between the rotorcraft wake and ship airwake. Reliance on overly simplified modeling and limited bodies of experimental data undermines the realism necessary for investigating the aeromechanics of new configurations and for effective training. For example variations on linear superposition with a frozen disturbance field are still used to represent the influence of the ship airwake and other flow-field effects on the aerodynamics, and thus flight dynamics of the aircraft even though such approaches were shown to be invalid for rotorcraft interacting with external disturbances approximately 20 years ago [3-7]. An example of the types of distortions experienced by the rotor wake and the disturbance field (in this case an idealized wake vortex trailed from an aircraft wing tip) is shown in Figure 2. As will be discussed below, this prior work is significant because it attempted to breakdown the response¹ of a rotorcraft interacting with an external flow field to the most fundamental level by idealizing both the aircraft and the disturbance field; however, given the level of fidelity available at the time of the work, no attempt was made to investigate the full aircraft 6 degree-of-freedom flight dynamics response, flying or handling qualities. Only recently, in work by Continuum Dynamics, Inc. (CDI) in collaboration with NAVAIR Flight Vehicle Modeling and Simulation Branch personnel, have evaluations of fully-interacting DI simulations been undertaken in a research trainer [9] (see Figure 3) and a production fleet trainer [10].

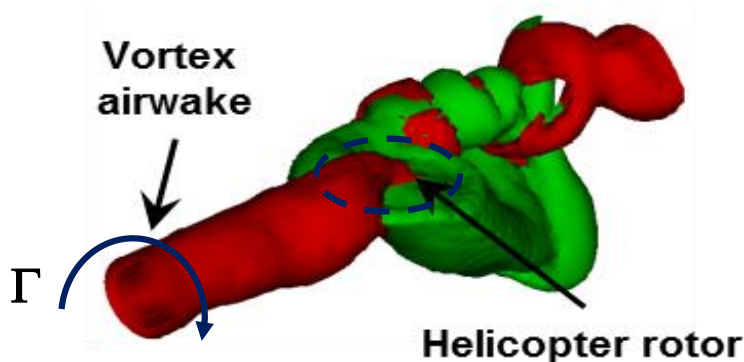


Figure 2: Predictions of interacting rotor wake (green) and airwake (red) with direction of rotation of the vortex and the location of the rotor disk in blue

Over the past few years, CFD has been used to improve the modeling fidelity of ship airwakes [11], with recent success in developing airwake databases for integration into fleet pilot trainers [12]. Development of such a database is a substantial undertaking, and may provide a higher fidelity model – subject to resolving the length and time scales that impact aircraft handling qualities. From a fundamental fluid mechanics standpoint, prediction of these flows will always require more fidelity until we can simulate the Kolmogorov scale directly; however, from flight dynamics and handling qualities standpoints a much coarser representation may be sufficient, or at least the fidelity of the stored airwake database can be coarser, rather than the

¹individual component response subject to changes in modeling assumptions and fidelity

resolution of the original CFD prediction. Exactly what that coarser representation looks like is unknown and current airwake database requirements [12] are an ad hoc compromise between perceived numerical storage availability, feasibility and intuition rather than a formal quantification based upon aeromechanics and handling qualities.² Indeed, CDI, the Navy, and others have already begun investigating methods to reduce the storage and computational cost of accessing these databases. The proposed effort seeks to address this unknown directly from a physical insight standpoint, since once the scales of the disturbance that impart significant influence on the aeromechanics are known, the specification of an airwake database, and indeed the level of fidelity of the trainer, can be defined exactly. Finer resolution will always provide more information with regards to high frequency vibration (assuming that the trainer has sufficient predictive fidelity), but at a minimum, the scales that influence flight dynamics and handling qualities must be present. It is worth noting that independent of the complicated DI problem, the fluid dynamic scales present in atmospheric turbulence, formation flight wake interactions and nearfield operations (wake vortices and flow around/near to buildings) that influence realistic flight dynamics and handling qualities are also unknown beyond qualitative measures associated with low fidelity models and anecdotal evidence from pilots.

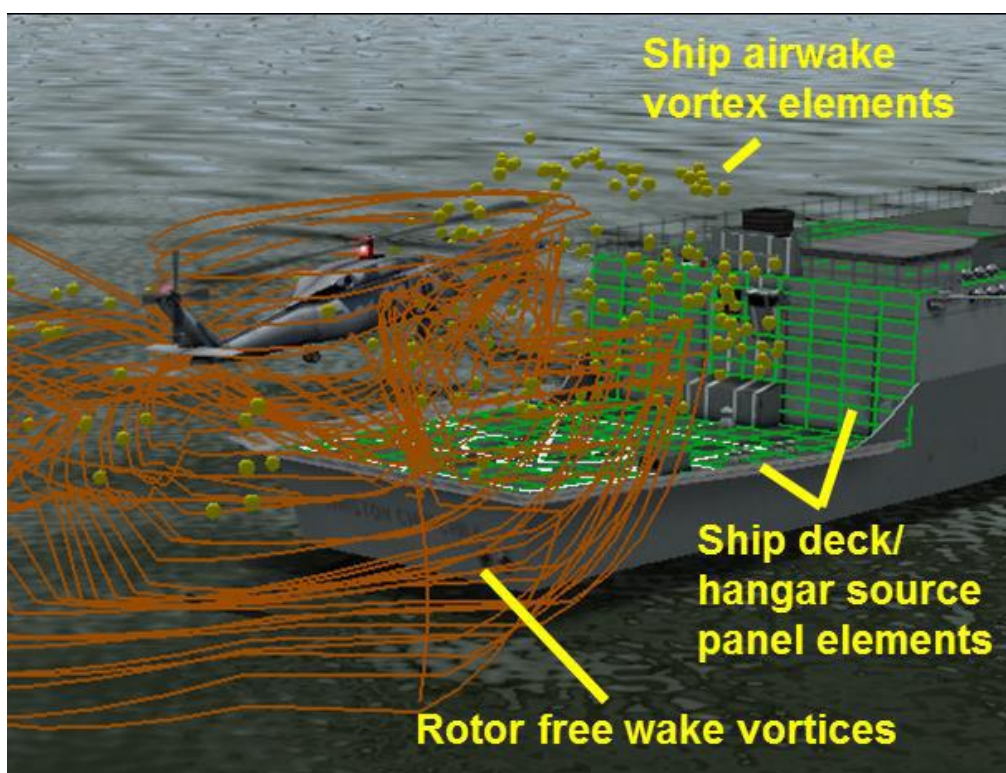


Figure 3: CASTLE / ExHel / CHARM visualization of shipboard operations

Analysis of the response of rotorcraft to gusts, atmospheric turbulence and disturbance fields, such as ship airwakes, has generally followed practices derived from the fixed-wing community, focusing on frozen gust profiles superimposed onto aircraft aeromechanics [13] - i.e. the fields are fixed in space and do not distort/respond to the aircraft. The underlying assumption is that the time scales of the interaction are too short for such a mutual fluid dynamic interaction to

²As will be described below, many ship airwakes are stored at frequencies up to 2Hz because this is the perceived bandwidth for pilot response, not aircraft or rotor response.

occur [6], and indeed, ONR sponsored work at UMD/GIT [1] is investigating exactly this for canonical fixed-wing configurations.

Unlike their fixed-wing cousins, the response of a rotorcraft to external disturbances can be broken into two parts, namely, the response of the rotor, and the response of the airframe. When integrated together, the net response of the complete rotorcraft can be determined. Historically, the influence of gusts on the rotor has centered on ensuring the dynamic stability of the rotor system [14] and making first order estimates of the heave response to sharp edged gusts in order to quantify ride quality [13]. Rotor blade loading is usually much larger than the wing loading of conventional aircraft, and thus, since the heave damping derivative is inversely proportional to the blade loading, helicopter heave sensitivity is much lower than that for fixed-wing aircraft [13]. From a flight dynamics and handling qualities standpoint, estimates of the full six degree of freedom (6-DOF) derivatives can be similarly made, however, care must be taken to ensure that only the aerodynamic derivatives are used to determine “gains” related to gust response [13].

Such a derivative-based approach was a good fit with early rotorcraft flight dynamics simulations that applied simple stability derivative-based models. However, as rotorcraft became more complicated, the need to develop more sophisticated models for control law, control system development and training led to the adoption of a component build-up approach for aeromechanics models and flight dynamics simulations. In these simulations, each individual rotorcraft system/component is represented by a model that can be validated independently, and examples include GenHel [15] and the “example helicopter” model in NAVAIR’s CASTLE simulation environment.

From an aerodynamics perspective, most modern rotorcraft flight dynamics models employ a blade element rotor model, with lifting-line/strip theory local aerodynamics coupled to a finite-state wake model; fuselage and empennage aerodynamics use lookup tables. The effective state-of-the-art in flight dynamics simulation employs a lifting surface blade aerodynamics model coupled to an unsteady vortex wake and panel method for the airframe [9, 10, 16]. CFD methods have been coupled to flight dynamics models of isolated rotors to study maneuvers (i.e. UTTAS pull-up) and DI scenarios [17], but full vehicle simulations using consistent (aerodynamic) fidelity across the aircraft components have not been performed due to computational cost.

The approach used to generate and represent the source of the rotorcraft gust model, or atmospheric disturbance, has taken a similar development route, with initial models following the fixed-wing paradigm of applying a frequency-domain linear statistical representation of atmospheric turbulence (determined by taking a PSD or Fourier transform of a suitable turbulence field – often the von Karman spectra or the Dryden “white noise” model [18]) as a frozen field. Extensions of this approach, such as Jones’ Statistical Discrete Gust method, enable more detailed representation of the structure of a gust since some of the gust’s phase information can be retained through the use of wavelets [13]. With the adoption of blade-element based aerodynamics models by the flight dynamics community, more sophisticated spatially accurate models of disturbance fields have been adopted by implementing analytical representations and 3D and 4D (space and time) look-up tables. Examples range from discrete vortices representing the wake (i.e. tip vortex) from a large fixed-wing aircraft implemented in research simulators [19, 20], to steady (3D) and unsteady (4D) ship airwake databases derived from CFD predictions implemented in operational flight trainers [12].

Despite significant advances in the level of realism in the disturbance field, and the acknowledgement that the assumption is fundamentally flawed when applied to rotorcraft [13],

the vast majority of simulations still apply the frozen field model. Reliance on this assumption is predicated by the level of fidelity offered by contemporary analysis tools (i.e. they do not employ aerodynamics models capable of distorting the flow field), but also by the prevalence of linear superposition in text books and papers when discussing the theoretical underpinnings of rotorcraft aeromechanics (i.e. [13, 21, 22]). Nevertheless, work over the past 20 years undertaken by the proposed key personnel and others has shown a path forward that will form the basis of the proposed effort.

In the early 2000s, there was a resurgence of interest in more tightly integrating fixed- and rotary-winged aircraft into the civil airspace, and research in Europe investigated the impact of fixed-wing wake vortices on helicopter flight dynamics [3-7, 20, 23-25]. Padfield and co-workers [20, 23-25] employed a conventional 6-DOF flight dynamics model of a Westland Lynx-like helicopter, with finite-state-wake, fuselage aerodynamic lookup table and frozen disturbance field, to determine that the helicopter's handling qualities could be significantly degraded and that the fuselage response could be large and important. Whitehouse and Brown [3-7] investigated the impact of the level of modeling fidelity used to represent the wake and the disturbance field on the aeromechanics of a generic isolated rotor interacting with a tip vortex from a large aircraft. The level of modeling fidelity ranged from analytical idealizations and traditional frozen field and finite state wake models, to fully distorting CFD models solving the Eulerian (grid-based) form of the incompressible Navier-Stokes equations in vorticity-velocity form³. To quantify the effects of modeling fidelity in a consistent and rigorous manner, the flight dynamics of the model and the controls were constrained and the fundamental rotor aerodynamic and dynamic responses were calculated. Whitehouse and Brown concluded that when the wake and disturbance field interact over relatively long timescales, such as during low speed flight, the validity of the frozen field assumption must be called into question [6]. Predictions revealed that at low forward speeds the distortion of both the rotor wake and the disturbance field could be significant (see Figure 2), and that the severity of the rotor's response is reduced when compared to the frozen field case (see Figure 4). At high speeds, where the interaction time scale between the rotor wake and the disturbance field is much shorter, the frozen field assumption appears to be more appropriate, nevertheless, differences in the predicted thrust coefficient could vary by as much as 10% of the trim value.

Around the same time in the United States, CDI started to use a distorting Lagrangian wake model to investigate multiple aircraft wake interactions in the terminal area for NASA [26]. This model built upon CDI's extensive history modeling aircraft wake vortices [27-32], and included flight dynamics models for a variety of configurations for preliminary safety assessment. This work was later extended under Navy sponsorship to investigate multiple aircraft shipboard operations [33], and was integrated into the Navy's CASTLE environment to support V-22 formation flight operations [2].

³An ONR effort (N00014-14-C-0014) by CDI developed a proof-of-concept aerodynamics library (VTM2) compatible with flight dynamics software that is based upon the method employed by Whitehouse and Brown.

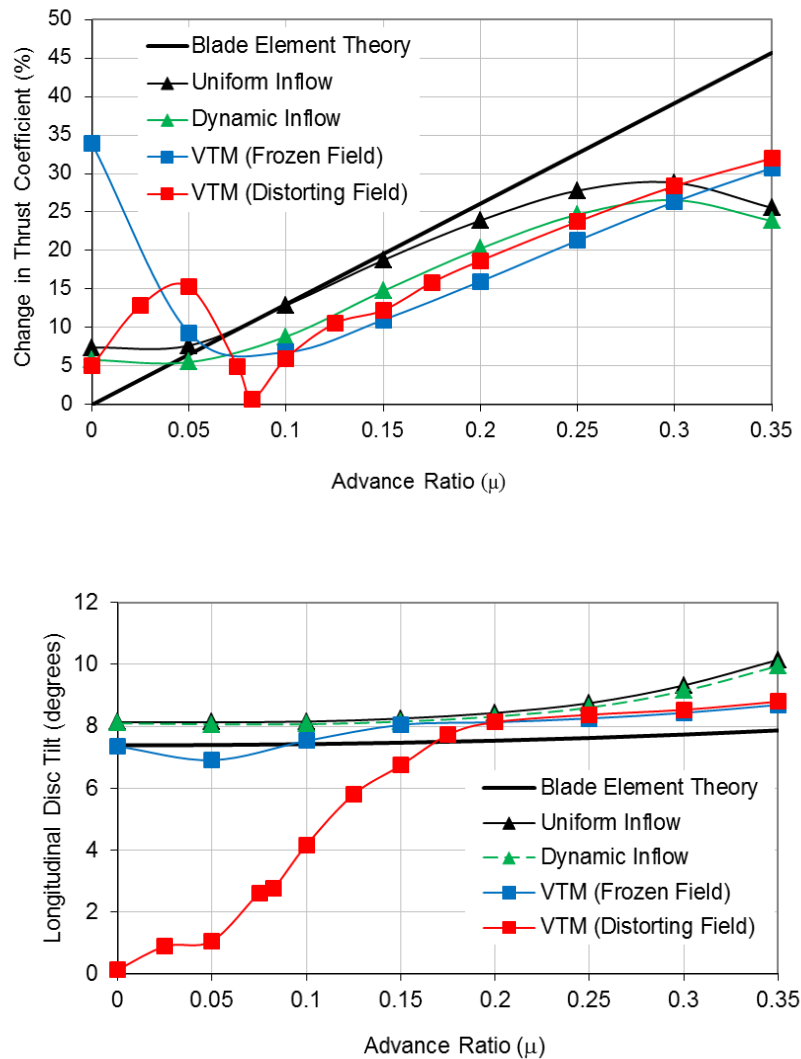


Figure 4: Mean perturbations to rotor thrust and longitudinal disc tilt for an isolated rotor interacting with a vortex aligned with the flight path. Adapted from [6, 7]

In recent years, several efforts of significance have demonstrated proof-of-concept for undertaking fully-coupled flight dynamics and disturbance field fluid dynamics predictions [9, 10, 17]. In [17], researchers at NAVAIR integrated the Kestrel CFD solver with CASTLE to simulate a helicopter landing on a ship. Due to computational cost considerations, the CASTLE blade element rotor model was used for the blade aerodynamics and to determine the strength of appropriate momentum sources that should be inserted into the CFD grid to represent the wake. Kestrel then calculated the evolution of rotor wake and the ship airwake, including their mutual interference, and provided induced velocities to the blade model. No attempt was made to represent the helicopter fuselage or to undertake blade-resolved CFD; only qualitative results were presented. More recently, CDI, in collaboration with NAVAIR, interfaced the CHARM unsteady aerodynamics module (lifting surface blade aerodynamics model coupled to an unsteady vortex wake, panel method airframe and vortex particle disturbance field) with CASTLE and undertook fully-coupled simulations of a helicopter approaching and landing on a

ship deck in both a research simulator [9] and a production (fleet) pilot trainer [10]. In the CDI approach, a ship-airwake database is used to initialize a vortex particle representation of the disturbance field. The Lagrangian vorticity-velocity form incompressible Navier-Stokes equations - the vorticity transport equations - are then solved to determine the mutual interaction of the rotorwake, the ship airwake and the fuselage. Like the work in [17], only limited qualitative metrics were presented, with comparisons made only to the baseline CASTLE model. While the work in [9, 10, 17] represents a significant advance in demonstrating what is feasible with regards to flight dynamics simulation for DI applications, these efforts did little to answer the key fundamental questions about aircraft response outlined above.

Unfortunately, while there have been significant strides in advancing aeromechanics modeling in recent years, the fundamental understanding of the response of rotorcraft to disturbance fields has advanced little since the early 2000s. The following quote from Whitehouse and Brown [6], dated 2004, remains just as pertinent today as it did then.

“Unfortunately, the wide variation in modeling techniques employed in the various published studies and the absence, in most cases, of any systematic variation in model parameters has made it difficult to organise the literature into a mutually-consistent body of evidence from which any general conclusions regarding the safety implications of helicopter encounters with aircraft wakes can be inferred. The situation is compounded by a singular lack of data against which these computations can be validated, and thus the possibility of model-dependence in some of the published results cannot be excluded. In particular, in most previous studies the mutually-induced distortion of the wakes of the rotor and the interacting aircraft has been neglected, yielding the so-called ‘frozen vortex’ assumption. This assumption has generally been adopted as a matter of computational expediency, but where the wakes interact over relatively long timescales, its validity must be called into question. Such is the case when the helicopter is operating at the low forward speeds that are characteristic of terminal flight operations.”

Indeed, it is exactly this need that the effort described herein sought to address. Now, with rotorcraft aerodynamics prediction methodology in a flight dynamics environment at the point where both the disturbance field, the aerodynamic/wake model and the rotorcraft configuration can be varied in a systematic manner, courtesy of recent CDI work for the Navy [9, 10, 34, 35]), we are in a position to attempt to answer many of the fundamental flying/handling qualities and flight dynamics questions outlined above.

Technical Objectives

The goal of the effort was to develop a fundamental understanding of the relationship between the length and time scales typically present in disturbance fields experienced by Naval aviators (i.e. ship airwakes, wing wakes etc.) and rotorcraft flight dynamics and flying qualities when the fully-interacting fluid dynamics of the airwake and rotor wake and flight mechanics are

accounted for. With this information in hand, the engineering community would be better able to understand the relationship between aircraft type, ride quality, flight dynamics and flying qualities during flight conditions where disturbance fields are encountered. The community would be able to more accurately define trainer requirements, minimum experimental campaign requirements, minimum CFD modeling requirements and consequently establish a benchmark to evaluate CFD predictive capability. Furthermore, the conclusions of this work would also directly impact the development of requirements for new aircraft given the direct correlation between flight dynamics and flying qualities. The effort undertook the research required to develop such an understanding, with disseminating the observations and conclusions of the work to the Navy and the broader flight dynamics, flying qualities and handling qualities communities - a key objective from the outset. The effort was structured using a build-up approach that first focused on defining relevant disturbance fields followed by predicting and understanding the fundamental aeromechanics response (i.e. aerodynamic forces and moments and rotor dynamics). The effort culminated in predicting the flight dynamics and handling qualities for realistic, but generic, helicopter and tiltrotor configurations that include representations of flight controls, propulsion system and cross-coupling characteristics.

The key objectives for the effort roughly formed the main tasks and were as follows:

1. Define the spatial and temporal fluid dynamic scales present in relevant disturbance fields and develop numerical representations for testing.
2. 6-DOF generic model assembly and shakedown testing to ensure correct operation and functionality.
3. Define aeromechanics performance and handling qualities metrics along with a detailed simulation test matrix that includes systematic and consistent model fidelity build-up.
4. Undertake simulation of a generic helicopter interacting with frozen and distorting disturbance fields to establish fundamental response characteristics.
5. Undertake simulation of a generic tiltrotor interacting with frozen and distorting disturbance fields to establish fundamental response characteristics.
6. Develop a realistic full helicopter model and undertake simulations of interactions with frozen and distorting disturbance fields to establish flight dynamics and flying qualities response.
7. Develop a realistic full tiltrotor model and undertake simulations of interactions with frozen and distorting disturbance fields to establish flight dynamics and flying qualities response.
8. Documentation and dissemination of observations and conclusions to the Navy and the wider flight dynamics/ flying qualities/handling qualities community.

Results and Discussion

The effort commenced with a review of relevant disturbance field literature, followed by an extensive investigation of how to parameterize the flowfields and represent them in a simple, almost canonical, form. The latter endeavor became a significant focus of the effort, as practical representations of disturbance fields that relate back to the primary structures in the flow appears to be somewhat absent from the literature.

Disturbance Field Characterization

A literature review of relevant disturbance fields and their characterization was performed during this effort, and a summary is presented below. Properties of flow over two-dimensional obstacles are described first, followed by a description of characteristics over 3D obstacles. For 3D geometries, the direction of the incoming wind plays an important role on flow characteristics [36-38], and this influence will be discussed. In the context of DI environments, flat-topped ships have unique wake structures, and receive special attention in this document. The influence of ship motion on airwake features will be discussed as well.

Backward-Facing Step (2D)

Figure 5 illustrates the general flow features associated with a backward-facing step [39], where an unstable shear layer is formed when the upstream boundary layer separates at the sharp corner [40, 41]. This shear layer consists of concentrated vorticities with fluctuating velocity gradients that become increasingly more turbulent as the shear layer propagates downstream with increasing curvature towards the reattachment zone, where part of it is deflected upstream towards the recirculation zone (where the pressure is lower), and part of it is entrained downstream up to as much as 50 step heights [40].

In the recirculation zone, a primary larger “bubble” can be identified, where flow velocities directed upstream can reach up to 20% of freestream velocities, and where negative skin-friction coefficients have been measured in tests [40]. A secondary smaller recirculation zone can also result when 3D effects due to the presence of side walls are accounted for – see discussion below and in [42, 43].

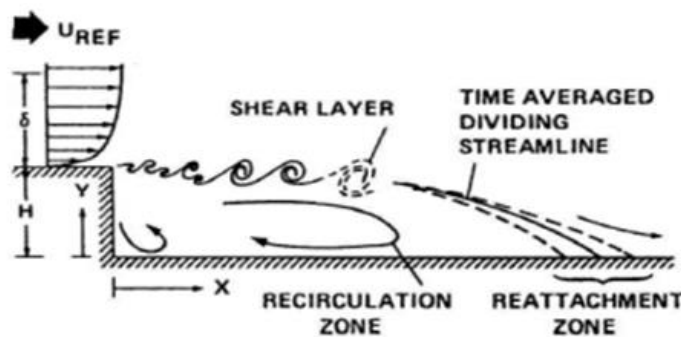


Figure 5: Flow topology over backward-facing step [4].

Flow in the reattachment zone is highly unsteady with coherent structures with length scales at least as large as the step height passing through this region [40, 44]. Coherent structures are large scale vortex structures that conserve their spatial features for prolonged durations. The location where the shear layer impinges on the wall fluctuates, shifting the reattachment zone upstream and downstream at low frequency [40, 41, 45, 46], with the primary recirculation bubble shrinking and expanding accordingly. Driver et al [39] suggest that this “flapping”

motion results when part of the shear layer having greater momentum entrains more of the flow downstream, resulting in less flow being diverted towards the recirculation region. This causes the recirculation region to shrink and increases the shear layer curvature, which, in turn, increases the adverse pressure gradient in the reattachment zone, forcing flow back into the recirculation region. The amplitude of this flapping motion is estimated to be approximately 20% of the shear layer width b , which is given by

$$b = \Delta U / (d\bar{u}/dy)_{max}, \quad (1)$$

where U is the mean velocity in the stream wise direction, $\bar{u}$ is the streamwise velocity fluctuation, and the y coordinate is as defined in Figure 5 [39]. Driver et al further note that this low frequency motion does not have a strong impact on flow turbulence [39]. It should be noted that there is no consensus on the origin of the flapping motion in literature, for example see [44].

The mean reattachment length x_R , which represents the point of zero skin friction, is generally used to characterize the reattachment zone and can vary from $4.9H$ to $8.2H$, where H is the step height, depending on the following parameters:

- Reynolds number of the initial boundary layer,
- boundary layer thickness,
- free stream turbulence, and
- blockage effects (for wind tunnel tests) [40].

In general, x_R is sensitive to the boundary layer Reynolds number only in the laminar and transition regimes (see Figure 6), and the effect of boundary layer thickness δ is noted to be more pronounced when $\delta/H \ll 1$ [40]. Eaton and Johnston [40] suggest that x_R decreases with high levels of freestream turbulence, and that its effect would likely be dependent on the spectrum of the turbulence.

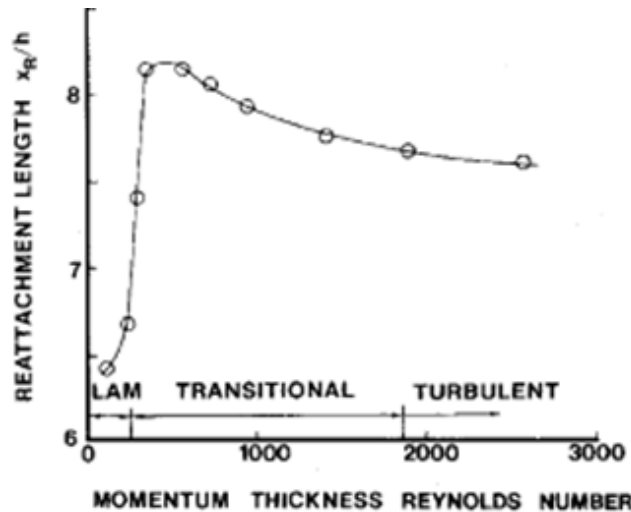


Figure 6: Influence of Reynolds number on reattachment length [40].

Eaton and Johnston [40] further show that maximum turbulence intensity I , defined as the ratio of root-mean-square of turbulent velocity fluctuations to mean absolute velocity

$$I = \frac{\bar{u}}{U_0}, \quad (2)$$

tends to be close to $I = 20\%$, and is typically observed approximately $1H$ upstream of the mean reattachment point. Beyond $1H$, the maximum turbulence intensity decays rapidly.

Vortex shedding is characterized by the Strouhal number which is defined as

$$St = \frac{fb}{U}, \quad (3)$$

where f is the vortex shedding frequency and b is the characteristic length. Through spectral analysis of the pressure fluctuations at two streamwise locations $x/H = 5.5$ and $x/H = 8.0$, Driver et al. [39] identify a dominant frequency of $St = 0.2$, which is comparable to the characteristic frequency noted in the spanwise vortical structures of free shear layers, and is consistent with shedding from a flat plate aligned perpendicular to the flow [47].

Rectangular Prism (2D)

In 2D flow over a rectangular prism, free shear layers are shed from the sharp edges of the cross-section as depicted in Figure 7 [48], where streamlines over four rectangular prisms with different width-to-length aspect ratios are shown. It is evident that aspect ratio influences the mean reattachment length, and for aspect ratios 1:3 and 1:4, the mean flow reattaches to the lateral sides of the rectangle. The resulting mean (time averaged) flow features are also symmetric. The Strouhal number for vortices shed by rectangular prisms can vary from 0.1 to 0.18 depending on the aspect ratio and Reynolds number [49].

Surface-Mounted Cuboid (3D)

Flow over 3D obstacles, such cuboids, introduce additional complexity relative to the 2D case as end and side wall effects become important [50, 51]. Flow features around a surface-mounted cuboid are depicted in Figure 8 and Figure 9 [50]. Upstream of the obstacle, horseshoe vortices form that extend over the sides of the obstacle and interact with the downstream separation region, resulting in high flow unsteadiness [52]. An arch vortex forms on the leeward side of the obstacle, and flow detaches at the top edge of the wind-facing surface of the obstacle and reattaches downstream of the arch vortex. The mean reattachment length in 3D is shorter than that noted for 2D backward facing step due to side wall effects. Note from Figure 8 that time-averaged flow features are symmetric.

Similar flow features are noted with 3D rectangular prisms [53], though they are sensitive to aspect ratio [54]. If the height is less than the length and width, the flow reattaches on the top surface of the prism, and up to three recirculation regions are observed, as shown in Figure 10 [53]. The location of the reattachment point on the top surface and behind the prism varies in the spanwise direction as depicted in Figure 11 [53]. Herry [54] notes that the location of the reattachment point is sensitive to the obstacle length-to-height aspect ratio, width-to-height aspect ratio, and Reynolds number. There is a minimum Reynolds number Re_c above which flow features become independent of Reynolds number for bluff bodies with sharp edges [37, 55, 56], however, a general rule for obtaining Re_c based on geometrical properties does not seem to have been established [55]. Not surprisingly, however, the mean velocity and turbulence of any upstream atmospheric boundary layer also influences downstream flow turbulence levels [37, 56, 57].

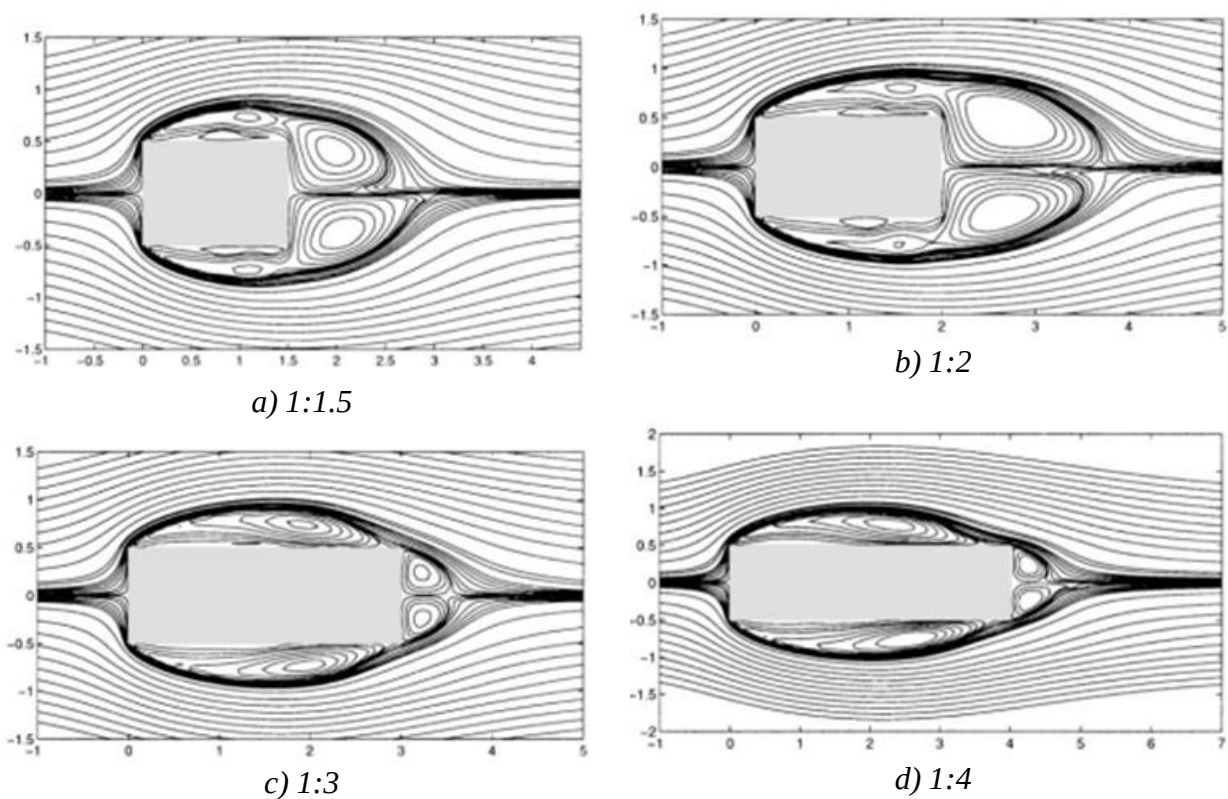


Figure 7: Streamline of mean flow around rectangular prisms with different aspect ratios [48].

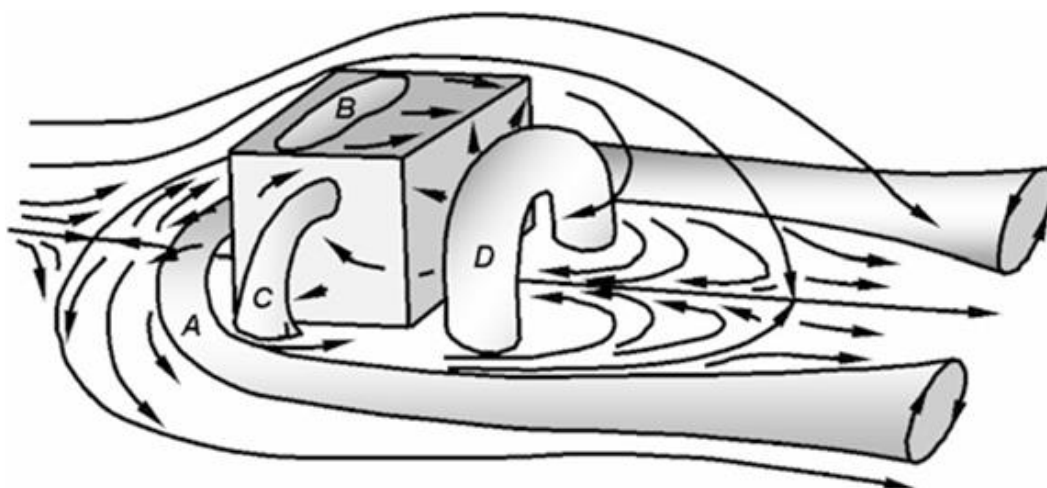


Figure 8: Schematic of principal flow features around surface-mounted obstacle [50].

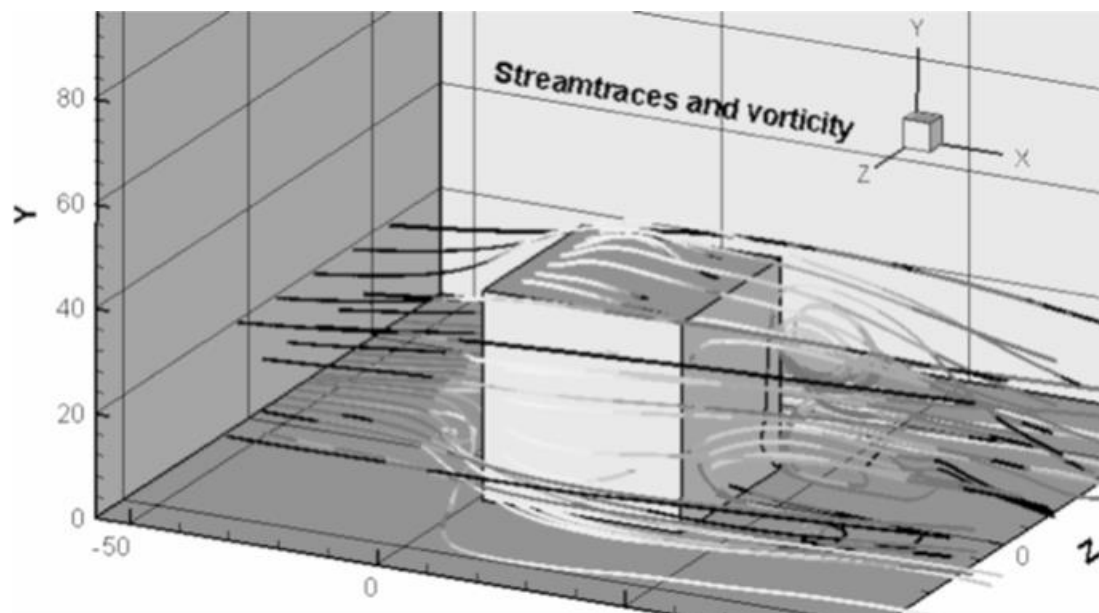


Figure 9: Perspective view of streaklines around surface-mounted obstacle [50].

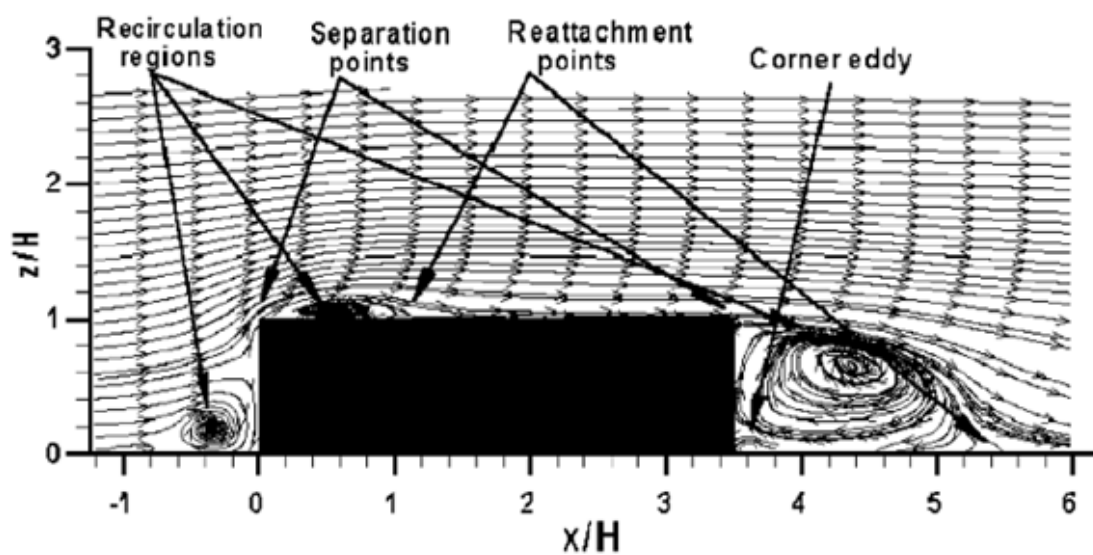


Figure 10: PIV derived streamlines at center plane around surface-mounted obstacle [53].

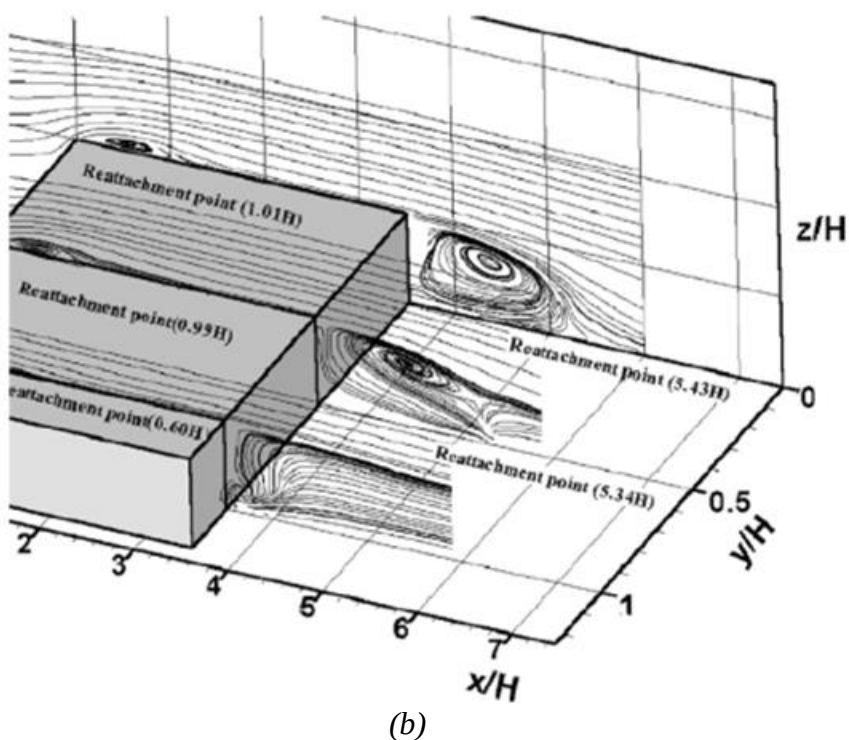
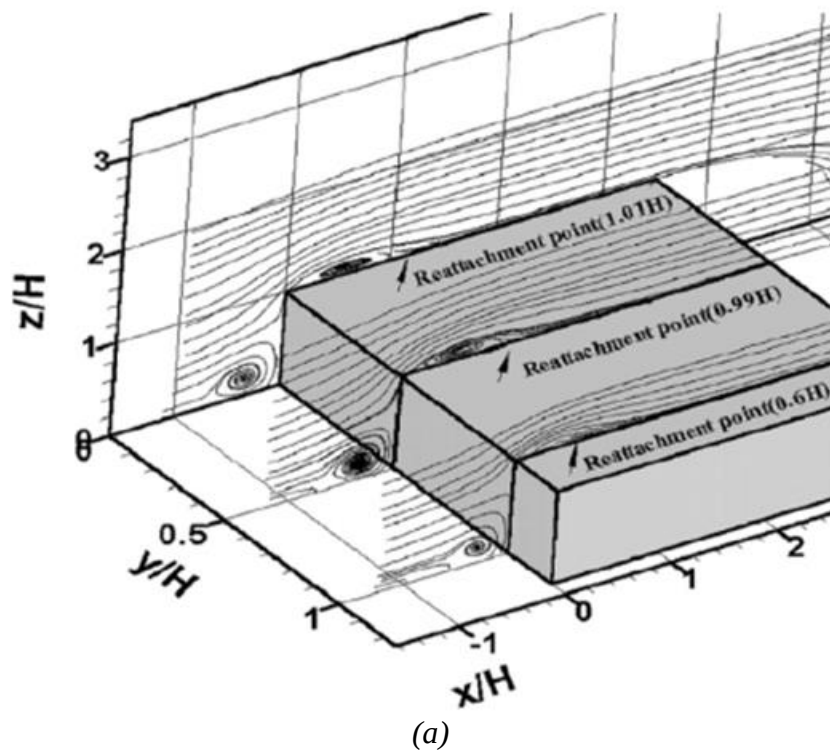


Figure 11: Streamlines of flow over surface mounted obstacle seen from (a) windward and (b) leeward directions [53].

An important parameter used to quantify flow turbulence is the turbulent kinetic energy (TKE) defined as [53]

$$TKE = \frac{1}{2}(\overline{u^2} + \overline{v^2} + \overline{w^2}), \quad (4)$$

where, $\bar{u}, \bar{v}, \bar{w}$ represent fluctuating streamwise, spanwise and vertical velocity components. TKE can be related to turbulence intensity, introduced previously, as:

$$I = \sqrt{\frac{2}{3}TKE} \quad (5)$$

Kim et al. [53] observed peak TKE levels on the top surface of a $3.5H$ long rectangular prism at $x \approx 0.5H$, see Figure 12. Becker et al. [37], on the other hand, examined flow over a rectangular prism with a shorter length of $0.25H$ and observe peak turbulence levels at $x = 3H$, as depicted in Figure 13, demonstrating the influence of aspect ratio on flow characteristics. Their spectral analysis further indicates dominant frequencies at $St_H = 0.15$, with little sensitivity to Reynolds number and the width-to-height aspect ratios (1:2, 1:3, 1:4 were considered) [37]. Note that this Strouhal number is higher than $St_H = 0.104$ noted for flow over cubes [52]. The subscript “H” indicates that the obstacle height was used as characteristic length in computing the Strouhal number, see Eq. (3).

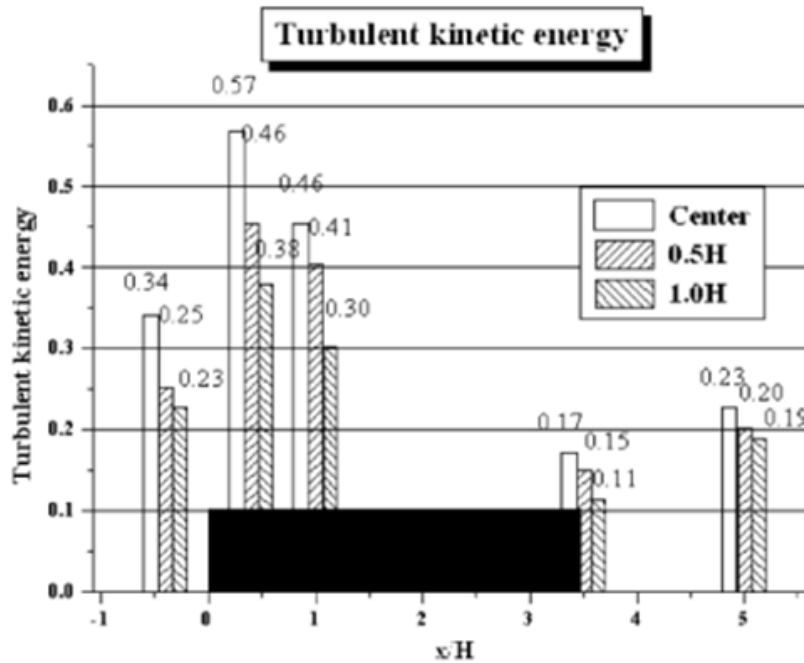


Figure 12: Turbulent kinetic energy peaks at different locations for flow over a 3D rectangular prism from Ref. [53] (boundary layer thickness of $\delta = 0.06H$ and Reynolds number (based on step height) of 7.9×10^3)

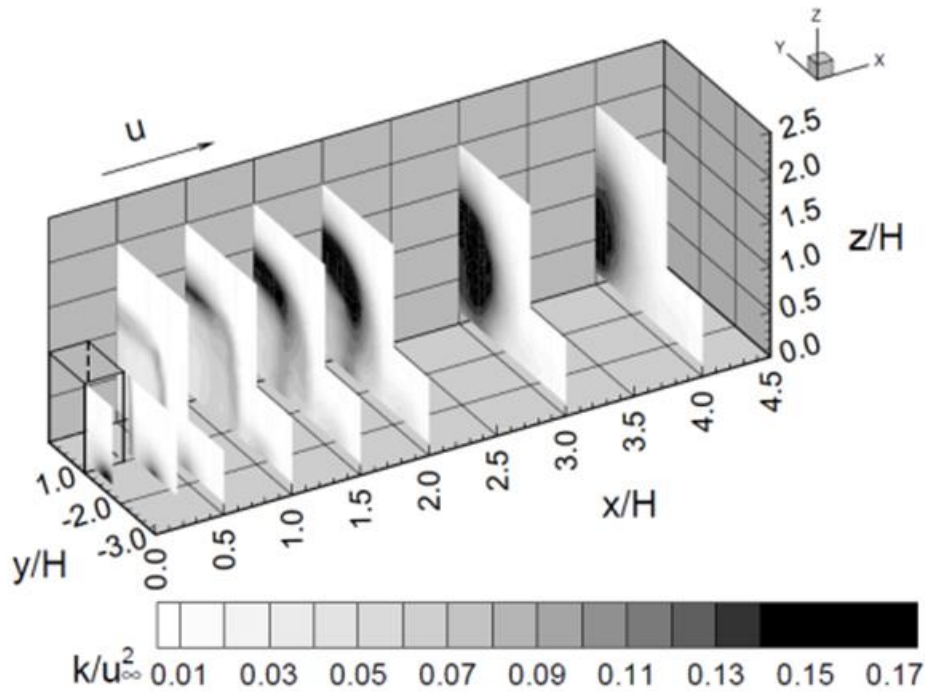


Figure 13: Turbulent kinetic energy at different locations for flow over a 3D rectangular prism from Ref. [37].

Small-Deck Ships (3D)

The double-backward facing step geometry, depicted in Figure 14 along with prevalent flow features [58], closely matches the aft region of typical small-deck aviation ship geometries and is thus of particular interest. Tinney and Ukeiley [58] derived the flow features in the figure using both PIV and oil-flow visualizations at $Re = 9000$ for the ship geometry shown in Figure 15. The flow features in Figure 14 are similar to those for flow over a surface mounted cube shown in Figure 8, with some notable differences. In the flow aligned case, the upstream horseshoe vortex seen in Figure 8 is no longer present in Figure 14 due to flow reattaching on the walls of the structure before the first step. Streak lines that start along the edges of the ship structure from the nose curl up into vortex tubes, denoted as “wing vortex” in Figure 14, due to pressure gradients between the side and top surfaces of the structure and interact with flow downstream of the reattachment region to produce counter-rotating vortex tubes on both sides of the streamsurface bifurcation point on the figure.

Although Figure 14 depicts a symmetric upside-down arch vortex aft of the first step, this structure is unsteady and at any instant in time is asymmetric, see Figure 16 from [54]. This is a consequence of bi-stability of the flow, which causes the resulting asymmetric flow features to switch intermittently between portside and starboard locations. The influence of this asymmetry on helicopter-shipboard operations remains to be examined.

Tinney and Ukeiley [58] observe peak longitudinal turbulence intensity near the recirculation zone at $1H \leq x \leq 2H$ downstream of the first step (see Figure 17a) and peak vertical turbulence intensity closer to the reattachment zone in the $2H \leq x < 4H$ region (see Figure 17b). Turbulent kinetic energy is dominated by longitudinal velocity fluctuations, and peak TKE is observed in $1H \leq x < 2.5H$ region in Figure 17d. Note that the TKE was computed using only longitudinal

$\bar{u}$ and vertical $\bar{v}$ components. The longitudinal and vertical turbulent intensities in the figure are noted to be greater than those observed with 2D obstacles [58]. The dominant frequency was characterized by a Strouhal number of $St_H = 0.17$ [58].

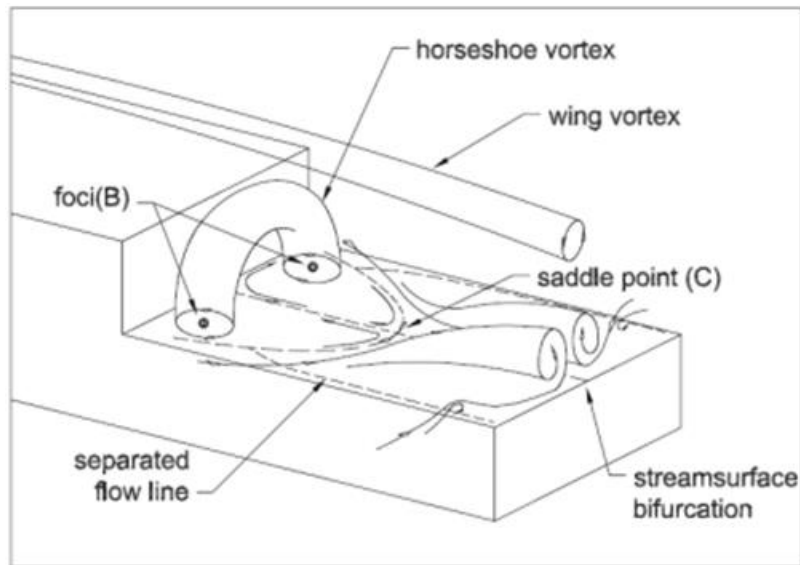


Figure 14: Flow feature for a 3D double-backward facing step [58].

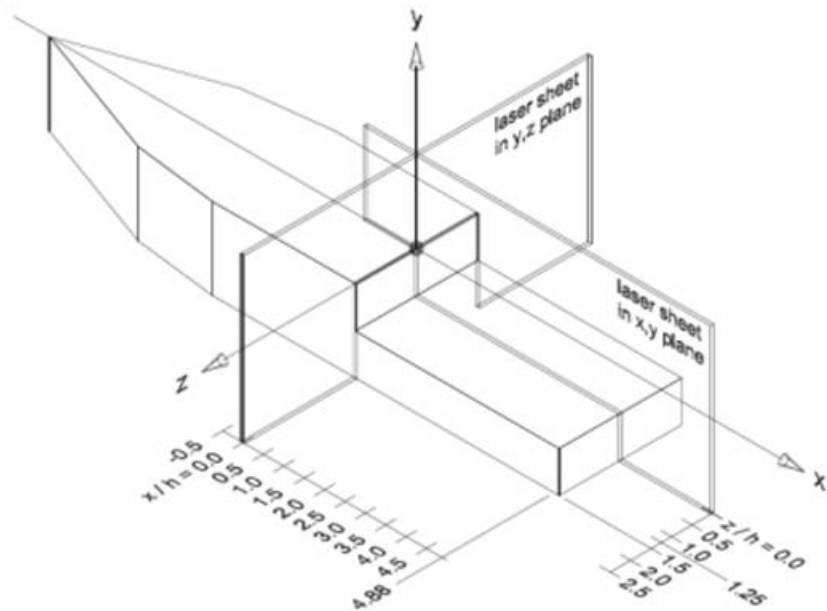


Figure 15: Ship geometry used to derive flow features for a 3D double-backward facing step [58].

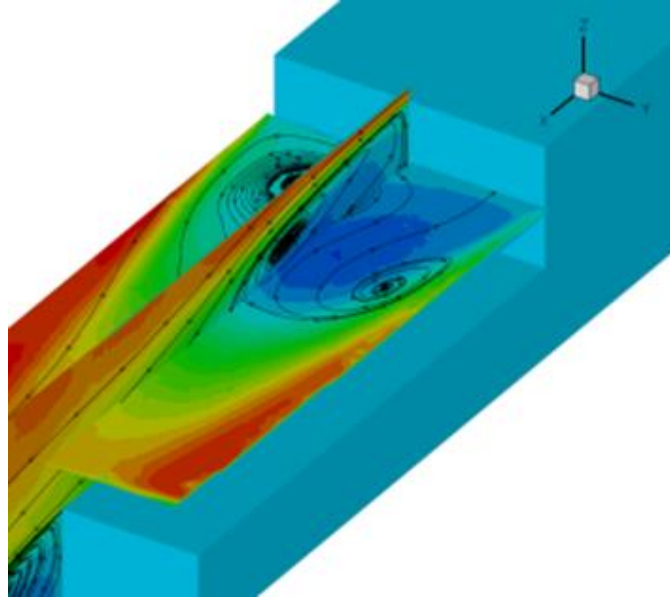


Figure 16: Asymmetry in arch vortex behind 3D double-backward facing step [54].

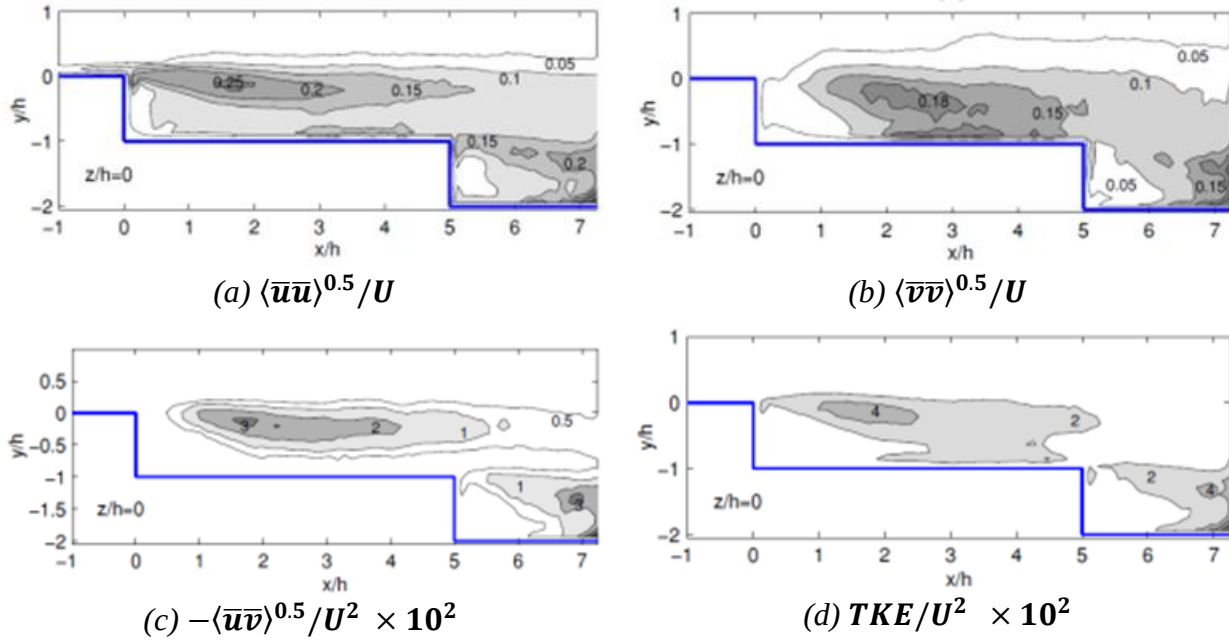


Figure 17: Contours of turbulence parameters for a 3D double-backward facing step [58].

Flat-Topped Ships (3D) and Wind-Over-Deck

Flat-topped ships, such as aircraft carriers and Landing Helicopter Assault (LHA) ships (e.g. Figure 18), are characterized by sharp-edged flat decks and superstructures located towards the starboard side of the ship. While helicopter landing spots are distributed across the entire deck, the fixed wing landing zone typically takes up most of the port side of the deck.

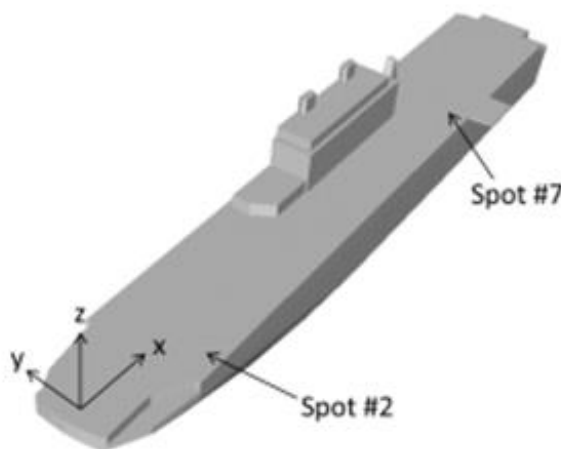


Figure 18: LHA ship geometry with landing spots #2 and #7 highlighted, adapted from Fig. 1b in Ref. [59].

A unique aspect of the flow characteristics around such configurations is the existence of “wing-vortex” like structures that originate and propagate from the edges of the ship structure, denoted as “deck-edge vortices” in Figure 19 [59]. From Figure 19a, headwinds produce a large separation at the bow, though strong coherent wind aligned deck-edge vortices also form. These vortices are generated due to pressure differences between the side and top surfaces of the deck. Vortical structures denoted as “burbles” in Figure 19a shed periodically from the bow towards the superstructure at frequencies of 0.2 – 0.3 Hz [59]. The interaction between the superstructure, burbles, freestream, and deck-edge vortices produces the highly turbulent region over and aft of the superstructure.

The airwake in the lee side of ship is highly unsteady and separated, producing downwash fields that can cause fixed-wing aircraft to bolter during landing, resulting in a touch-and-go maneuver on the deck and requiring an additional landing attempt. This region of high turbulence is known as the “burble,” (not to be confused with the burble structures shed from the bow) and the associated loss of altitude and deviation from desired glideslope experienced by pilots during landing is known as the “burble effect” [60]. The ambient wind interacting with the ship structure, combined with the ship velocity, is referred to as “wind-over-deck” (WOD). For a starboard WOD condition at 30° (measured clockwise relative to the ship longitudinal axis), no bow separation or burble structure is present in Figure 19b. The starboard deck-edge vortex rolls onto the deck and spreads spanwise over the deck while the portside one detaches from the ship and is blown over the ocean, with significant flow separation over the superstructure.

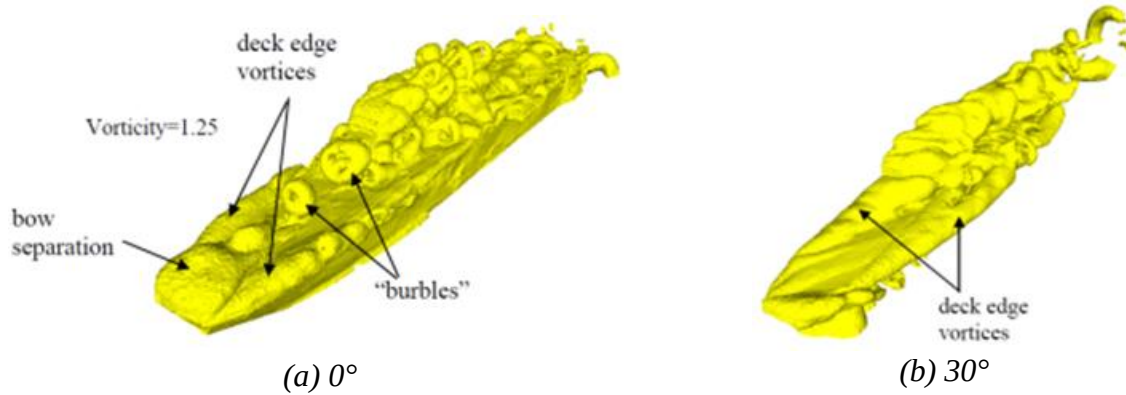


Figure 19: Snapshot of vorticity iso surfaces for a 30 kts WOD from Ref. [59]

Figure 20 shows wake visualization for the headwind and 30° starboard WOD conditions from Ref. [61], where both time-accurate and time-averaged flow conditions over a full-scale LHA ship geometry were examined using the PUMA2 CFD solver. In Figure 20b, a vortical coherent structure from the starboard edge that runs diagonally across the fore of the ship is evident for the 30° WOD condition. This vortical structure results in lateral velocities of approximately -11 m/s over spot #2 (see Figure 21a [61]), which is 40% greater than the lateral freestream component value of -7.7 m/s ($-15.43 \sin 30^\circ$ m/s). The largest velocity fluctuations were noted in the time-histories ~6 m above spot #7 for the 30° starboard WOD condition, see Figure 21b [61]. From spectral analysis of longitudinal and vertical velocity time-histories, Sezel-Uzol et al. note peak frequencies at 0.1 Hz ($St = 1.3$) and 0.2 Hz ($St = 2.6$) over spot #2, and a peak frequency of 0.5 Hz ($St = 6.5$) over spot #7 for both headwind and starboard 30° conditions. Note that Strouhal numbers were not computed in Ref. [61] and are provided here using the full-scale beam width of 31.8 m as characteristic length and the free stream velocity magnitude of 15.43 m/s.

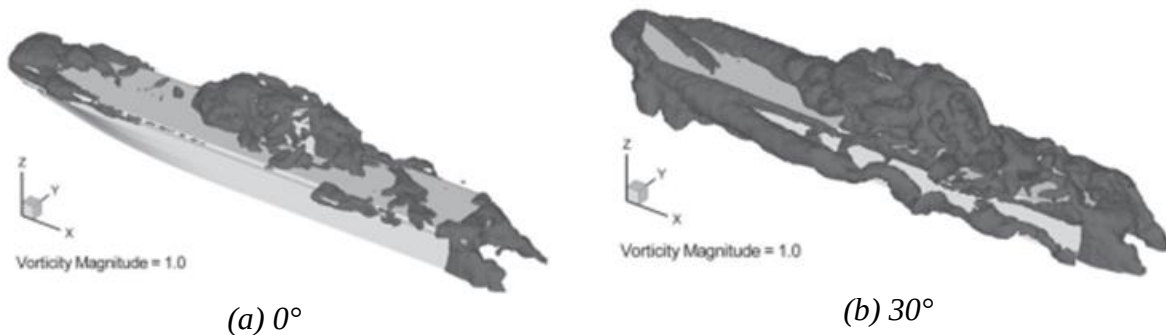


Figure 20: Snapshot of vorticity iso surfaces for a 30 kts WOD from Ref. [61]

The influence of WOD angle on longitudinal u and vertical w velocity airwake components are shown on the time-averaged flow contour plots for a plane 6 m above the deck in Figure 22 and Figure 23, respectively [61]. From the figures, oblique and beam WOD conditions produce the greatest fluctuations in velocity components. For beam winds, regions with negative longitudinal velocities are noted in the fore of the ship in Figure 22. The sharp edges of the deck and the superstructure generate regions with reduced u velocity magnitudes in the oblique conditions. The contours in Figure 23 reveal regions with significant negative vertical velocities (up to -6 m/s) in the fore of the ship, which may adversely influence rotor inflow during piloted

operations. These contours also indicate large upwash velocities of approximately 15 m/s on the windward edges of the ship, as flow rolls over the edges. It should be noted that the study by Sezer-Uzol et al. [61] did not account for atmospheric boundary layer (ABL), which is needed for better predictions of turbulence intensities at full-scale [62], however, it is not anticipated that including the ABL would fundamentally alter the primary flow structures in the wake. Rather, the ABL would serve to introduce additional frequency components, and likely accelerate the breakdown of bluff structures. In an accompanying paper [63], instantaneous vorticity magnitude plots for the PUMA2 based simulation of flow of the LHA ship were provided (see Figure 24). The deck-edge vortices are noted to have magnitudes in the $0.7 - 2.0 \text{ sec}^{-1}$ range for the headwind condition and peak magnitudes of 3.0 sec^{-1} in the starboard 30° WOD case.

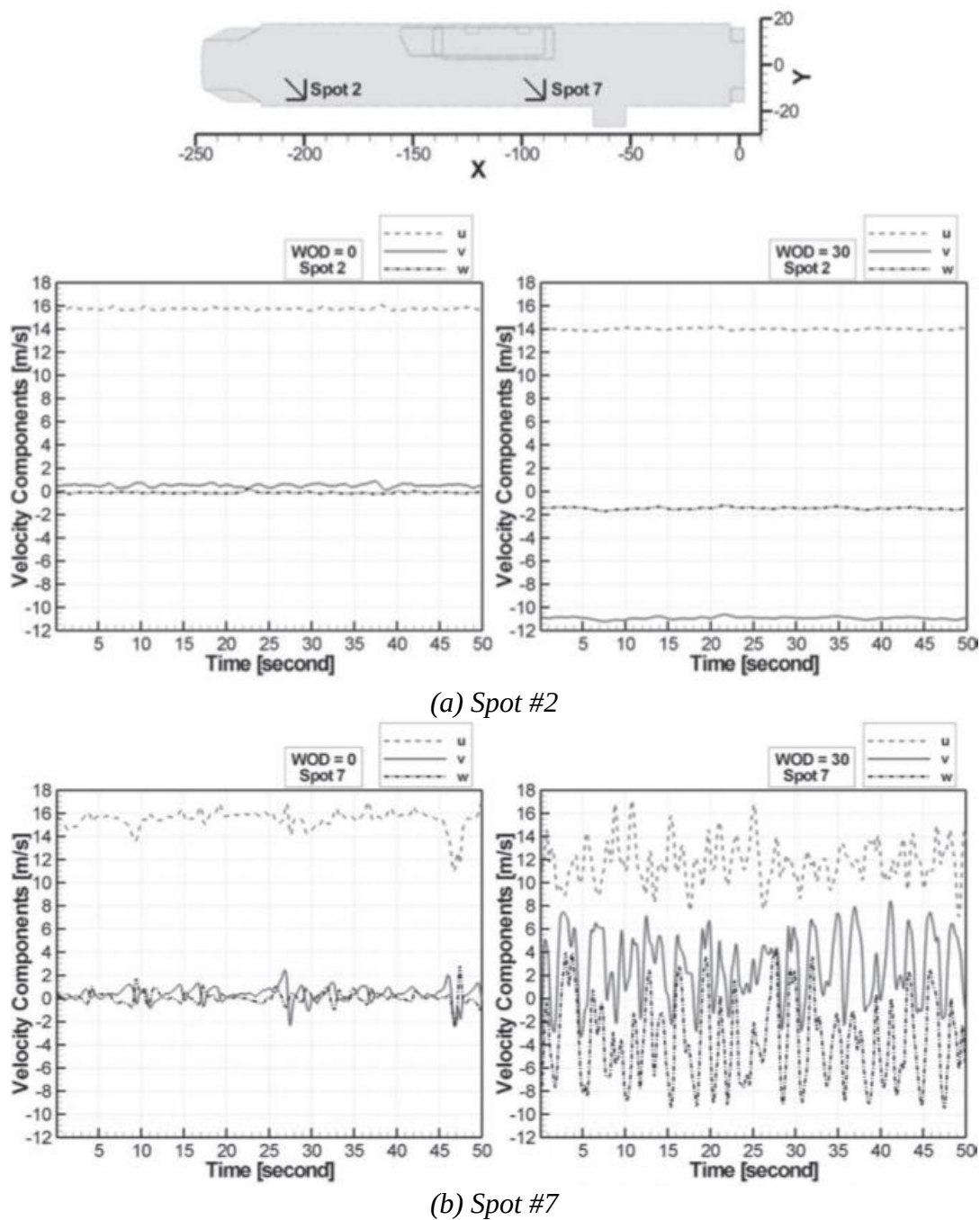


Figure 21: Time histories of velocity components for 30 kts WOD 6m above landings spots #2 and #7 [61]

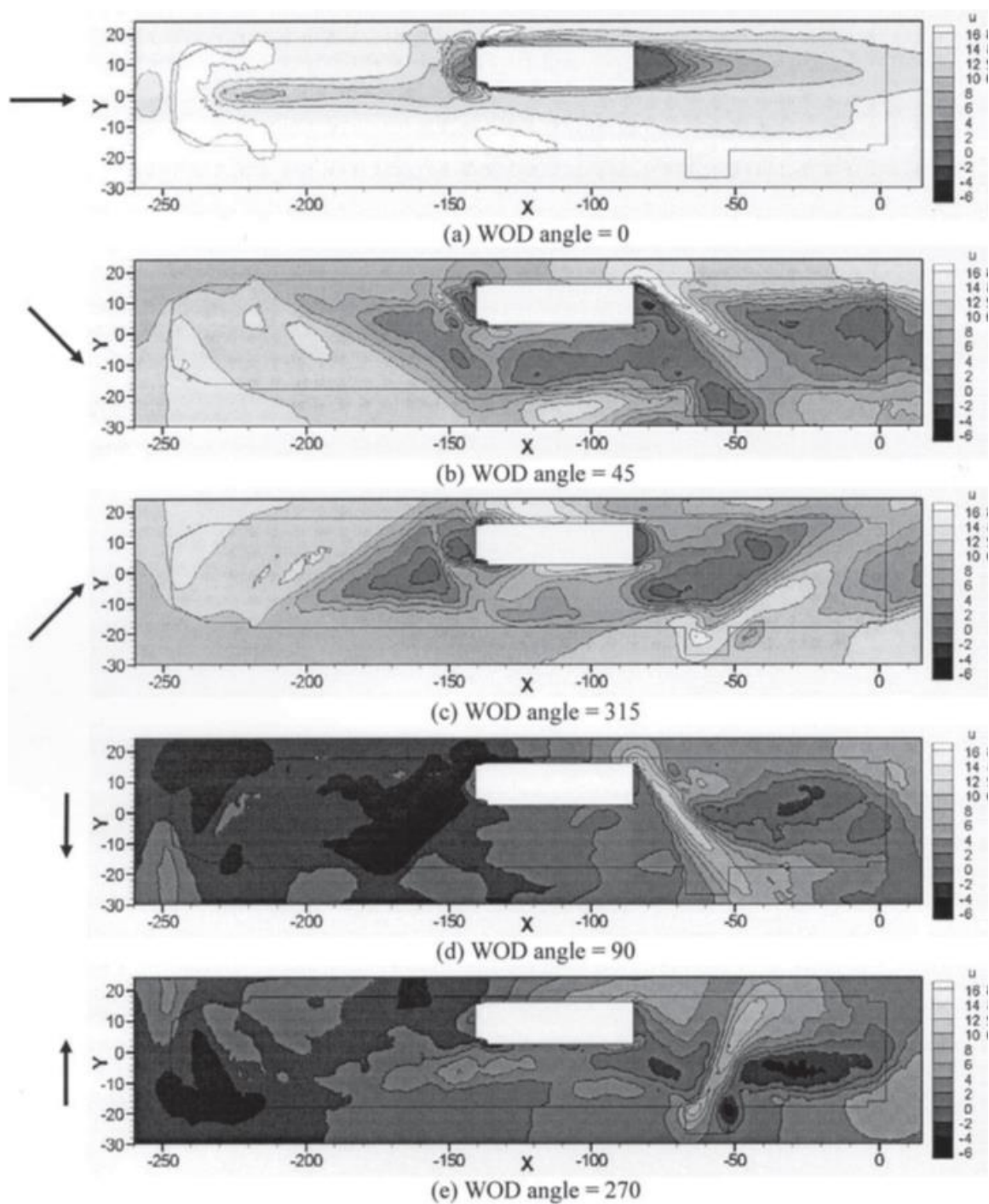


Figure 22: Time-averaged contours of longitudinal velocity for different WOD conditions [61]

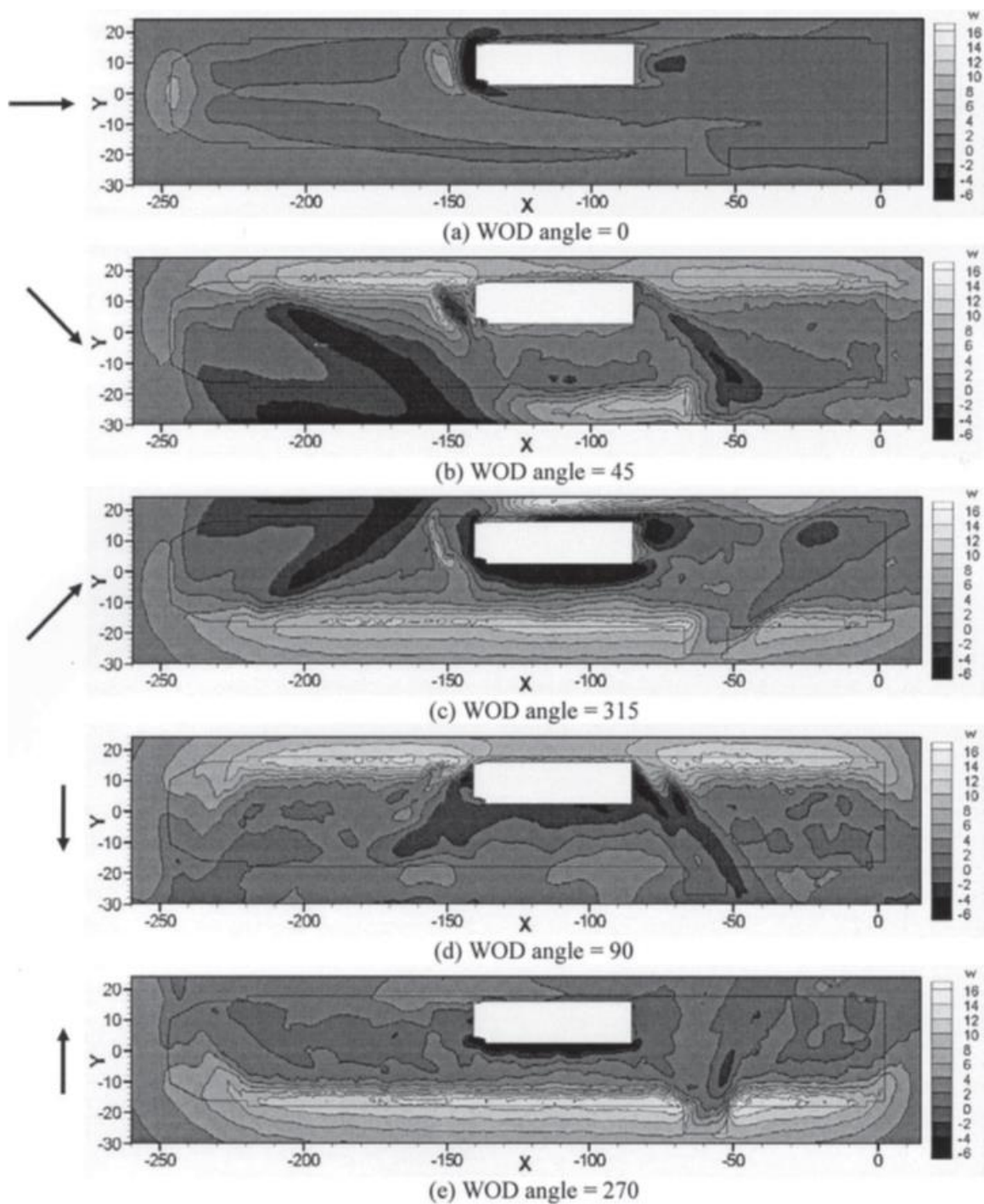


Figure 23: Time-averaged contours of vertical velocity for different WOD conditions [61]

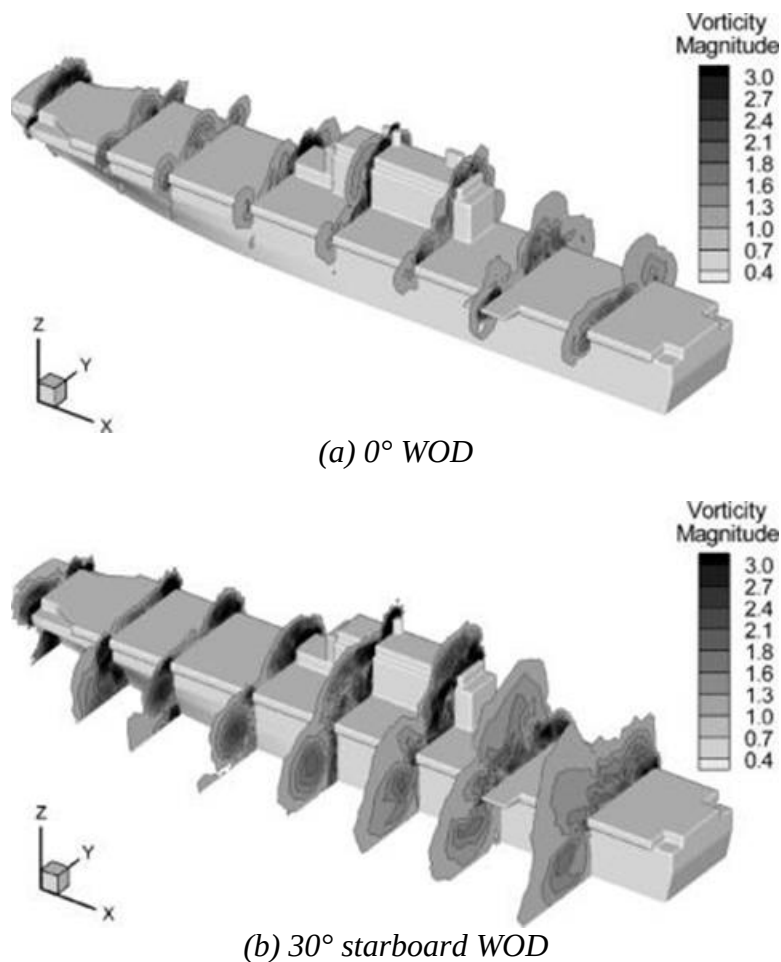


Figure 24: Instantaneous vorticity contour magnitude across ship deck [63]

Although CFD predictions provide valuable insight into ship airwake characteristics, they can only be considered reliable when validated against experimental data (model or full-scale) since factors such as solution type (RANS, DES, LES), time step size and mesh resolution can influence predictions [64]. Wind tunnel experiments are easier to setup, control and interrogate than full-scale tests, which are sensitive to changes in at-sea conditions, but usually fail to replicate full-scale Reynolds number due to tunnel limitations. This has been generally considered acceptable since the wakes shed from the sharp edges of ship decks and superstructures tend to be Reynolds number independent [59]. Turbulence in the wake at wind-tunnel scale experiments also tend to occur at much higher frequencies than full-scale tests [59], but the cause of this is not entirely clear. Polsky and Bruner in Refs. [59] and [65] compare time-averaged and steady-state CFD solutions to wind tunnel data obtained using a 1/120th scale model of the LHA ship for a headwind condition. Only time-averaged measurements were made during the experiment. Velocities along the data plane shown in Figure 25 are shown in Figure 26 for $z = 0.2315$ in (equivalent to $z = 2.315$ ft. at full-scale) [65]. Time-averaged predictions from CFD were obtained from time-accurate simulations. Corresponding standard deviation bounds are also plotted in Figure 26 to highlight velocity fluctuations in the time-accurate solution.

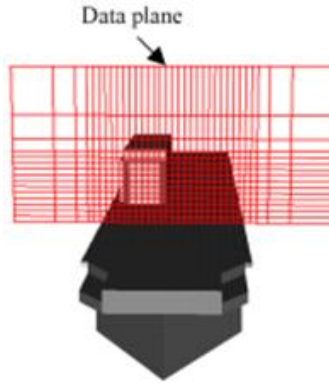


Figure 25: Plane considered for CFD comparison with experimental data in Ref. [59]

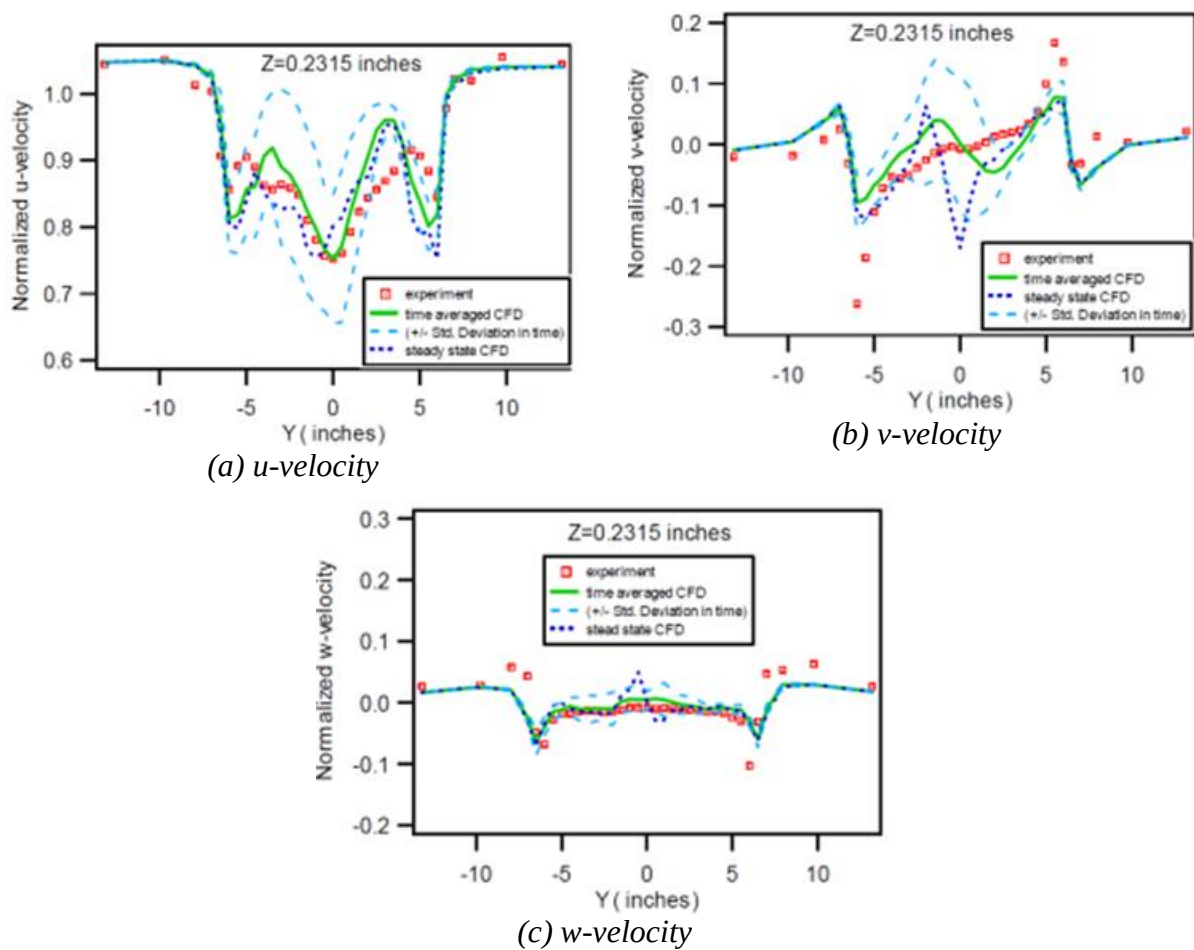


Figure 26: Comparison of velocities from CFD with wind tunnel data, $z=0.2315$ in [65]

Discrepancies between CFD predictions (with standard deviation) and experiments are noted near the edges of the deck at $y = \pm 5$ inches in the lateral and vertical velocity components, potentially due to deck-edge vortex effects not being modeled properly in CFD. Time-averaged velocity predictions for $z = 0.7315$ in (equivalent to $z = 7.315$ ft. full-scale) are shown in

Figure 27 [59]. Similar offsets are evident in the lateral and vertical velocity components near the edge locations at $y = \pm 5$ in.

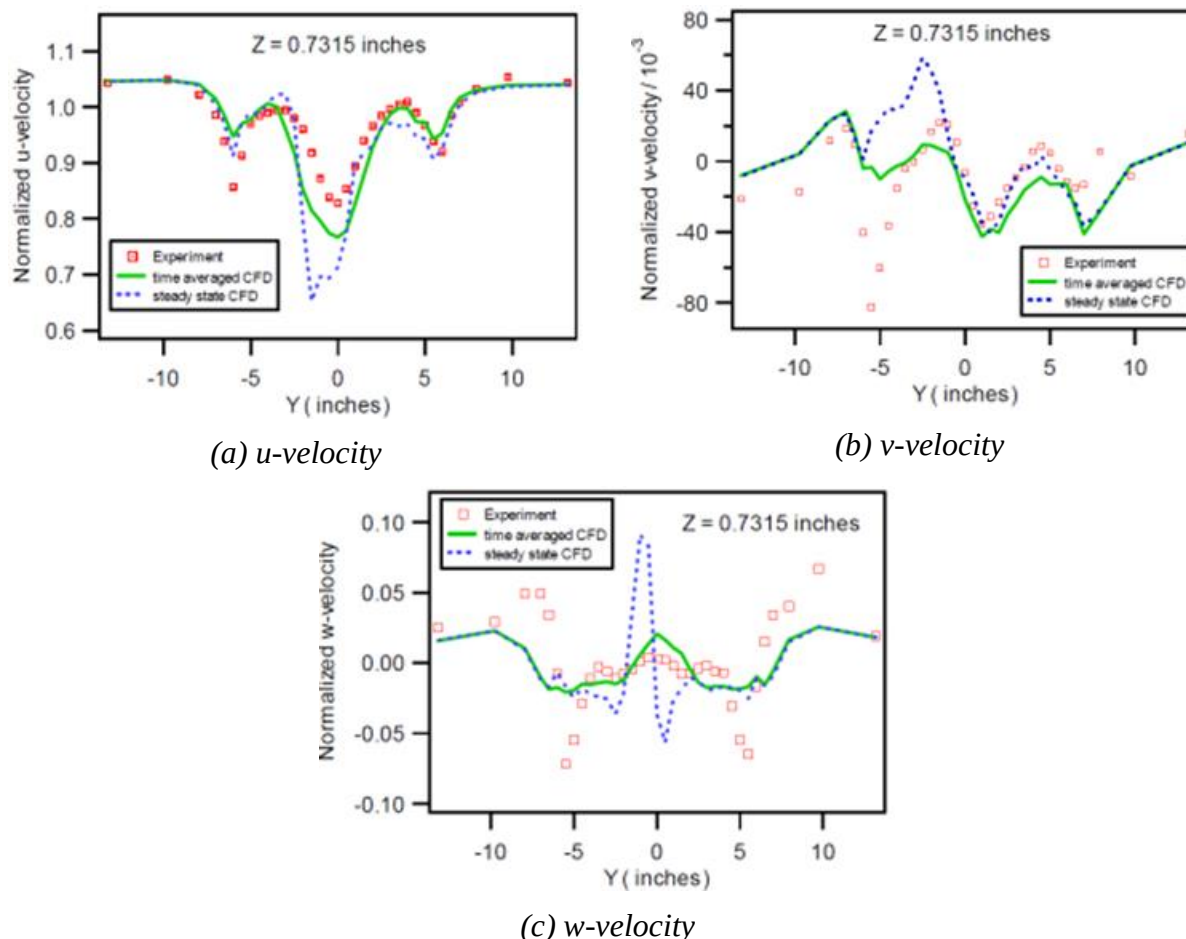


Figure 27: Comparison of velocities from CFD with wind tunnel data, $z=0.7315$ in [59]

The discrepancy between CFD predictions and experimental data was also noted in a study by Rajagopalan et al. [66], where mean velocity predictions from the Rot3DC unsteady, incompressible flow solver were compared to PIV data obtained for a $1/48^{\text{th}}$ scale model of the LHA ship for 0° and starboard 15° WOD conditions. This study was part of a comprehensive effort by NAVAIR to better understand ship-aircraft dynamic interface after an on-deck V-22 aircraft experienced an uncommanded roll due to the combined upstream airwake from the ship and a CH-46 aircraft. Figure 28 shows streamwise velocity contours across landing spots #2, #4, #7 and #8. The deck-edge vortices are evident at spots #2 and #4. The influence of the superstructure is shown as well, resulting in velocities with magnitudes of up to 20% of freestream near spot #7. Comparisons of predicted and measured mean vertical velocities at the four landing spots at $z = 5.206$ in (equivalent to $z = 20.83$ ft. at full-scale) are shown in Figure 29. Note that this height corresponds to the height of V-22 rotors when the wheels are on the deck. Figure 29 shows that CFD fails to capture the peaks at $y = \pm 0.6$ near the edges of the ship (spot #4), similar to Ref. [65], but this discrepancy does not seem to have been discussed in literature.

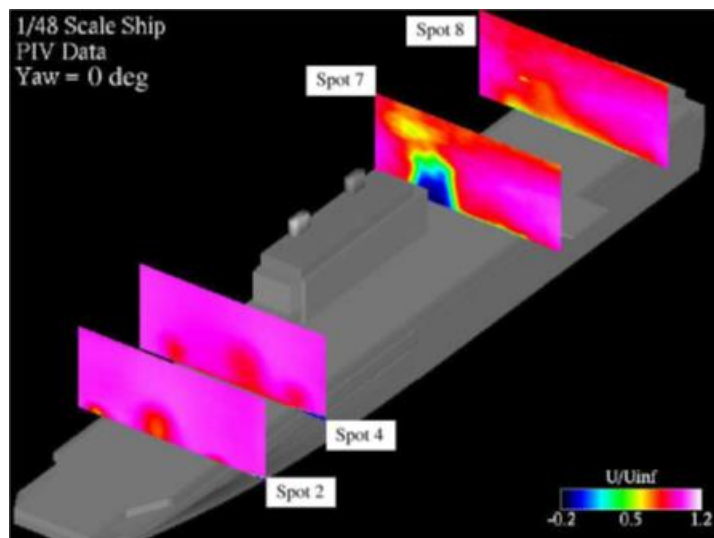


Figure 28: Streamwise velocity contour plots as determined from PIV data for headwind condition [66]

In recent years, the need for a more systematic approach to studying airwake characteristics for flat-topped ships has been highlighted [67], and more recent studies have attempted to provide quantification metrics such as turbulence intensities [68-70] to enable direct comparison between tests and simulations. Nevertheless, there does appear to be a dearth of experimental measurements of the wake downstream of the ship – *exactly where an aircraft on approach may fly*.

An extensive CFD investigation of the flow over a LHA ship was performed using OpenFOAM in Ref. [68], and qualitative agreement with results from [61] was noted. Mean velocity magnitudes across the ship beam at spots #2, #4, #7 and #8 are shown in Figure 30 for $z = 3$ m (9.8 ft.) and $z = 6$ m (19.7 ft.). The large drop in velocities at spots #2 and #4 are due to bow separation while those at spots #7 and #8 are due to the shedding downstream of the superstructure. It is interesting to note that the influence of deck-edge vortices is more pronounced closer to the deck at $z = 3$ m at spot #4. Corresponding turbulence intensity plots are shown in Figure 31, where bow separation produces the greatest turbulence at spots #2 and #4. Unfortunately, the definition of “turbulence intensity” is not specified by the author, and thus these figures should be viewed from a qualitative perspective. Streamwise turbulence intensity plots across the ship centerline and a line through the portside landing spots are shown in Figure 32 for two mesh resolutions. At the centerline, a peak in turbulence is noted due to bow separation and predictions from the two mesh resolutions show good agreement. Agreement in predictions deteriorates slightly on the portside line with differences of up to ~7% evident at $z = 3$ m.

The influence of WOD direction was assessed also in Ref. [68]. Figure 33 shows turbulence intensity plots for 6 WOD conditions over spots #2, #4, #7 and #8, where “green” winds denote winds from the starboard of the ship and “red” winds from the portside. As the wind conditions become more oblique, turbulence intensity over the landing spots increases significantly, with beam winds resulting in peak turbulence intensity values of ~1000%, which again should be viewed from a qualitative perspective. For turbulence intensities across the ship length shown in Figure 34, the magnitudes of the turbulence intensities for 60° and 90° winds are similar, with the latter condition producing greater turbulence over the entire flight deck including the fore

section. Note that no validation against experimental or full-scale test data was performed in this study [68].

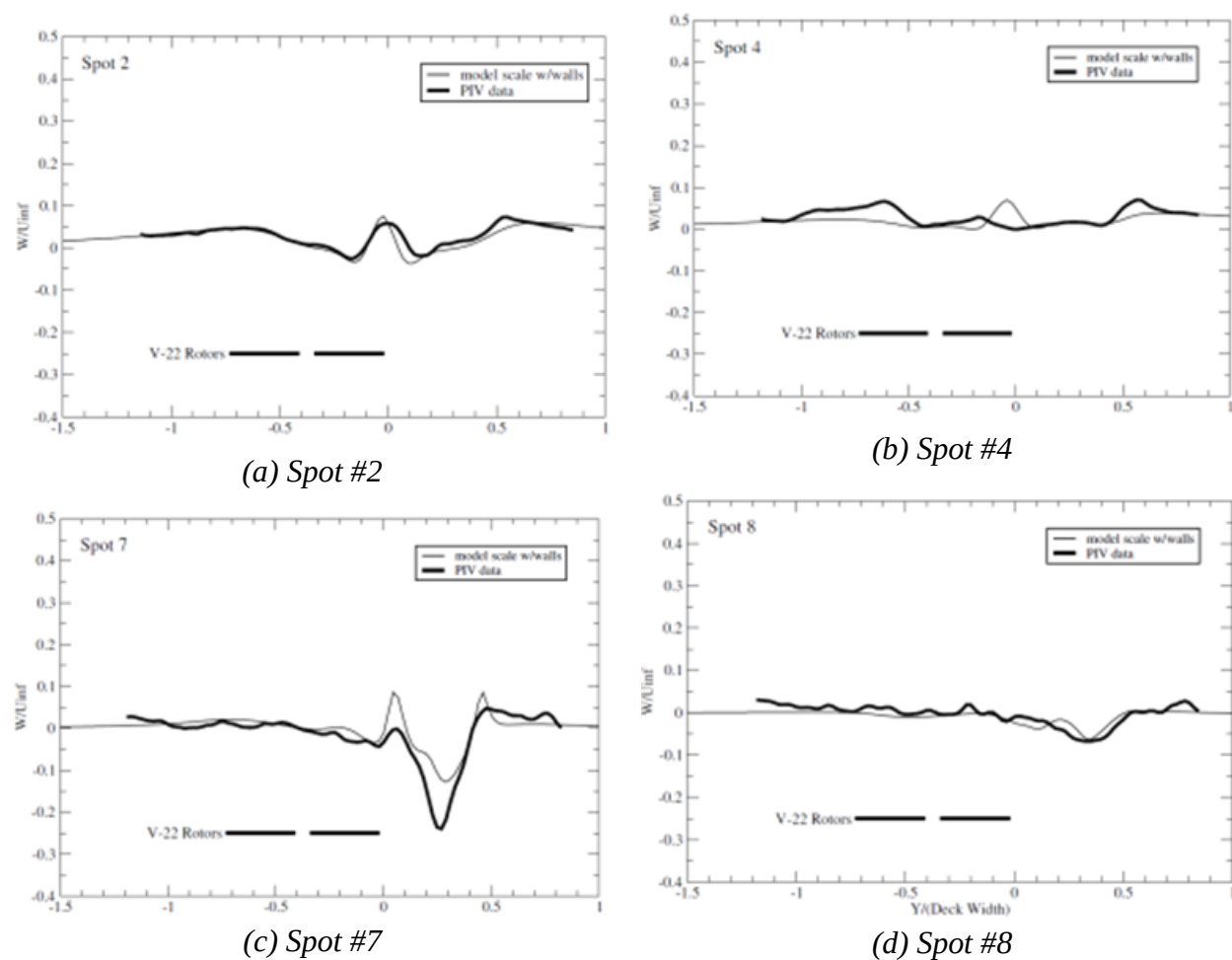


Figure 29: Mean vertical velocities at transverse planes across four landing spots for $z=5.206$ in [66]

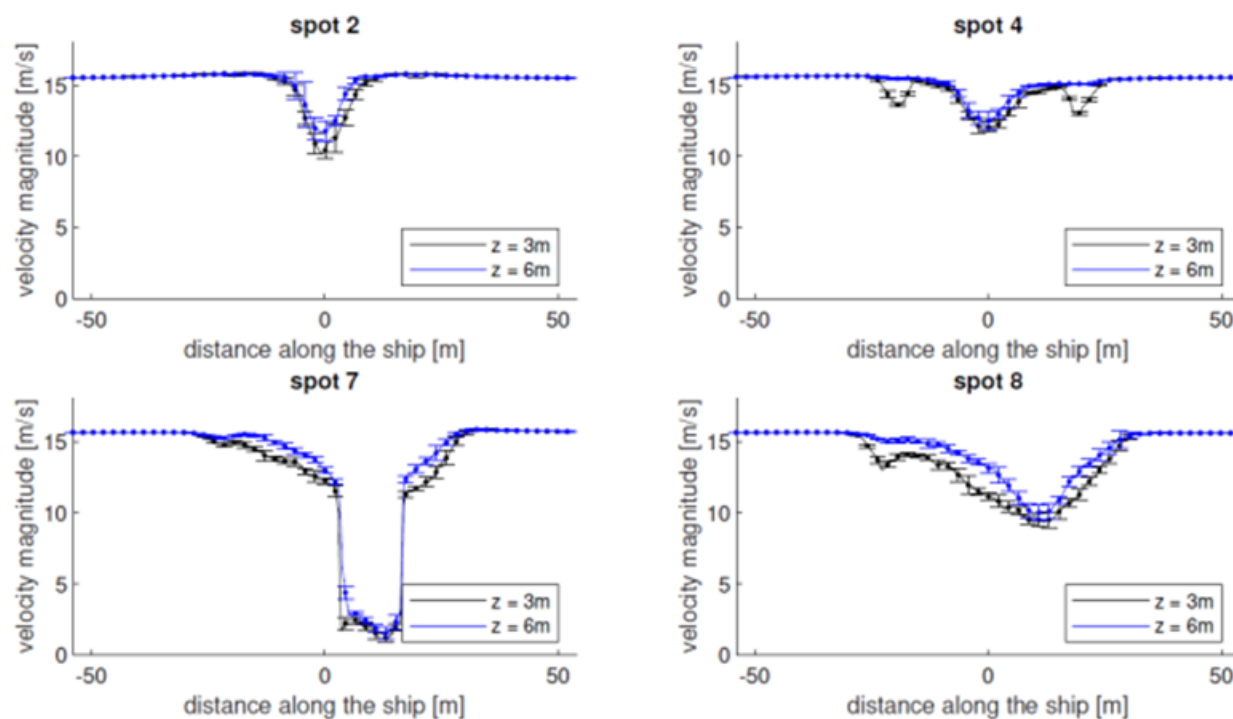


Figure 30: Mean velocity magnitude across landing spots of the LHA ship for a headwind condition [68]

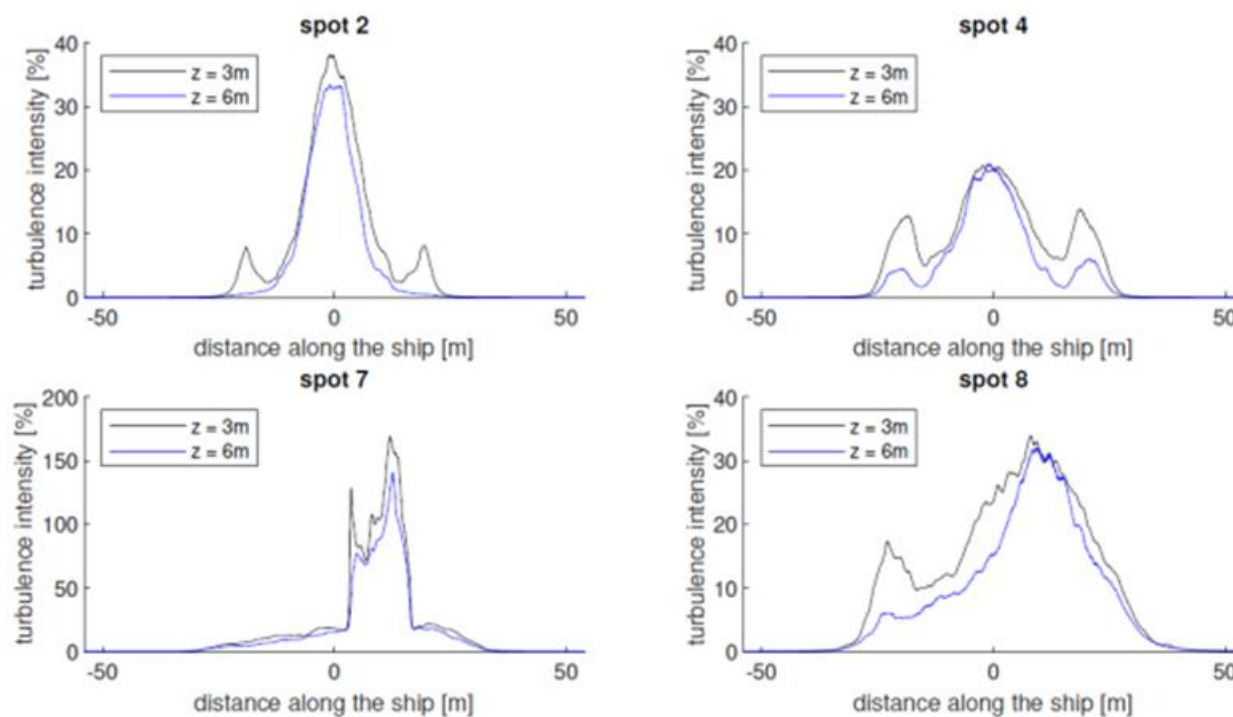


Figure 31: Turbulence intensities across landing spots of the LHA ship for a headwind condition [68]

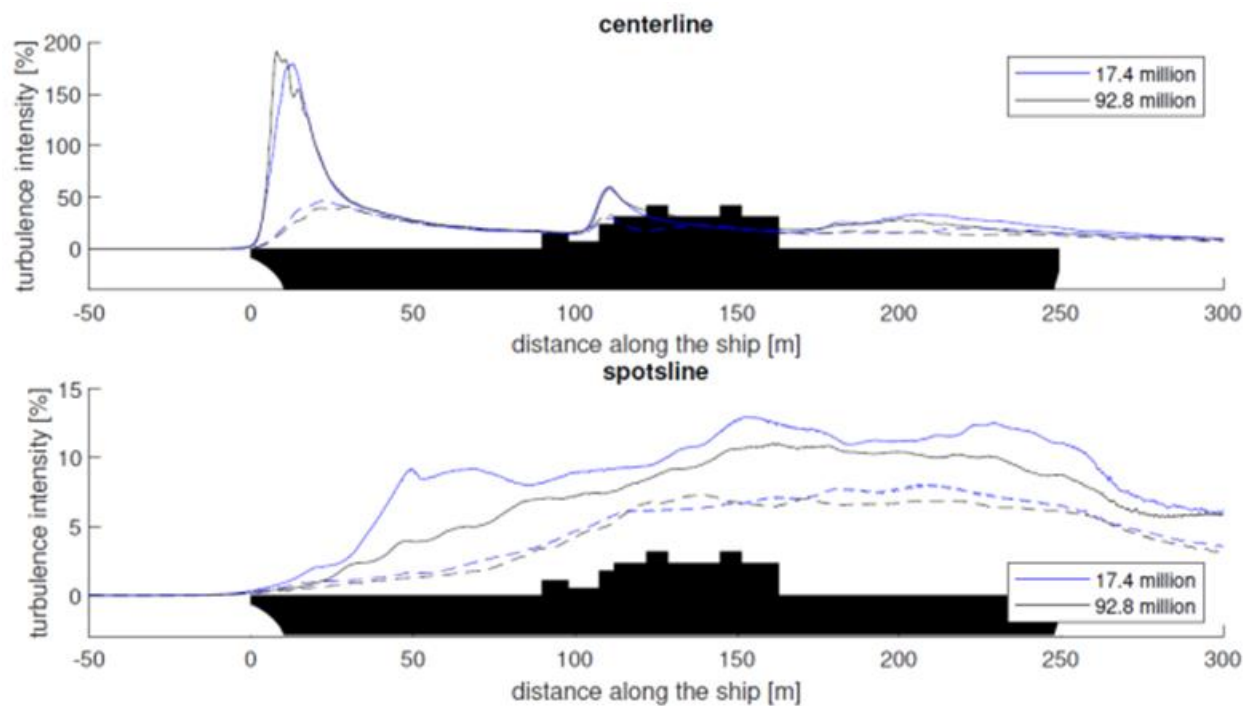
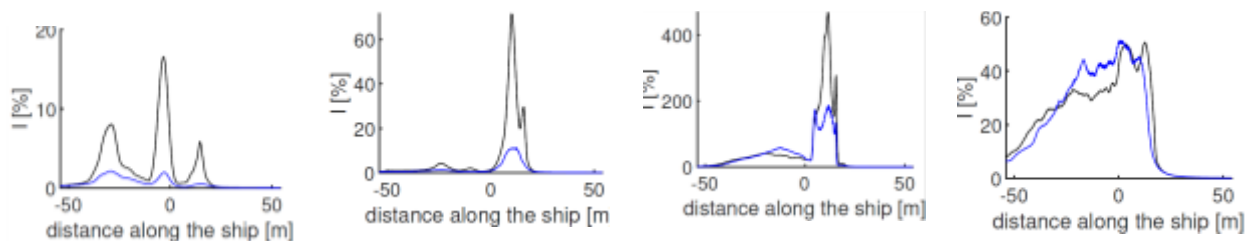


Figure 32: Turbulence intensities across LHA ship length for headwind condition; full line corresponds to $z = 3$ m and dashed line to $z = 6$ m [68]



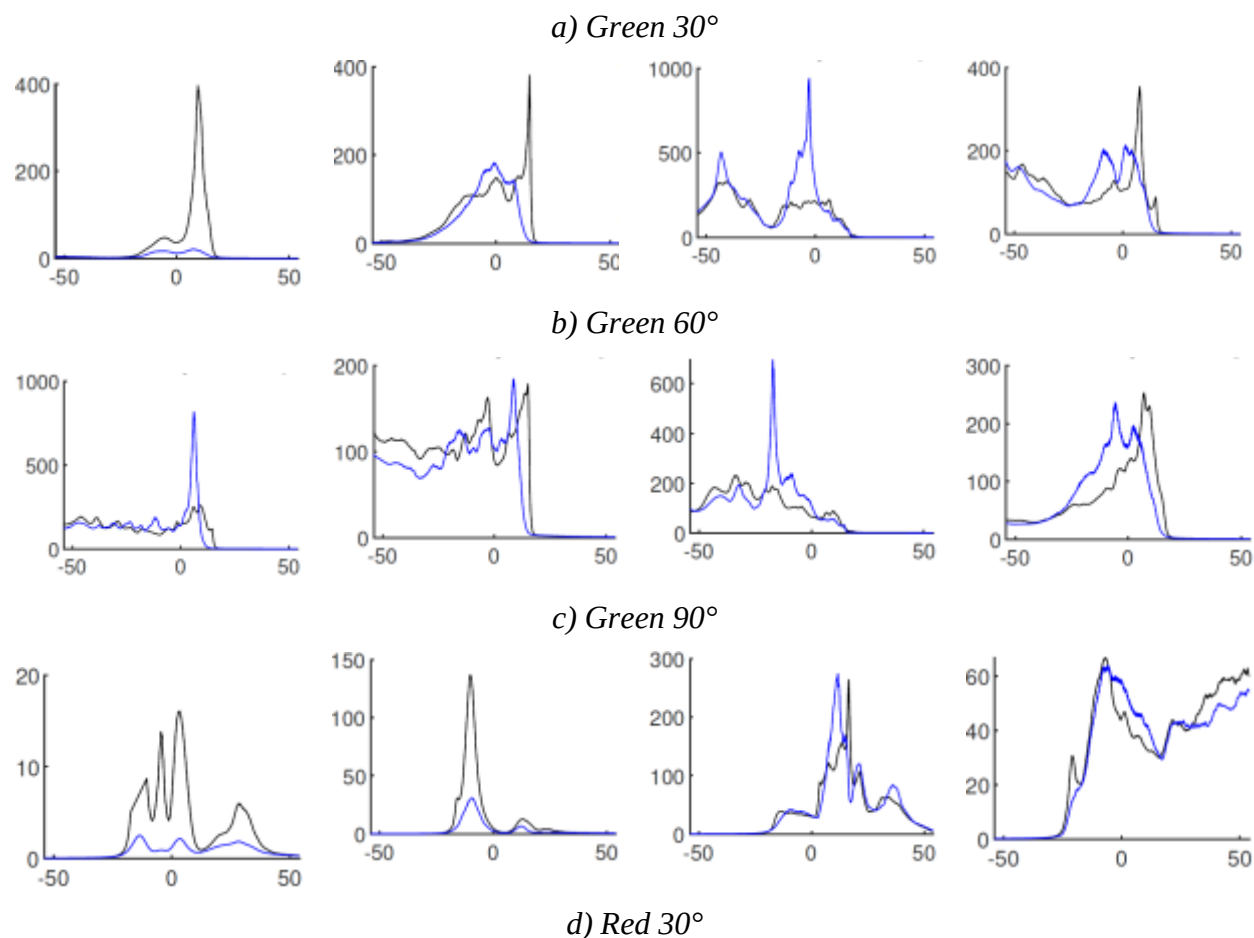
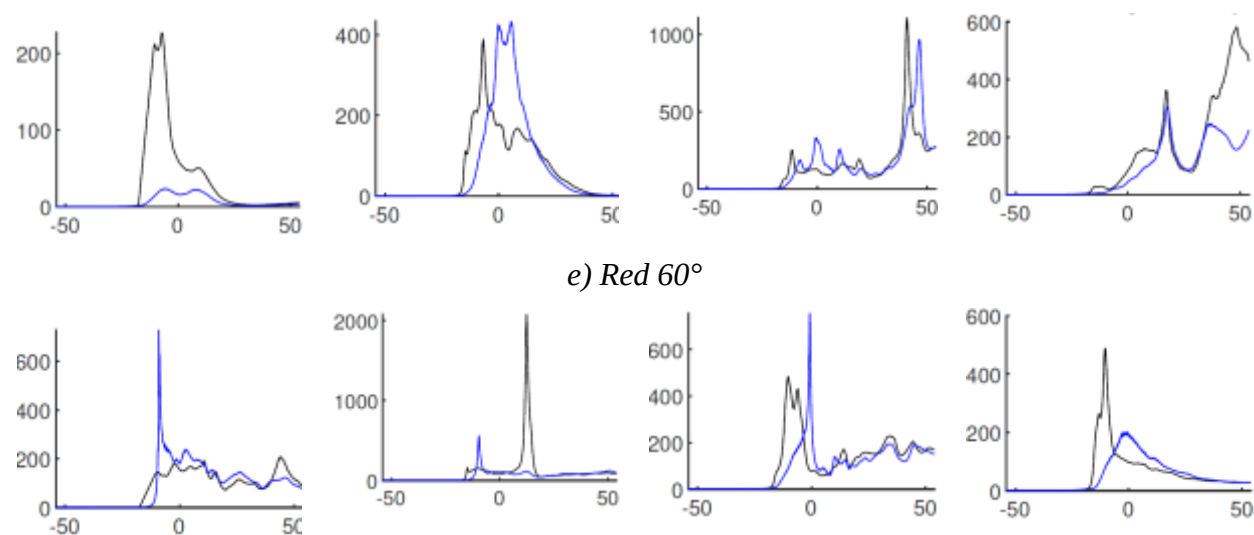


Figure 33: Turbulence intensities across landing spots #2, #4, #7 and #8 (in that order from the left) for various WOD directions; black line corresponds to $z = 3$ m while blue line corresponds to $z = 6$ m; “green” winds are from the starboard side of the ship and “red” winds from the portside [68]

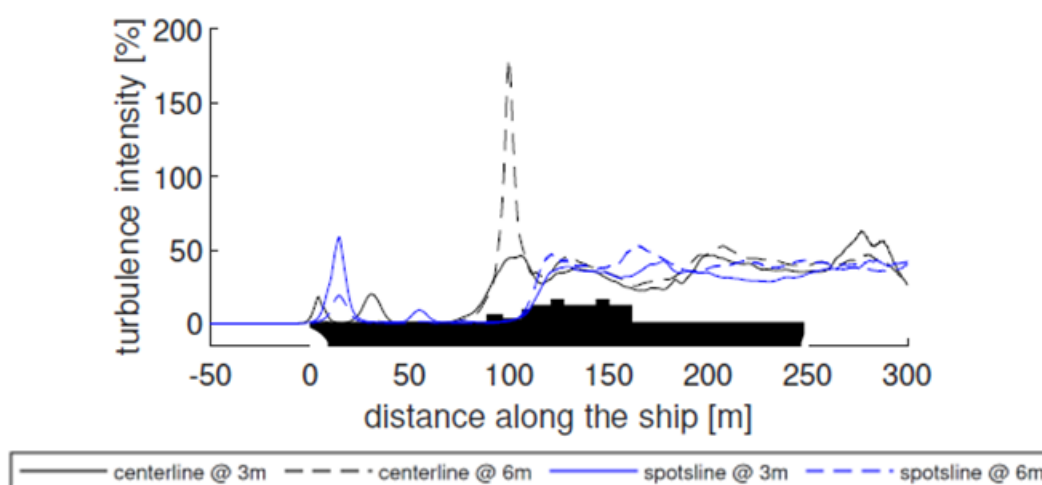


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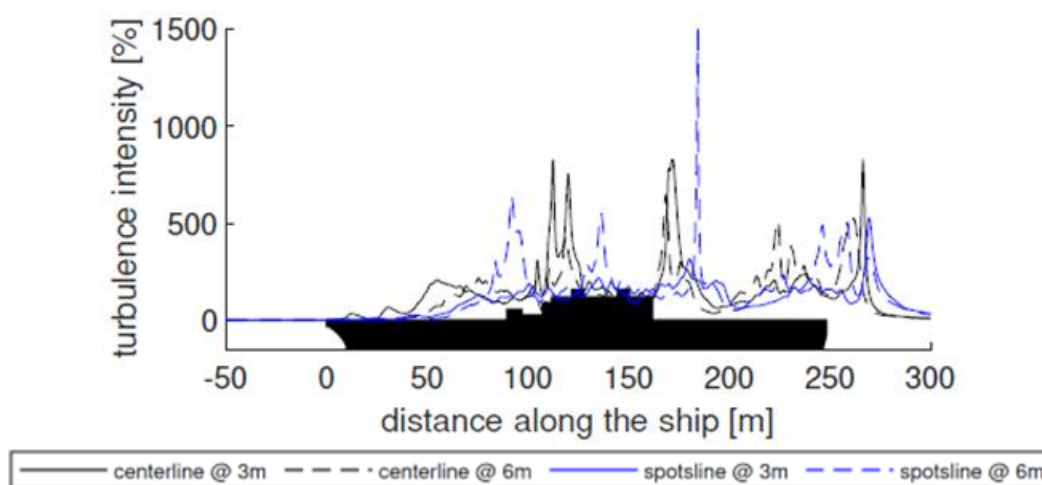
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f) Red 90°

Figure 33: Turbulence intensities across landing spots #2, #4, #7 and #8 (in that order from the left) for various WOD directions; black line corresponds to $z = 3$ m while blue line corresponds to $z = 6$ m; “green” winds are from the starboard side of the ship and “red” winds from the portside [68]



a) Green 30°

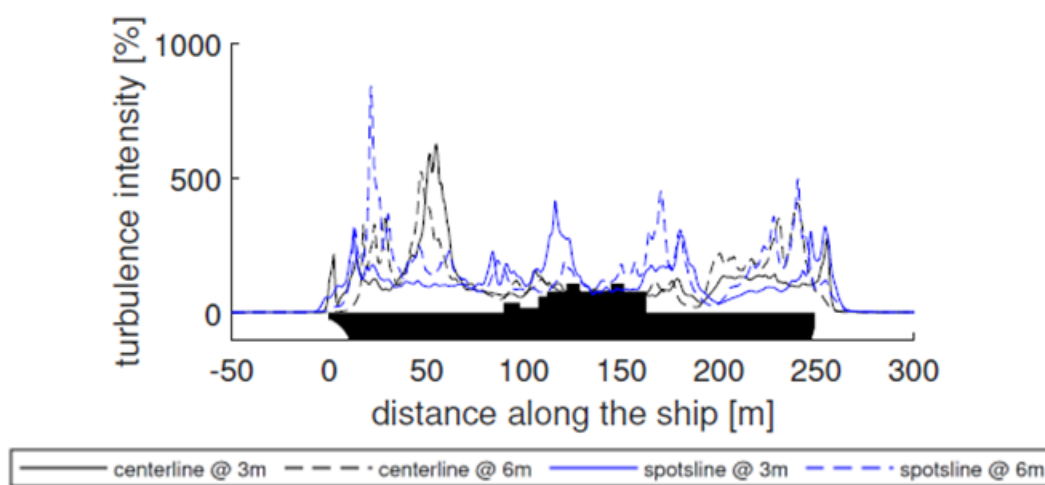


b) Green 60°

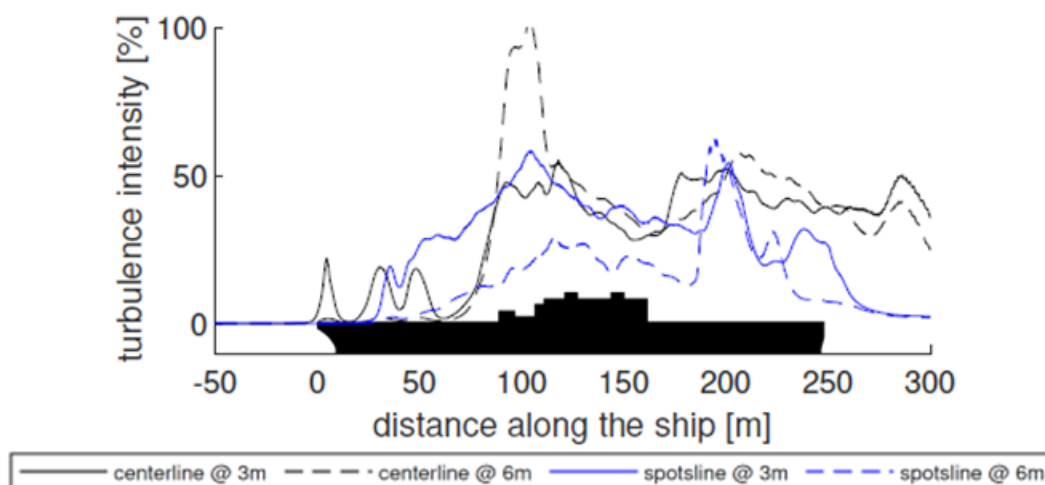
Distribution Statement A

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Figure 34: Turbulence intensities across LHA ship length for various WOD conditions; “green” winds are from the starboard side of the ship and “red” winds from the portside [68]



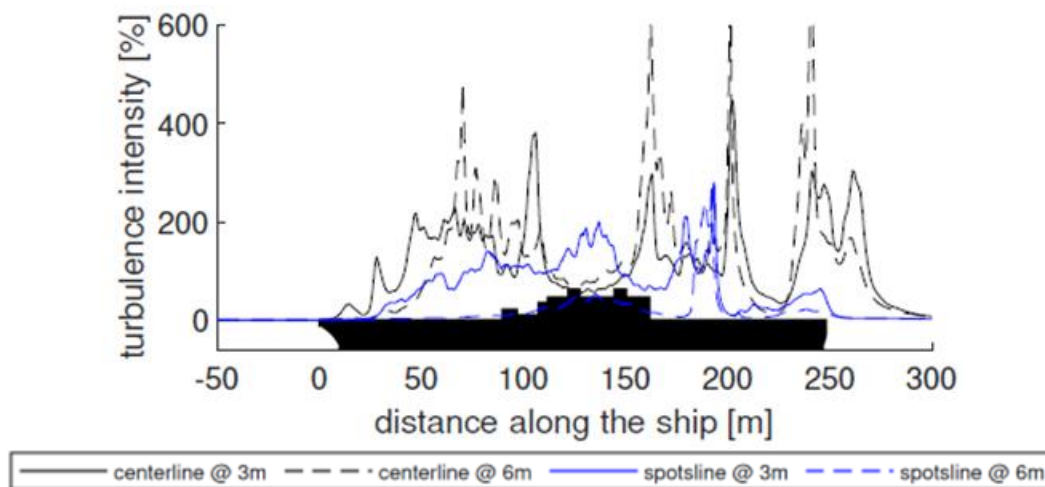
c) Green 90°



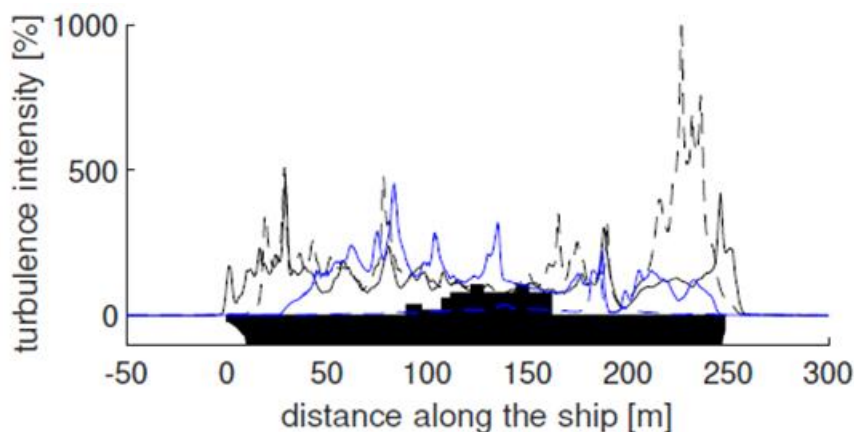
d) Red 30°

Figure 34: Turbulence intensities across LHA ship length for various WOD conditions;

“green” winds are from the starboard side of the ship and “red” winds from the portside [68]



e) Red 60°



f) Red 90°

Figure 34: Turbulence intensities across LHA ship length for various WOD conditions; “green” winds are from the starboard side of the ship and “red” winds from the portside [68]

CFD and experimental investigations of flow over an HMS Queen Elizabeth Class (QEC) aircraft carrier shown in Figure 35 were performed in Refs. [69, 70]. HMS QEC provides a 4-acre flight deck to support fixed- and rotary-wing aircraft, and includes a take-off ramp or “ski jump” to support short take-off, specifically for Lockheed Martin F-35B Advanced Short Take-Off and Vertical Landing (ASTOVL). A notable feature of this carrier is the twin island configuration with the front island serving as base for ship control and navigation and the rear one as base for flight operations. This twin island configuration results in greater turbulence over the deck span when compared to LHA class ships which have one island [69]. Watson et al. validated Delayed Detached Eddy Simulation (DDES) of flow over HMS QEC with measurements obtained from water-tunnel experiments of a 1/200th scale model [70]. Two Acoustic Doppler Velocimetry (ADV) probes were used to measure mean velocity components

and lateral and vertical velocity fluctuations. Water-tunnel experiments were performed to match Reynolds number to full-scale tests. CFD predictions of turbulence intensity, defined as

$$I = \frac{\left[\frac{1}{3} (\overline{u'^2} + \overline{v'^2} + \overline{w'^2}) \right]^{\frac{1}{2}}}{U_{\infty}}, \quad (6)$$

for the 1/200th scale model showed good agreement with experiments, as evident in Figure 36 [70].



Figure 35: HMS Queen Elizabeth aircraft carrier [70]

Subsequently, full-scale CFD simulations were performed with the atmospheric boundary layer modeled using surface roughness properties typical of oceanic surfaces [70]. Streamlines for headwind condition with turbulence intensity (see Eq. (6)) across a horizontal plane at $z = 10$ m are shown in Figure 37. Turbulence intensities of $\sim 12\%$ are noted due to the portside deck-edge vortex. Not surprisingly, peak turbulence occurs downstream to the superstructures, and similar plots for oblique WOD conditions are shown in Figure 38. For the green and red 25° and 45° WOD conditions, deck-edge vortices have turbulence intensities of $\sim 20\%$ across the deck. The greatest turbulence across the entire flight deck is evident in the green 90° condition.

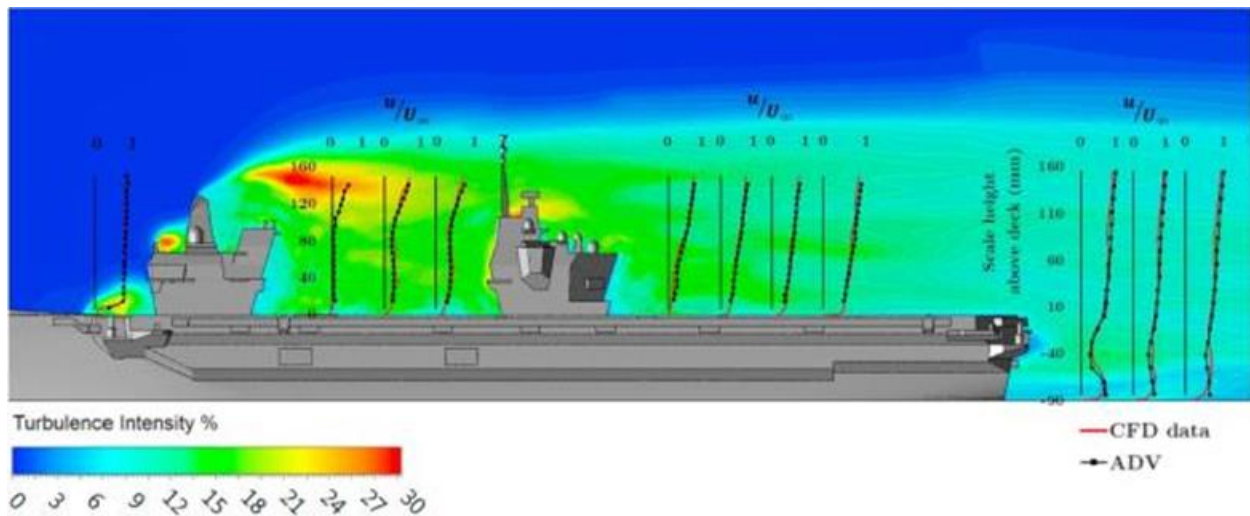


Figure 36: Comparison of experimental and CFD u -velocity components in a plane through center of islands [70]

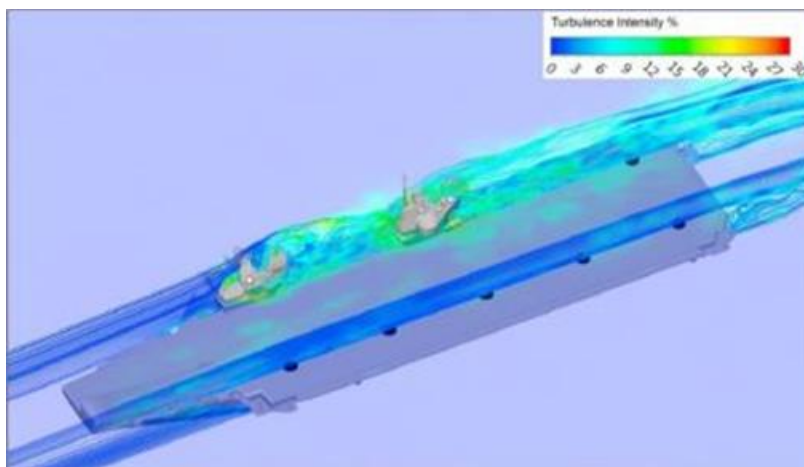


Figure 37: Streamlines for headwind condition in vertical planes through landing spots (black dots) and turbulence intensity contours in a horizontal plane 10 m above deck [70]

From the preceding discussions, it is evident that the geometry of a ship superstructure greatly affects the flow patterns observed in the aft of ship. A comparison of the mean flow characteristics around the windward vertical edge of the hangar for an oblique WOD condition is shown in Figure 39 for the Simple Frigate Shape 2 (SFS2) and Type 23 (T-23) Frigate ship geometries [62]. The ship geometries are shown in Figure 40 for reference. The superstructure of the T-23 Frigate does not cover the entire width of the deck as that of the SFS2 and includes a “notch”. Geometrical differences such as these greatly influence the resulting flow characteristics; the interaction of wind, the deck-edge vortex, and the superstructure results in a high vorticity region around the notch, which is not noted with the SFS2 in Figure 39.

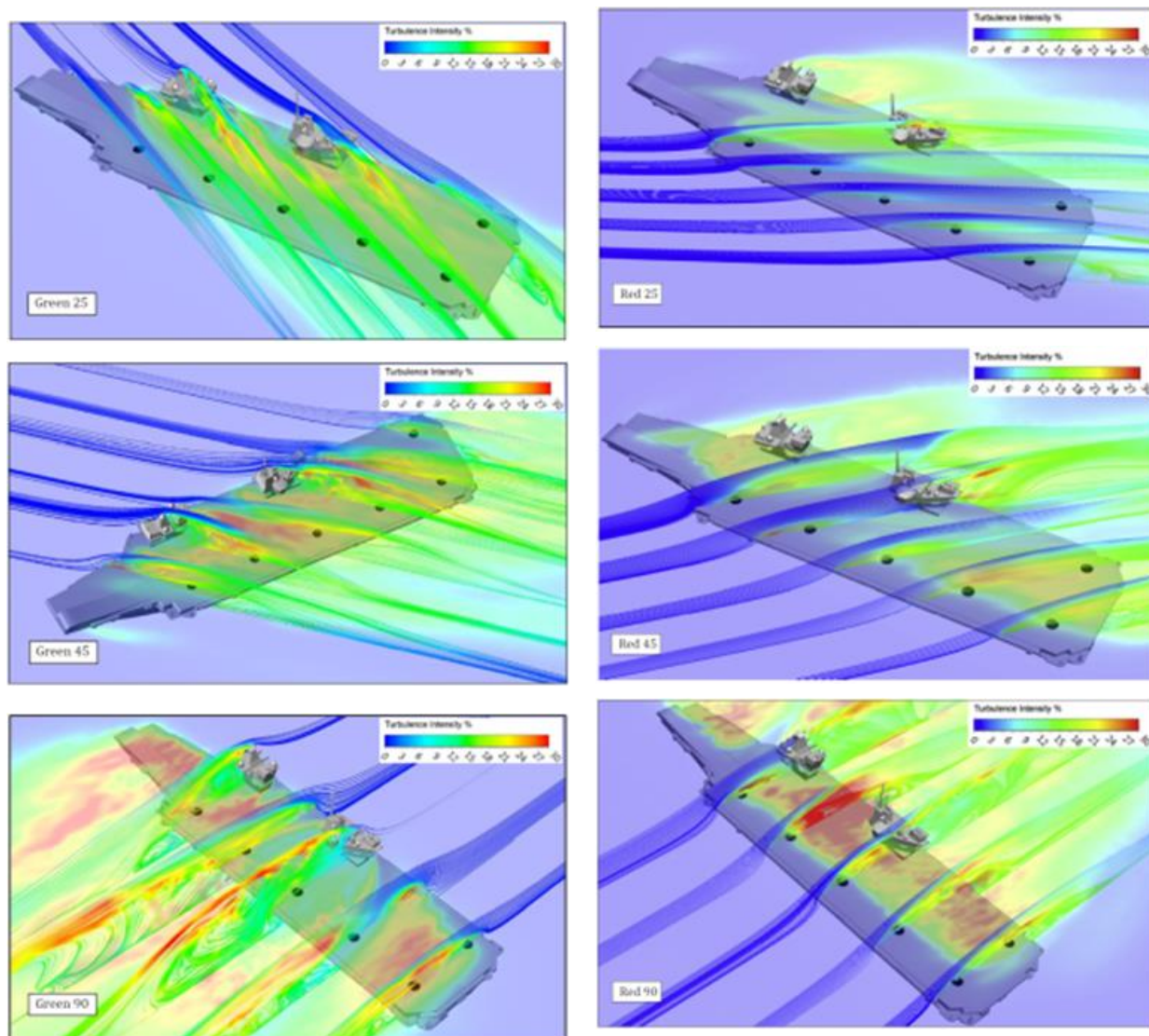


Figure 38: Streamlines in vertical planes for various WOD directions and turbulence intensity contours in a horizontal plane 10 m above deck [70]

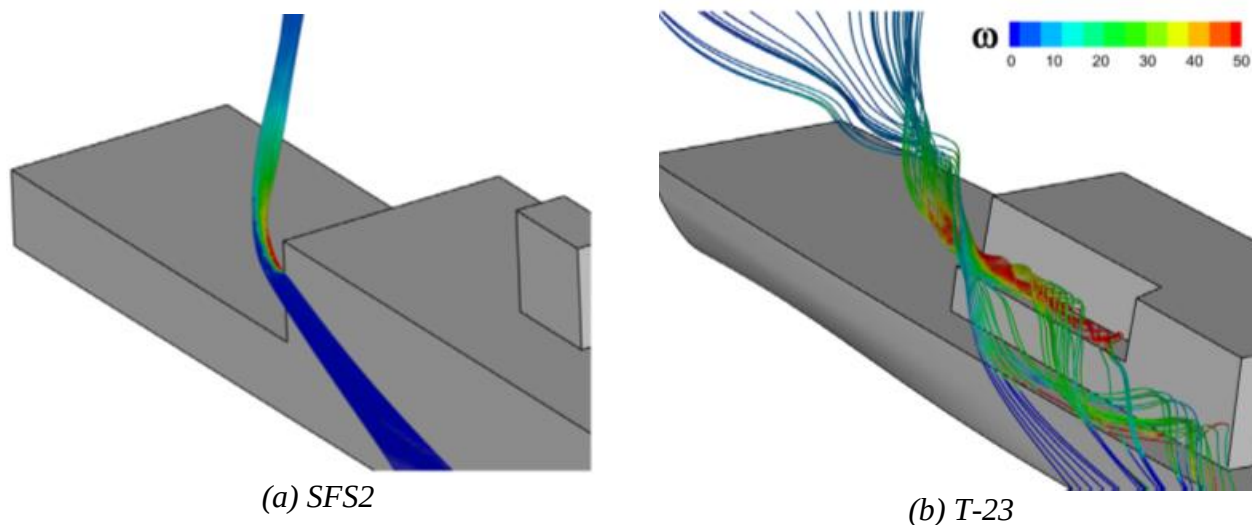


Figure 39: Mean path lines colored by vorticity magnitude for starboard 45° WOD condition highlighting separation characteristics from windward hangar edge [62]

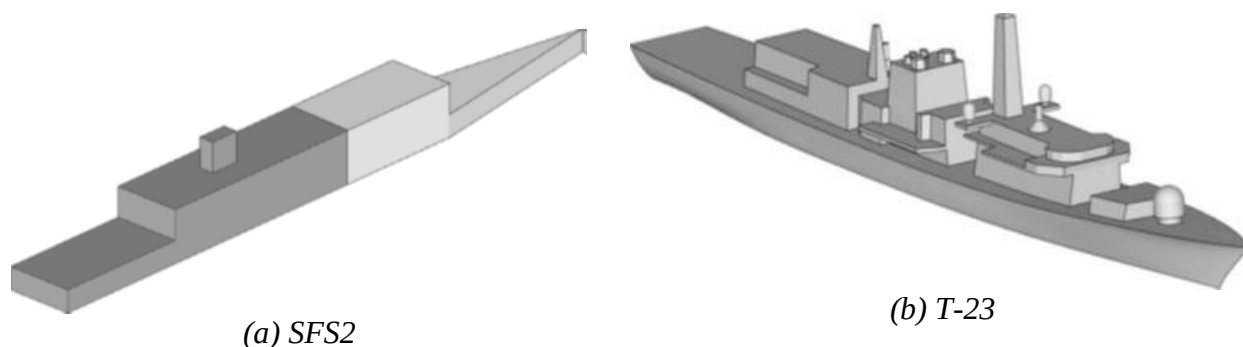


Figure 40: SFS2 and T-23 ship geometries [62]

Characterization of Corner Vortex Flow

Deck-edge vortices on a ship originate from corners and propagate along edges of the deck and superstructure. In oblique WOD conditions, relatively strong coherent vortical structures that can influence aircraft dynamics and handling qualities are shed from the corners. Characterization of flow features resulting from corner winds is lacking in ship airwake literature, but such phenomena have been looked at extensively by the wind engineering community given that vortices generated by corner winds can generate high suction regions on roofs of buildings, which, in turn, can lead to severe structural damage [71-74]. The large suction pressure regions are associated with two counter-rotating deck edge vortices that originate from the corner, as illustrated in Figure 41. In a similar manner to shear layers on backward facing steps, these vortices generate fluctuating pressure gradients by flapping over the top surface [71]. The size of each vortex depends on building dimensions, wind speed, wind incidence angle with the corner, and upstream (atmospheric) turbulence levels. Each vortex grows and decays alternatively (driven by the asymmetric shedding from the body), producing asymmetric instantaneous pressure distributions over the roof [75, 76]. Flow perpendicular to an edge produces separation bubbles over the edge and flow at an incidence angle of 45° results in

symmetric vortices along the edges. The magnitude of the peak suction pressure over the top surface tends to be greater for flow with low turbulence [71].

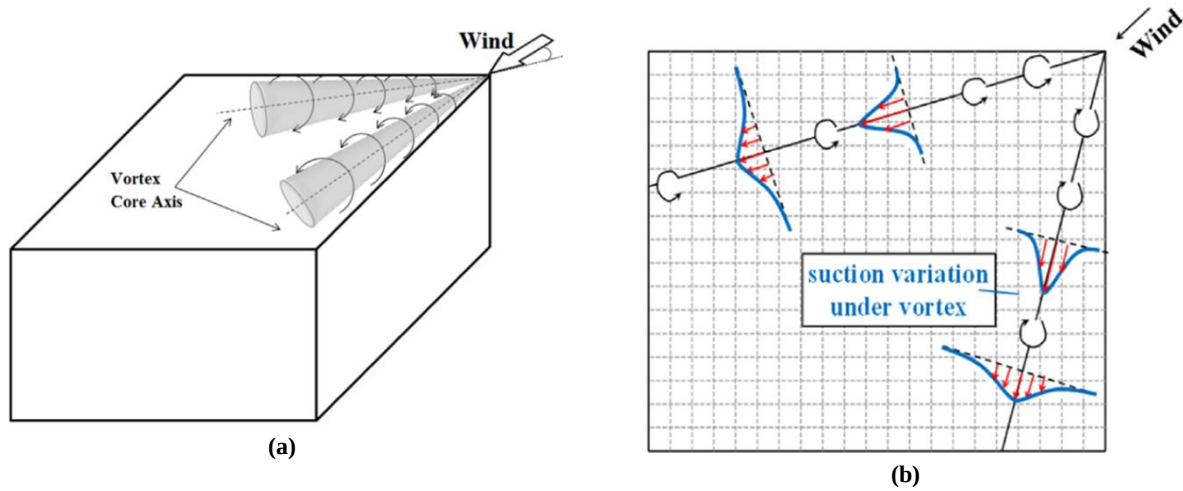


Figure 41: (a) Illustration of counter-rotating vortices generated, and (b) variation of suction pressure due to counter-rotating vortices over a flat roof from a corner wind [73].

Liu et al. in Ref. [72] performed wind tunnel experiments to assess the efficiency of passive vortex generators (PVGs) in mitigating pressure suction effects of the counter-rotating vortices on the top surface of a 1:244 scale model of a building that is 110 m long, 65.8 m wide, 22 m high and has a 5% roof slope—the centerline along the length of the roof is 1.64 m higher than the edges (5% of half of 65.8 m). An atmospheric turbulent boundary layer was simulated in the tunnel using the power law wind profile

$$U(z) = U_{ref} \left(\frac{z}{z_{ref}} \right)^\alpha, \quad (7)$$

where the power index α is set equal to 0.16, the roof height is taken as reference height z_{ref} with a corresponding reference speed U_{ref} of 8.6 m/s. Pressure measurements were undertaken following the pressure tape layout in Figure 42. Given the interest in flow phenomenology, only the baseline results without PVGs are of interest here.

Peak pressure coefficient C_p for various incidence angles are shown in Figure 43, indicating that the highest suction occurs at $\beta = 40^\circ$ and tap #21, close to the left edge in the blue corner zone in Figure 42. In the context of shipboard landing, the vortex along this edge is similar to a deck-edge vortex on an aircraft carrier that can influence aircraft that are on or near landing spots close to the edge of the ship.

Liu et al. [72] also quantify the size of the vortices for the $\beta = 40^\circ$ incidence condition, as shown in Figure 44. The vortex center axis (red line on the figure) is determined using the position of maximum mean suction pressure on cross-sections perpendicular to the wind direction. The radius of the vortex (black line on the figure) is estimated using the position of maximum root-mean-square (RMS) of pressure on the cross sections. The vortex along the longer edge of the building continually expands and moves away from the edge, while the vortex along the shorter edge expands slightly and remains closer to the edge. In general, the vortex along the edge that is at a smaller angle relative to the incoming flow is larger in size [71].

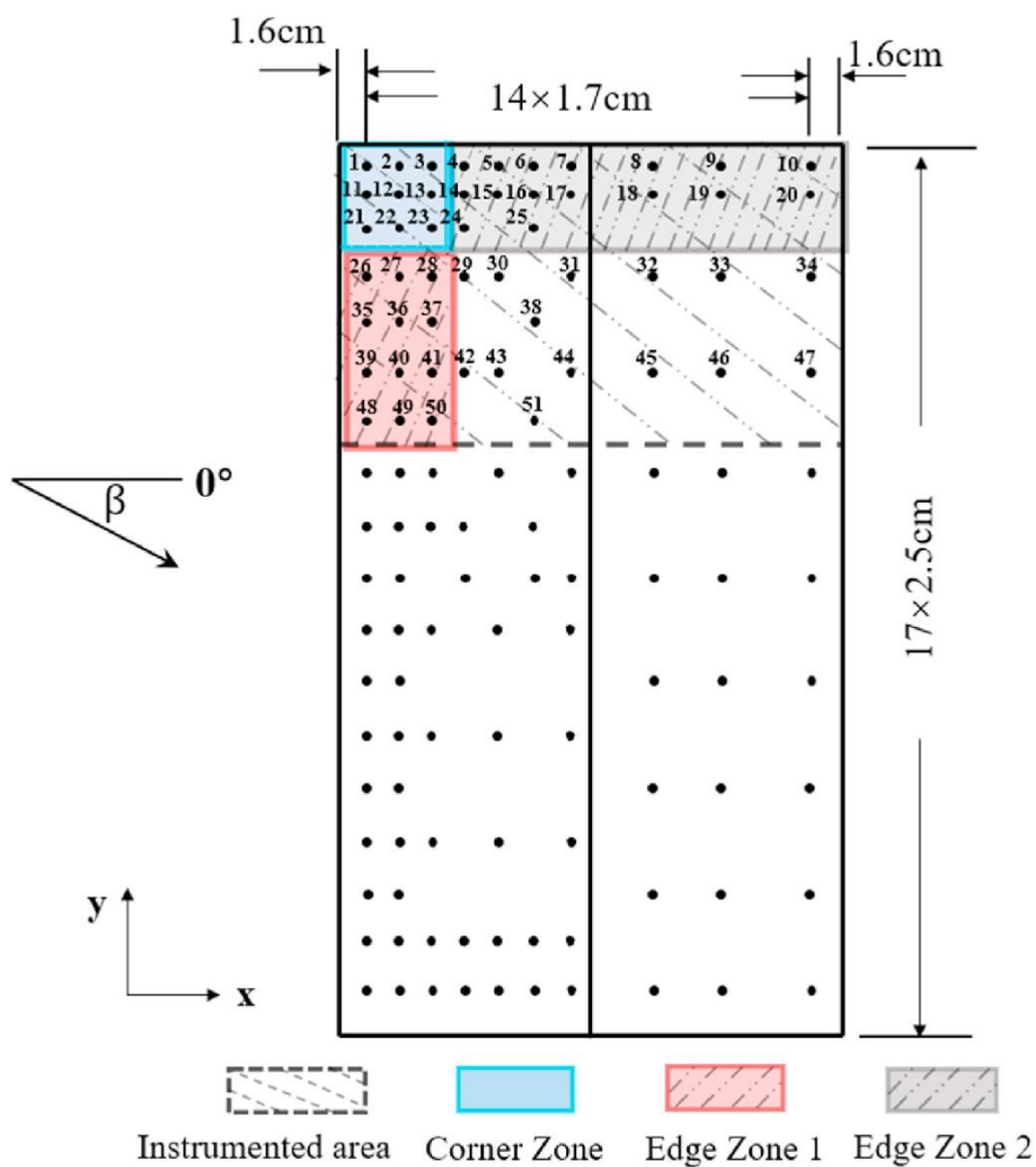


Figure 42: Pressure tap arrangement on top surface of building in Ref.[72].

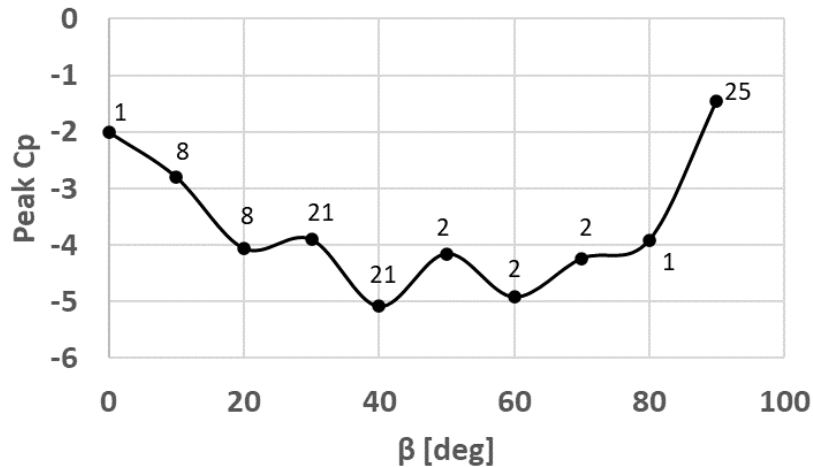


Figure 43: Peak C_p measured for different incidence angles; tap number indicated [72].

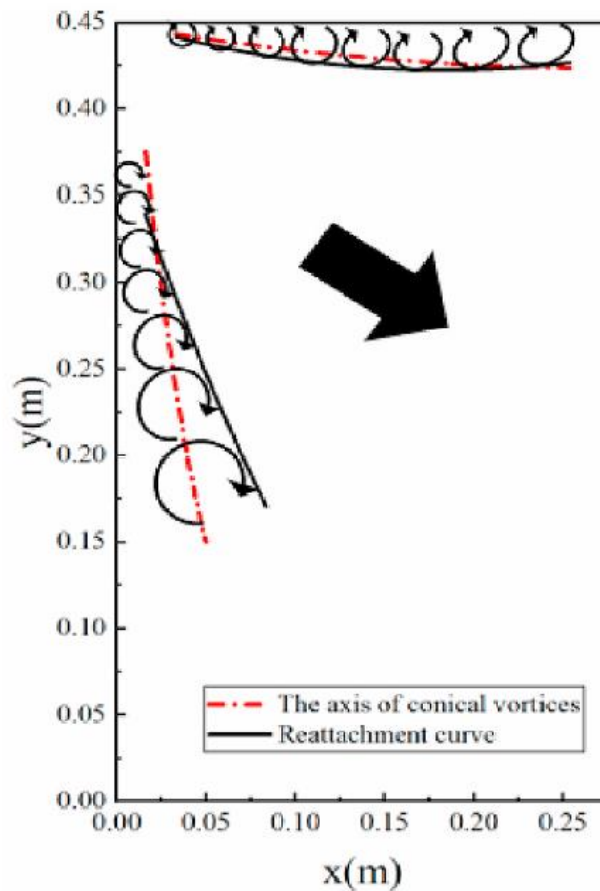


Figure 44: Range of counter-rotating vortices for $\beta = 40^\circ$ condition. Adapted from [72]. Please note that there is an error in the original paper that shows the sign of the vortex along the shorter edge pointing in the opposite direction.

Power spectral density (PSD) plots of pressure measurements at tap #21 for $\beta = 40^\circ$ and tap #2 for $\beta = 70^\circ$ are shown in Figure 45 and Figure 46 respectively, where $f \cdot H/U$ is the Strouhal characterization frequency with building height H as the characteristic dimension, and

$f \cdot S(f)/\sigma^2$ is the dimensionless power spectrum with $S(f)$ being the measured pressure spectrum and σ^2 is the variance of the measurements [72]. It should be noted that pre-multiplying the frequency by $S(f)/\sigma^2$ always results in peaks in the spectrum, even if the pure signal does not contain any dominating periodicity [71]. The “NO PVG” plots in Figure 45 and Figure 46 are of interest here, and show that for $\beta = 40^\circ$, the most energetic flow feature occurs at a Strouhal frequency of 0.07 with additional peaks at higher values such as 0.15 and 0.17. For $\beta = 70^\circ$, the most energetic flow features occur at Strouhal frequencies of 0.07, 0.13 and 0.19.

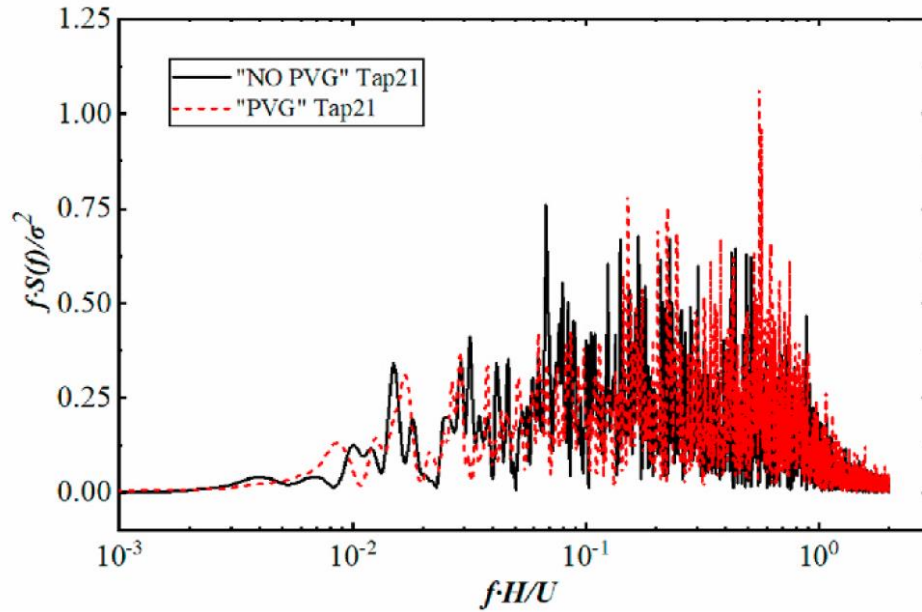


Figure 45: Power spectrum of pressure measurements at tap #21 for $\beta = 40^\circ$ [72].

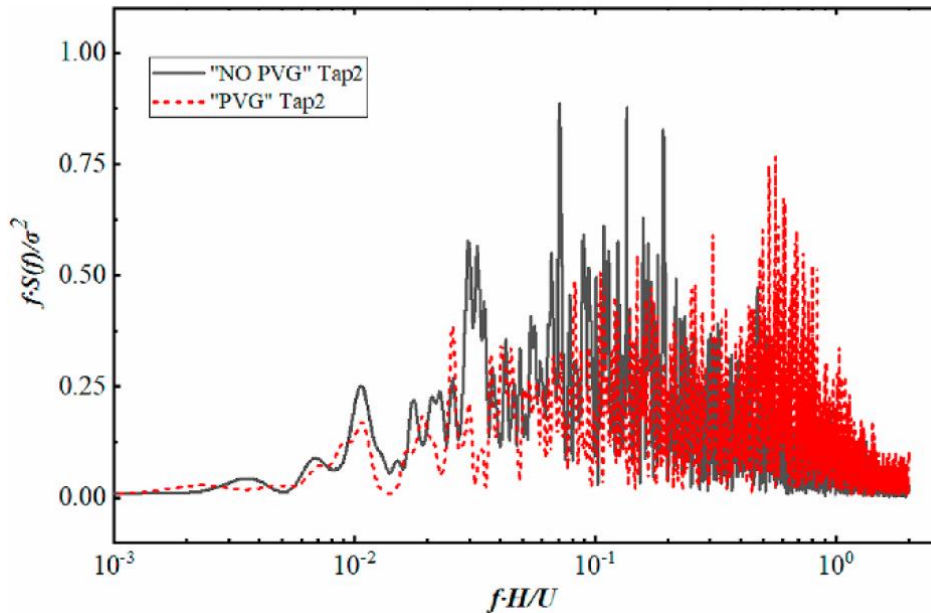


Figure 46: Power spectrum of pressure measurements at tap #2 for $\beta = 70^\circ$ [72].

Kozmar in Ref. [71] experimentally investigated the pressure distribution and fluctuations over the top surface of a 0.2 m wall-mounted cube due to corner winds for three simulated atmospheric boundary layers corresponding to low- (LT), mid- (MT) and high-turbulence (HT)

levels. The power index α in Eq. (7) was set to 0.17, 0.21 and 0.35, for values of U_{ref} of 14.97, 13.48 and 10.14 m/s, respectively with flow incidence angle of 40° . Contours of the steady pressure distribution over the cube are shown in Figure 47 showing that the magnitude of the suction peak on the top surface decreases with increasing upstream turbulence level.

The root-mean-square (RMS) of the pressure coefficient ($C_{p,rms}$) along three lines on the top surface of the cube are shown in Figure 48. Figure 48(a) shows $C_{p,rms}$ underlying the conical vortex along the leading edge. The pressure peaks close to the corner of the cube and decreases downstream as wake expands. In Figure 48(b), a decrease in suction pressure is noted closer to the edge, where the boundaries of the two counter-rotating vortices interact. The line considered in Figure 48(c) starts at a region outside the vortex, progresses towards the center, and ends in a region underlying it. Suction pressure underneath the vortex is approximately 3-4 times higher than that outside the vortex, indicating the potential adverse impact these vortices can have on the surface and on aircraft operating within it. For all three profiles, increasing the upstream turbulence level results in greater flow diffusion over the top surface. Greater fluctuations in $C_{p,rms}$ were observed, but it is not clear whether this is a consequence of unsteadiness in the inflow, or the vortices moving more.

Power spectral density plots of surface pressure fluctuations at various points along the three lines considered in Ref. [71] (see Figure 49 to Figure 51). Close to the corner at point #1 in Figure 49 vortex shedding due to the body edges occurs at a characteristic (for a cube [52]) Strouhal frequency of 0.1. As the vortex develops from point #1 to #5, the primary frequency progressively shifts to ~ 0.03 and a secondary peak at $St = 1.0$ emerges and becomes dominant. This change in periodicity has been attributed to reattachment of the flow on the top surface downstream from the corner [71]. The shift in primary periodicity is also noted in PSD plots for the $x/H = 0.25$ profile in Figure 50. The secondary high-frequency peak is not as pronounced along this profile. The PSD plots in Figure 51 correspond to a profile where points #3 and #8 are outside the conical vortex region and points #11 and #12 are underneath the conical vortex. Outside the conical vortex region, the dominant periodicity occurs at $St = 0.03$ with a less pronounced secondary peak at $St = 2.0$. As we move along the profile into the region underneath the vortex, the secondary peak at $St = 2.0$ becomes more dominant.

From the preceding discussion, corner flow produces a complex distribution of pressure with varied periodicity over a surface. It is important to note that the wind engineering community tends to focus on surface level measurements. For the ship-helicopter DI scenario, we also need to characterize corner flow properties above and beyond the surface since those are the regions where aircraft operate.

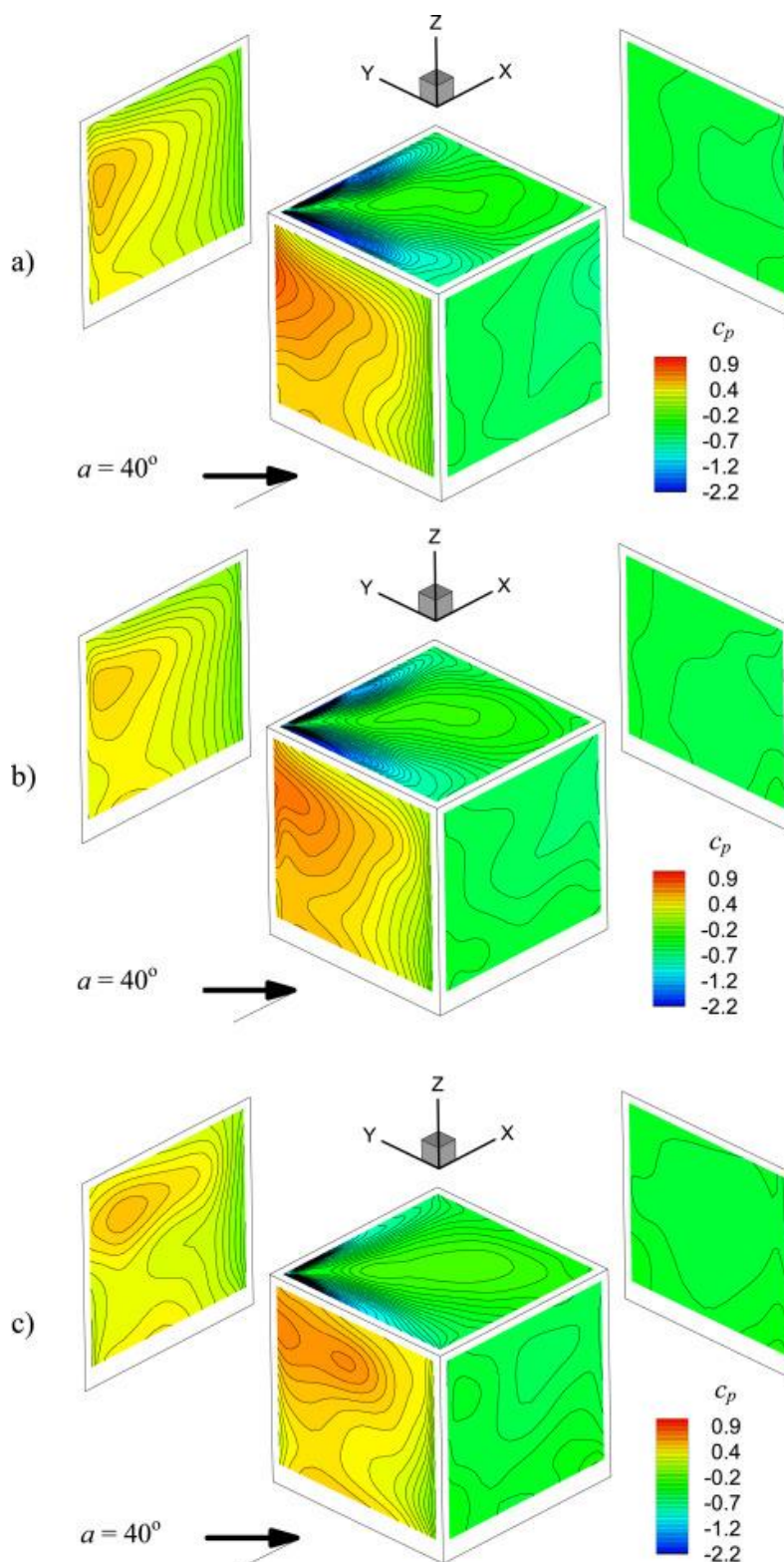


Figure 47: Contours of steady pressure distribution over surfaces of a cube for a corner wind at an incidence angle of 40° [71].

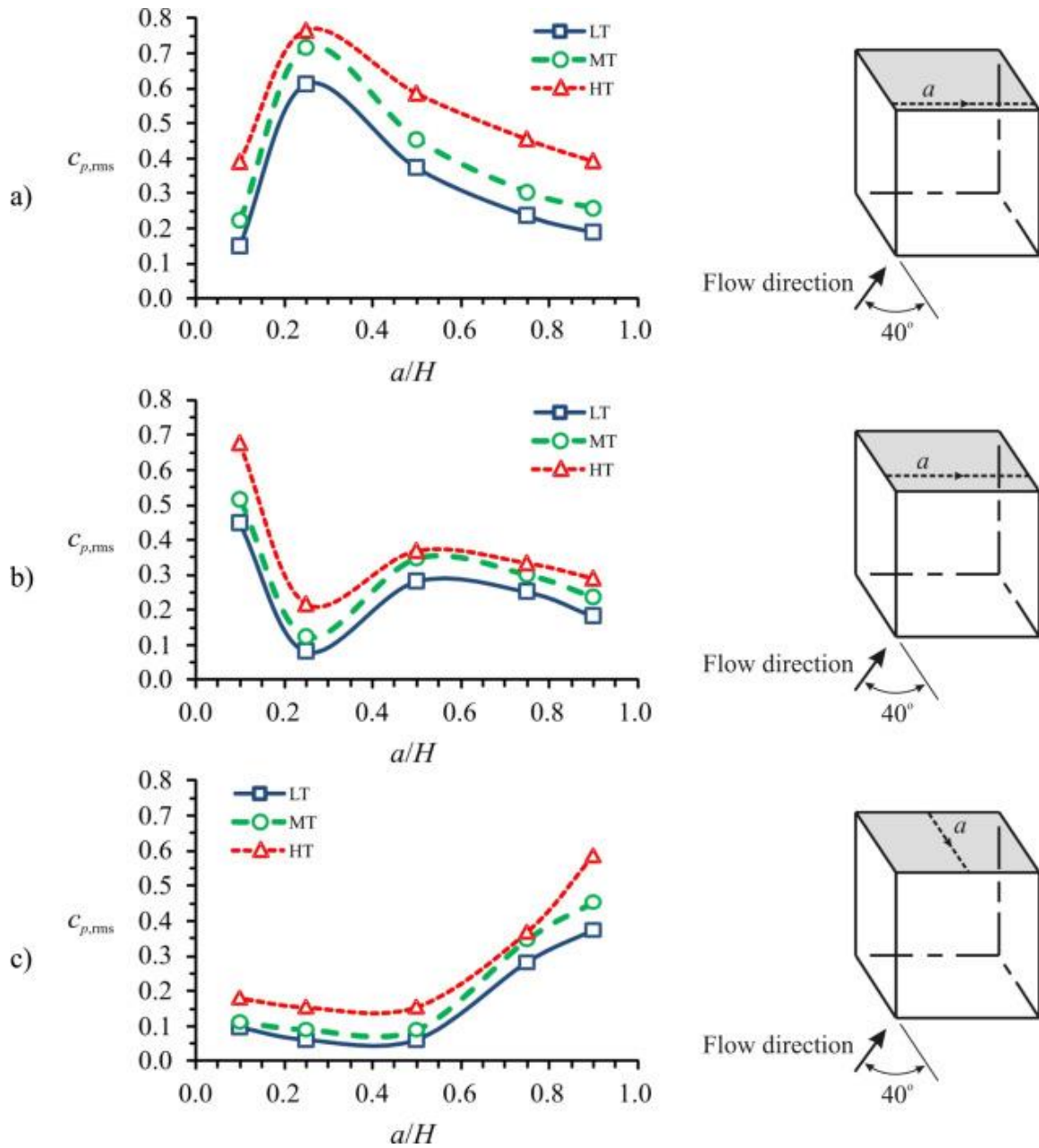
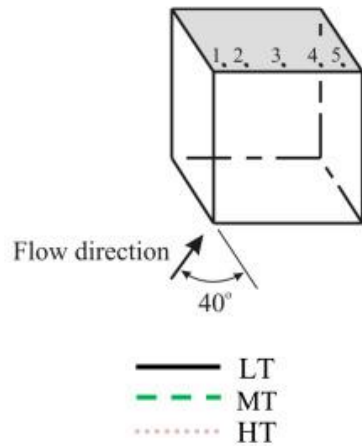
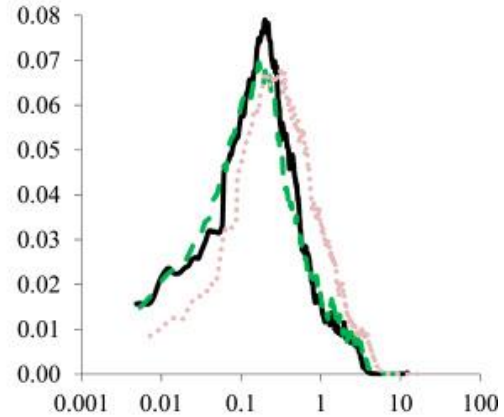


Figure 48: Profiles of $C_{p,rms}$ coefficients on the top surface of the wall-mounted cube at (a) $x/H = 0.1$, (b) $x/H = 0.25$ and (c) $y/H = 0.5$ [71].

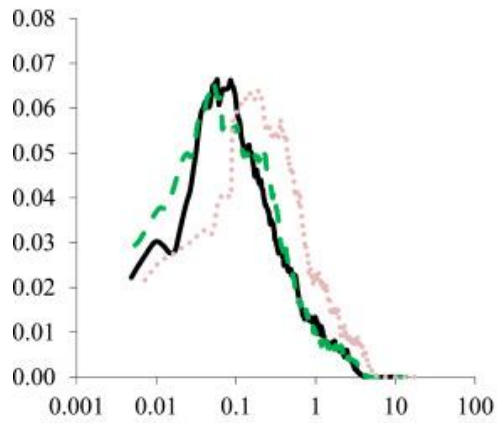
a) Schematic view and data legend



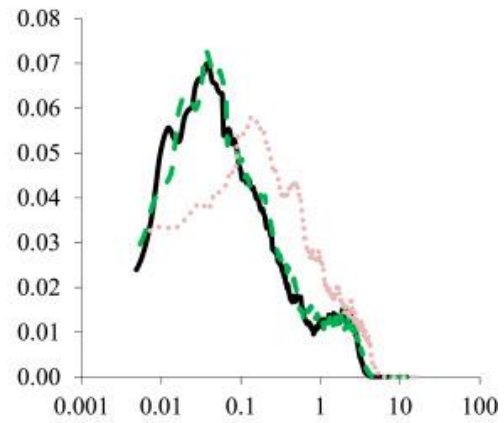
b) Point 1



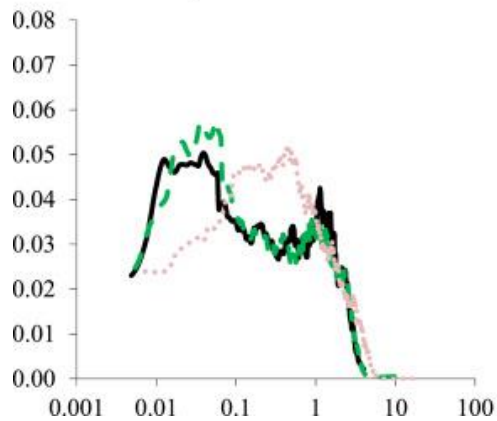
c) Point 2



d) Point 3



e) Point 4



f) Point 5

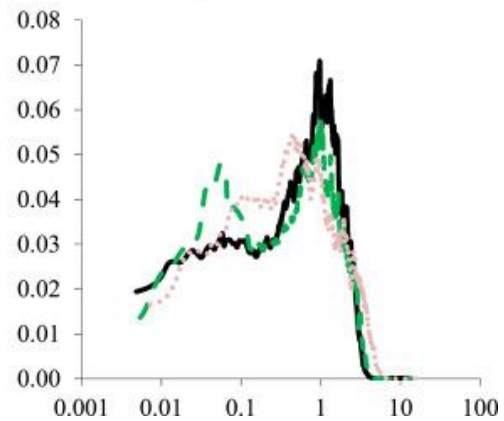


Figure 49: PSD of surface pressure fluctuations on top surface of cube along $x/H = 0.1$; x -axis is Strouhal frequency $f \cdot H/U$ and y -axis is the dimensionless power spectrum [71].

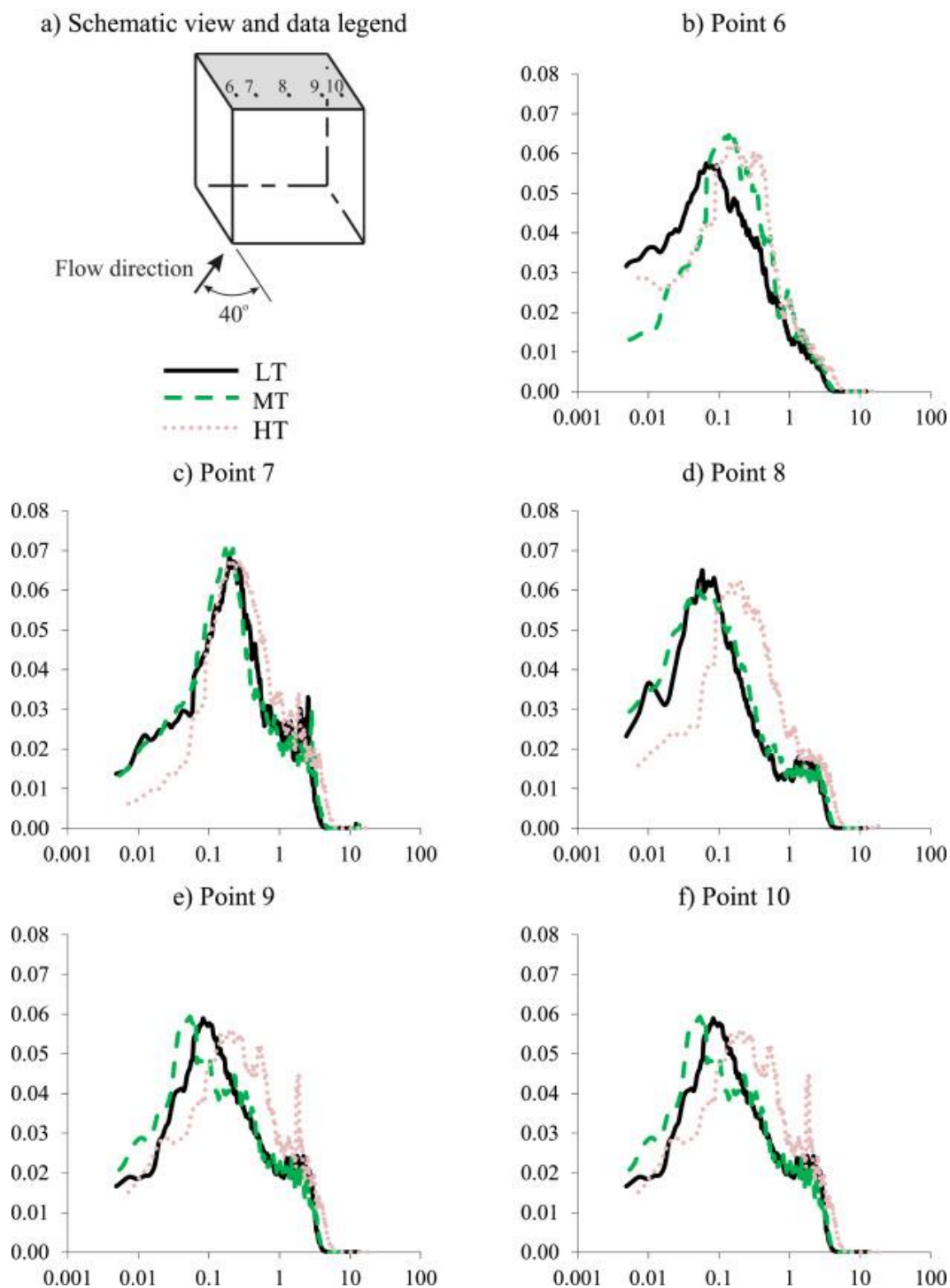


Figure 50: PSD of surface pressure fluctuations on top surface of cube along $x/H = 0.25$; x -axis is Strouhal frequency $f \cdot H/U$ and y -axis is the dimensionless power spectrum [71].

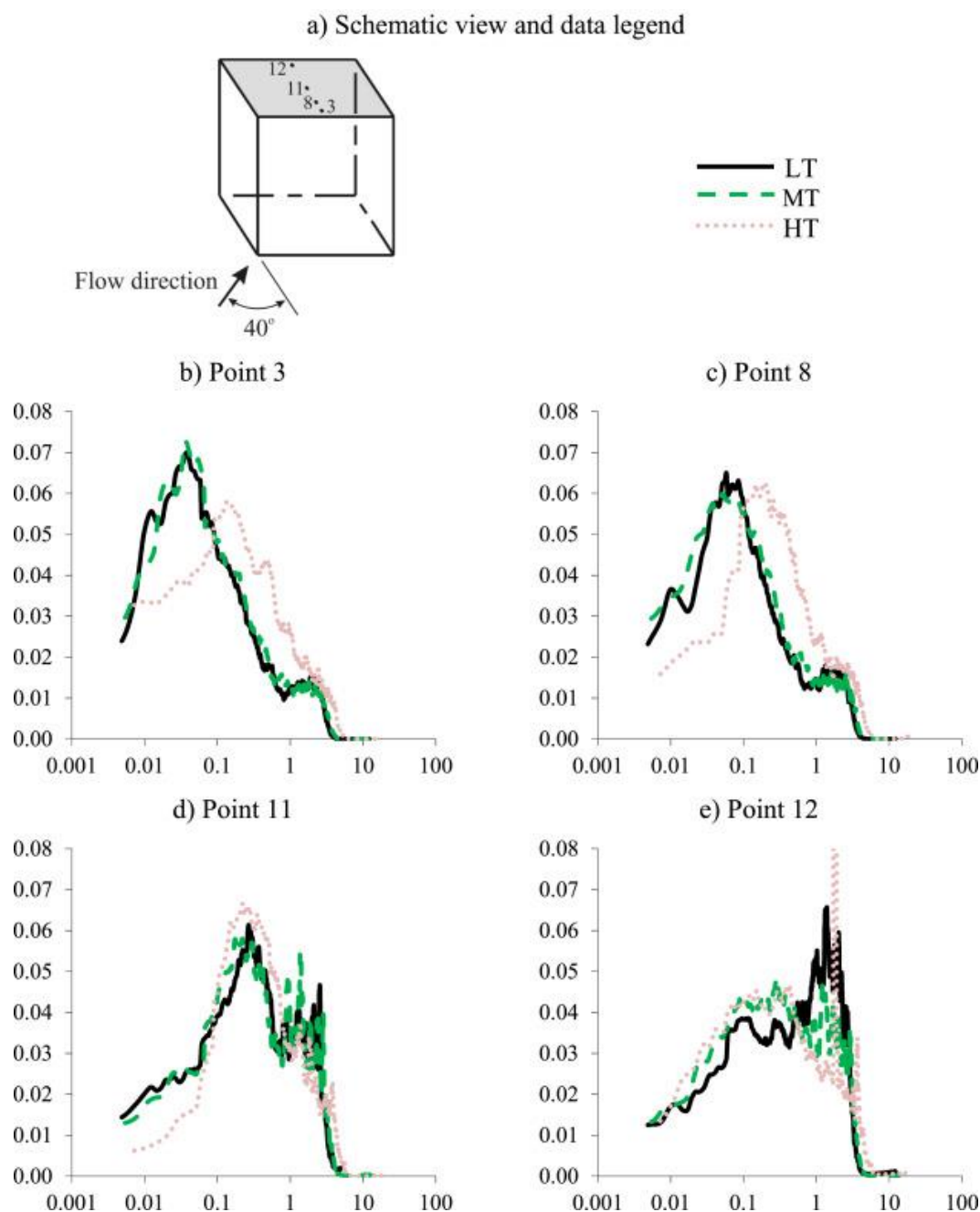


Figure 51: PSD of surface pressure fluctuations on top surface of cube along $y/H = 0.5$; x -axis is Strouhal frequency $f \cdot H/U$ and y -axis is the dimensionless power spectrum [71].

Influence of Ship Motion on Ship Airwake

The ship-helicopter DI is further complicated by wave induced motion of the ship [77], which can alter the vortex shedding strength and frequency of the airwake, and also induce a phenomena known as vortex “lock-in” [78], where the natural airwake shedding frequencies are reinforced by oscillatory motion of the ship. These effects can, in turn, influence pilot workload during shipboard operations. Most investigations in literature tend to focus on airwake over static ship configurations, with ship motion receiving attention only recently [78-81].

The influence of static and dynamic pitch attitude of a 1/100 scale model of the SFS2 ship geometry (see Figure 40) on ship airwake was investigated by Sydney et al. in Ref. [78] using PIV measurements of longitudinal and vertical velocities along the symmetry plane of the ship model. Time-averaged velocity magnitude contours for the baseline condition with the ship at an even keel in a headwind are shown in Figure 52 with relevant flow features highlighted.

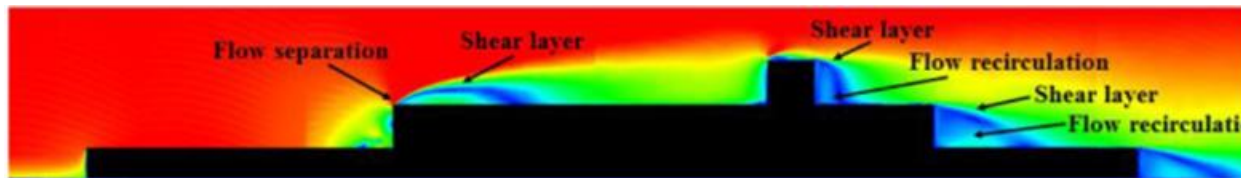


Figure 52: Time-averaged velocity magnitude over SFS2 model ship at even keel in a headwind; red indicates high velocity and blue indicates low velocity [78]

When the ship is statically pitched, the wake boundary – the layer between the ship airwake and freestream – translates higher, as evident in Figure 53 [78]. Note that the pitch angle is applied relative to waterline point of the bow. Wake boundary translation is due to airwake from the bow and front step of the superstructure, both of which are located at greater heights when the ship is pitched compared to the baseline case. Corresponding TKE contours are shown in Figure 54. As pitch attitude increases, the point at which turbulent airwake contributions from the front step, funnel or stack, and rear step interact moves closer to the hangar, and the recirculation region immediately aft of the hangar shrinks. The magnitude of the turbulence above the ship also increases relative to the baseline case.

Dynamic pitching of the ship at 1 and 2 Hz with an amplitude of 2.5° was also considered [78], and instantaneous velocity magnitude contours over one cycle are shown in Figure 55. The influence of deck motion is apparent when cases (b) and (d), corresponding to pitch attitudes of 0° with the bow moving up and down respectively, are examined. In case (d), flow perturbations are observed in the region where freestream was dominant in case (b), as indicated on the figure. These perturbations correspond to wake from the bow and front step that have convected downstream from the instance when these components were pitched upwards. Therefore, there is a lag between ship motion and airwake response over the deck. The frequency spectrum of velocity magnitude at a point located horizontally at the hangar and vertically at 1.4 hangar heights is shown in Figure 56, and, not surprisingly, indicates that there is an additional primary frequency in the wake associated with the ship motion frequency [78].

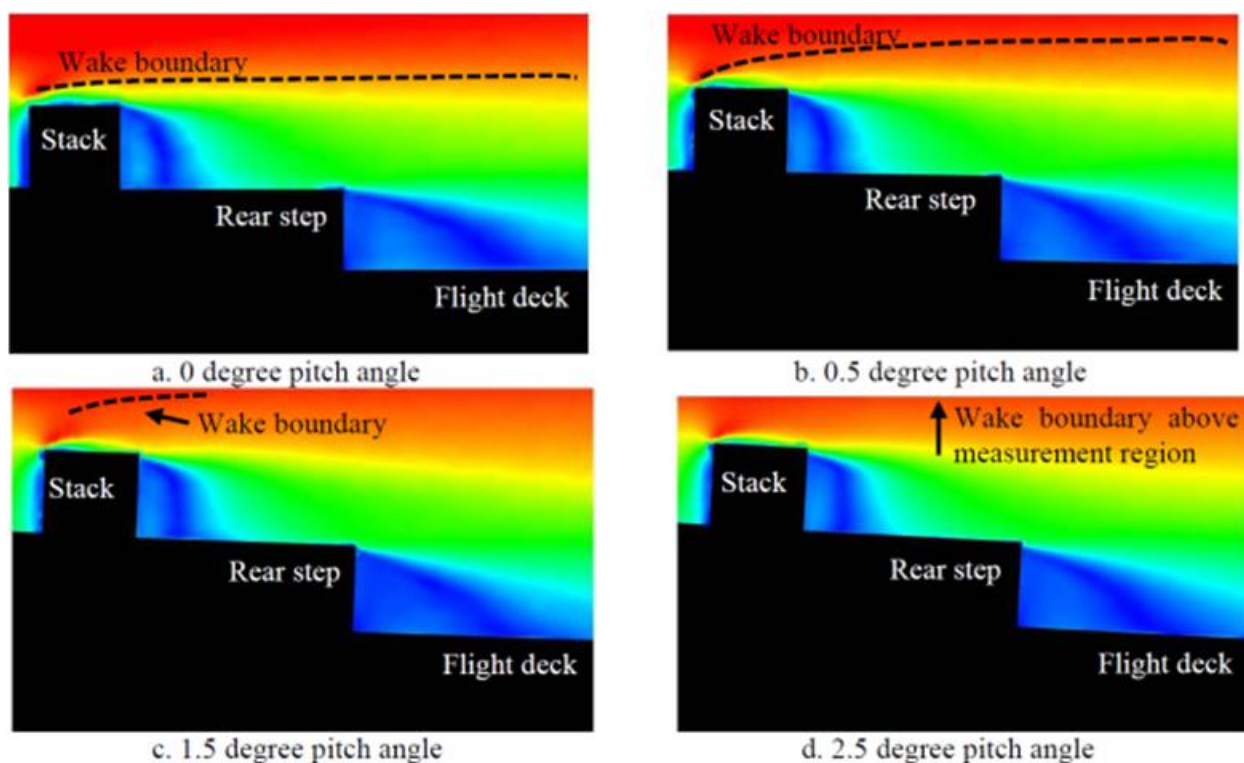


Figure 53: Effect of static pitch angle on mean velocity magnitude; red indicates high velocity and blue indicates low velocity [78]

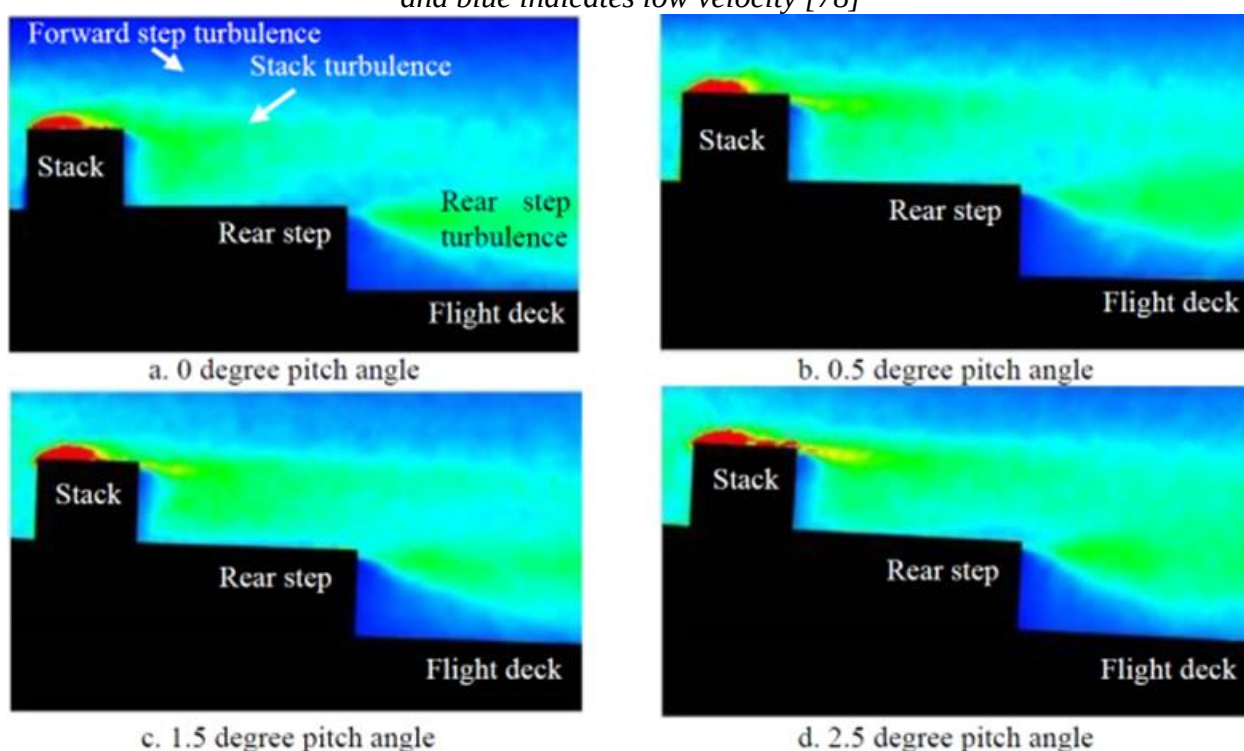


Figure 54: Effect of static pitch angle on TKE; red indicates high TKE and blue indicates low TKE [78]

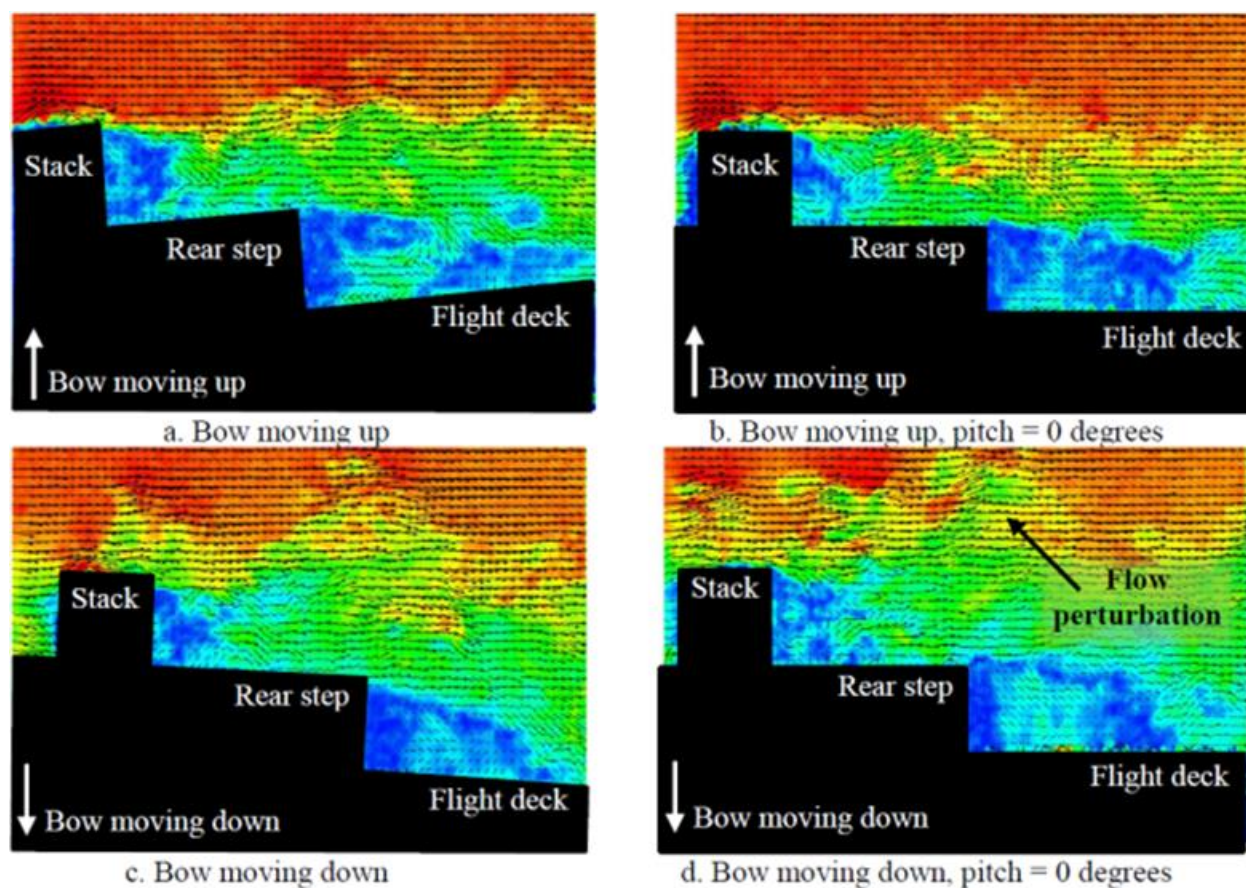


Figure 55: Instantaneous contours of velocity magnitude over one period of pitch motion [78]

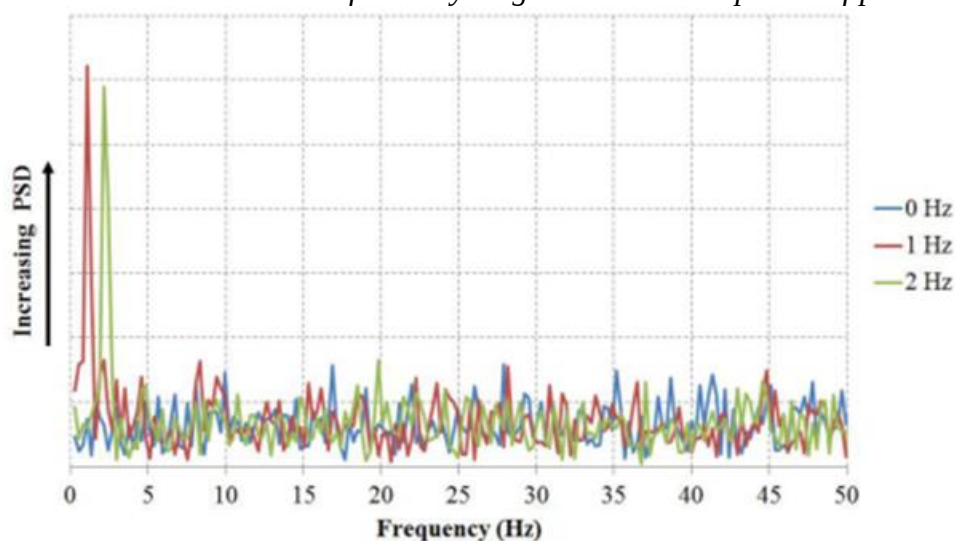


Figure 56: Spectrum of velocity magnitude at a point located horizontally at hangar and vertically at 1.4 times hangar height with ship oscillating in pitch attitude [78]

Similar observations were made by Yuan et al. [79] in experimental investigations of flow over a 1/50 scale model of a Canadian Patrol Frigate (CPF) - Figure 57 – where four probes were used to measure airwake velocities on the deck. Isolated single DOF heave, roll and pitch motions at 0.08 Hz with amplitudes of 1.5 m, 3.4° and 5°, respectively, were considered for a

headwind condition. Figure 58 shows the frequency spectra of velocity magnitude, and, not surprisingly, exhibits peaks at the ship motion frequency of 0.08 Hz heave and pitch excitation cases. The influence of roll motion on velocity magnitude is minimal, although this may not be the case for oblique WOD conditions, where deck roll attitude can cause the horizontal edges of the deck to effectively act as backward facing steps that can produce large regions of separation [82].



(a) Wind tunnel setup

(b) Probes used to measure airwake velocities

Figure 57: Model of CPF used by Yuan et al. during experimental investigations of influence of ship motion on airwake [79]

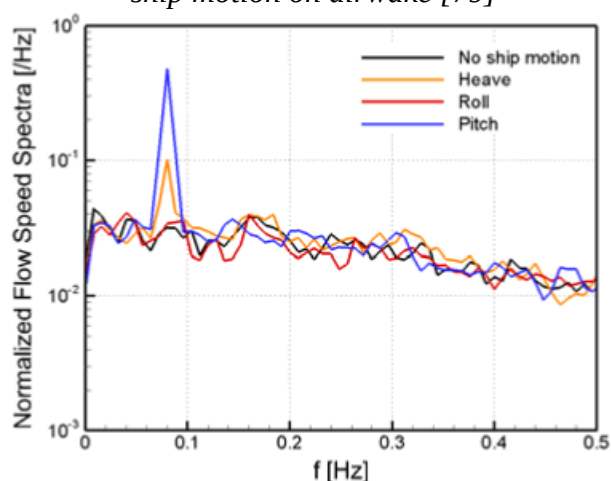


Figure 58: Spectrum of velocity magnitude measured by probe #3 from Figure 57(b) for isolated single DOF deck motion [79]

CFD investigations of the influence of ship motion using an ONR Tumblehome⁴ (ONRT) ship geometry (i.e. Figure 59) were undertaken by Dooley et al. [80]. Dooley et al. noted that in addition to inducing deck motion, waves can influence the airwake by “pumping” air onto the ship, and thus performed CFD simulations that accounted for wave motion, wave-induced ship motion, ship forward speed and wind-at-sea [80]. Sea states #3 (SS3) and #6 (SS6) were simulated using wind speeds of 15 kts and 35 kts, respectively, and four simulations are performed for each sea state:

- a) no wave motion and no ship motion included (baseline),
- b) wave motion included but no ship motion (designated as “NM/WW” in figures),

⁴ The ONRT ship represents the research version of the U.S. Navy Zumwalt class destroyer DDG-100, which was designed to have a small radar cross section.

- c) no wave motion but ship motion included (designated as “WM/NW” in figures), and
- d) both wave and ship motions included (designated as “WM/WW” in figures).

For conditions (b) and (d), the wave and ship motions determined from case (d) are superimposed individually in the simulation. The REX CFD code - a dynamic overset, unsteady Reynolds Averaged Navier-Stokes (URANS) or DES/DDES solver with unique capabilities for ship hydro/aerodynamics - was used. Combined heave and pitch motions are considered, and ship motion for one wave cycle is shown in Figure 60, where a wave phase of 0° corresponds to the time instance when the wave crest or peak is at the bow and the wave trough is at $\lambda = 0.6 L$, where λ is the wave length and $L = 154$ m is the ship length. From Figure 60, the magnitudes of pitch and heave motion for SS6 are approximately four times greater than those for SS3. The influence of SS3 motion was found to be minimal [80] and thus, this report will discuss findings from the SS6 simulations.

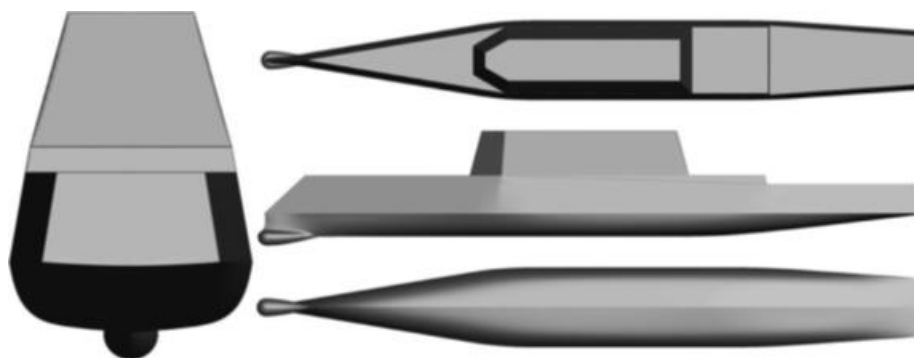


Figure 59: ONR Tumblehome ship geometry [80]

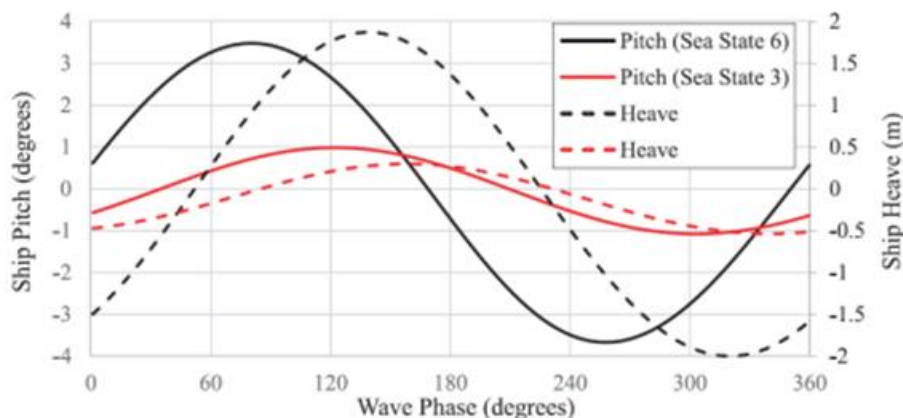


Figure 60: Ship pitch and heave motions through one wave encounter period for SS3 and SS6 [38]; 0° corresponds to the instance when the wave crest or peak is at the bow and the wave trough is at $\lambda = 0.6 L$, where λ is the wave length and $L = 154$ m is the ship model length [80]

The influence of ship motion on flow features is depicted in Figure 61, where instantaneous isosurfaces of vortical structures over a period of motion are plotted for SS6 [80]. From the figure, the “V” deck-edge vortices from the bow occur periodically during the cycle, progressively disappearing as the bow reaches motion peaks. The necklace vortex between the hull and superstructure persists during the wave cycle, with its strength decreasing as the bow pitches up and increasing as the bow pitches down. The detached structures on the top and behind the superstructure also persist through the cycle. The recirculation region aft of the superstructure expands when the bow pitches up and contracts when the bow pitches down,

indicating a translation of the deck impingement location of the shear layer shed from the superstructure top-aft-edge. Note that this shear layer translation occurs due to ship motion and represents a separate phenomenon from the shear layer flapping [39] described previously.

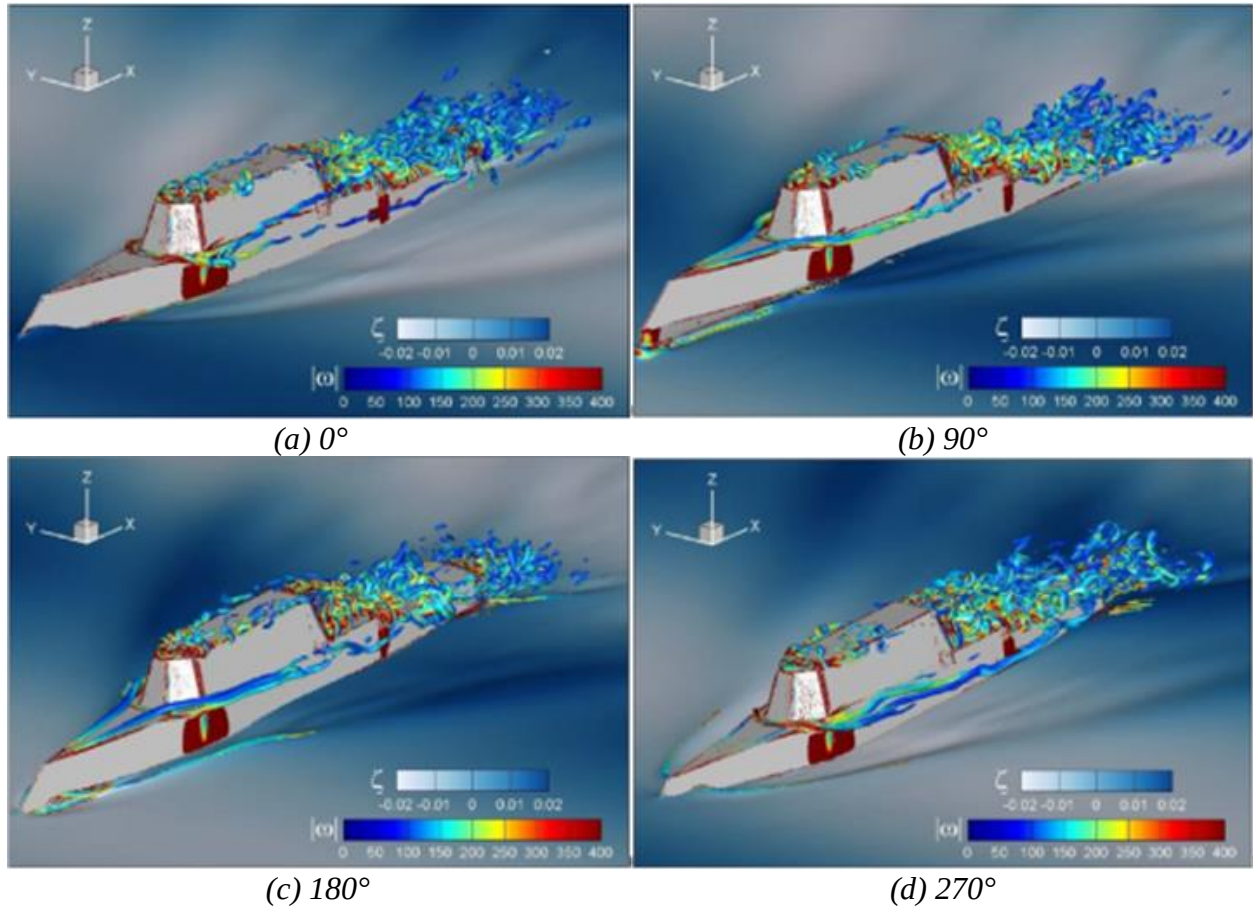


Figure 61: Isosurfaces of vortical structures for ONRT advancing at 15 kts in a 35 kts headwind (SS6) with wave and ship motion; 0° corresponds to the instance when the wave crest or peak is at the bow and the wave trough is at $\lambda = 0.6 L$, where λ is the wave length and $L = 154$ m is the ship model length [80]

Root-mean-square contours of longitudinal and vertical velocity fluctuations for SS6 are shown in Figure 62, where velocities are nondimensionalized using a speed of $U_0 = 25$ kts [80]. Ship motion increases the RMS of both velocity components aft of the superstructure and stern when compared to the baseline, most notably for the longitudinal component. The influence of ship motion is further displayed on Figure 63, where phase-averaged deviations in longitudinal Δu_T and vertical Δw_T velocities from baseline mean velocities are plotted over one wave period. Phase-averaged velocities are obtained by time-averaging velocities recorded at specific wave phase angles during the simulation. Large changes of approximately $\pm 0.25U_0$ are noted immediately aft of the superstructure in the longitudinal velocity component. Closer to center of the flight deck, where piloted operations take place, the deviations in longitudinal and vertical velocities are approximately $\pm 0.1U_0$ and $\pm 0.2U_0$, respectively. Note that the Dirichlet boundary condition used to model the ship geometry results in an upward velocity above the flight deck when the deck moves upward and conversely, generates a downward velocity when it moves downward [80].

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Phase-averaged longitudinal and vertical velocities at a point located 30 ft above the center of the flight deck are shown in Figure 64, where the wave encounter period is 8.5 s [80]. The vertical displacement of mid-flight deck location is also shown on the figure. There is a $\sim 90^\circ$ lag evident between ship motion and the longitudinal velocity response in the WM/WW and WM/NW cases in Figure 64. The existence of this lag reinforces findings from Sydney et al. [78], discussed above. Peak-to-peak amplitude of the time-averaged longitudinal velocity reaches $0.25U_0$ relative to the baseline, while that of the vertical velocity reaches $0.1U_0$ in Figure 65. The vertical velocity response is also noted to be in phase with ship motion.

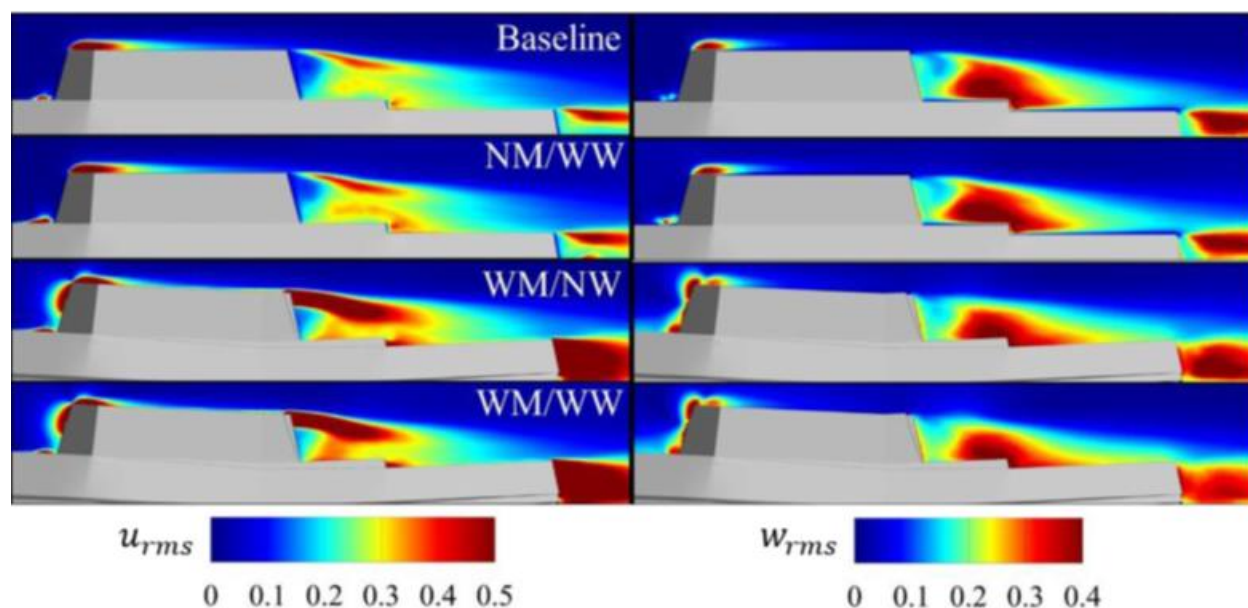


Figure 62: RMS of longitudinal (left) and vertical (right) velocity fluctuations for ONRT advancing at 15 kts in 35 kt headwind; “NM/WW” stands for “no ship motion/with wave motion”, “WM/NW” for “with ship motion/with wave motion”, “WM/WW” for “with ship motion/with wave motion”; velocities are nondimensionalized using an arbitrary speed of $U_0 = 25$ kts [80]

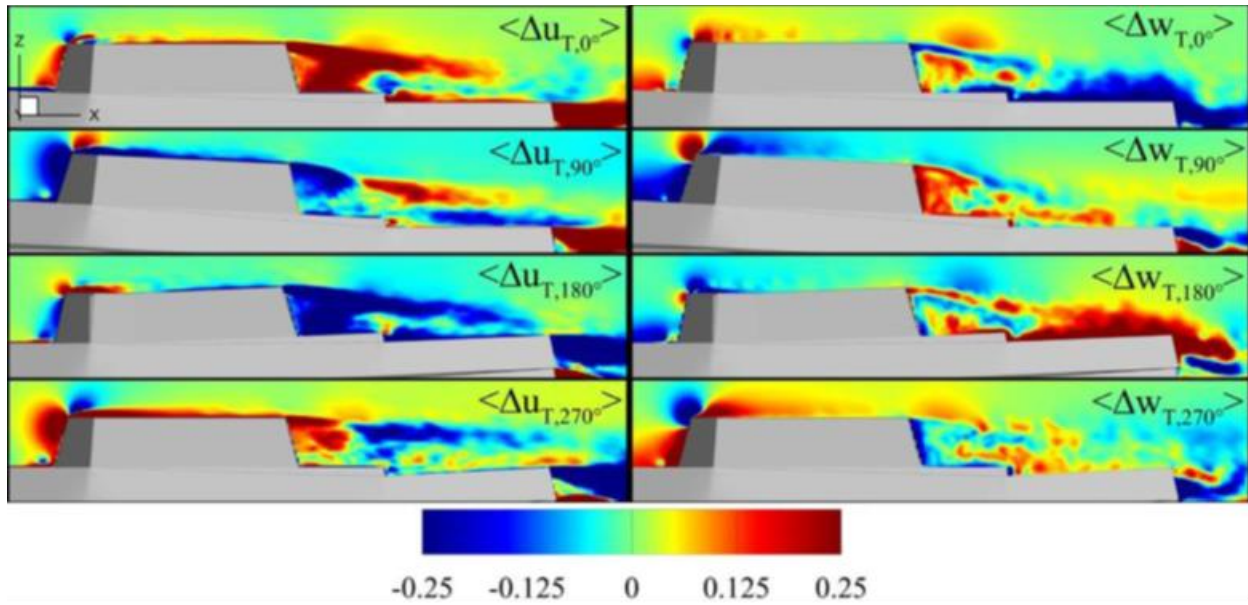


Figure 63: Phase-averaged deviations of longitudinal Δu_T (left) and vertical Δw_T (right) velocities from baseline over one wave cycle period; 0° corresponds to the instance when the wave crest or peak is at the bow and the wave trough is at $\lambda = 0.6 L$, where λ is the wave length and $L = 154$ m is the ship model length; velocities are nondimensionalized using an arbitrary speed of $U_0 = 25$ kts [80]

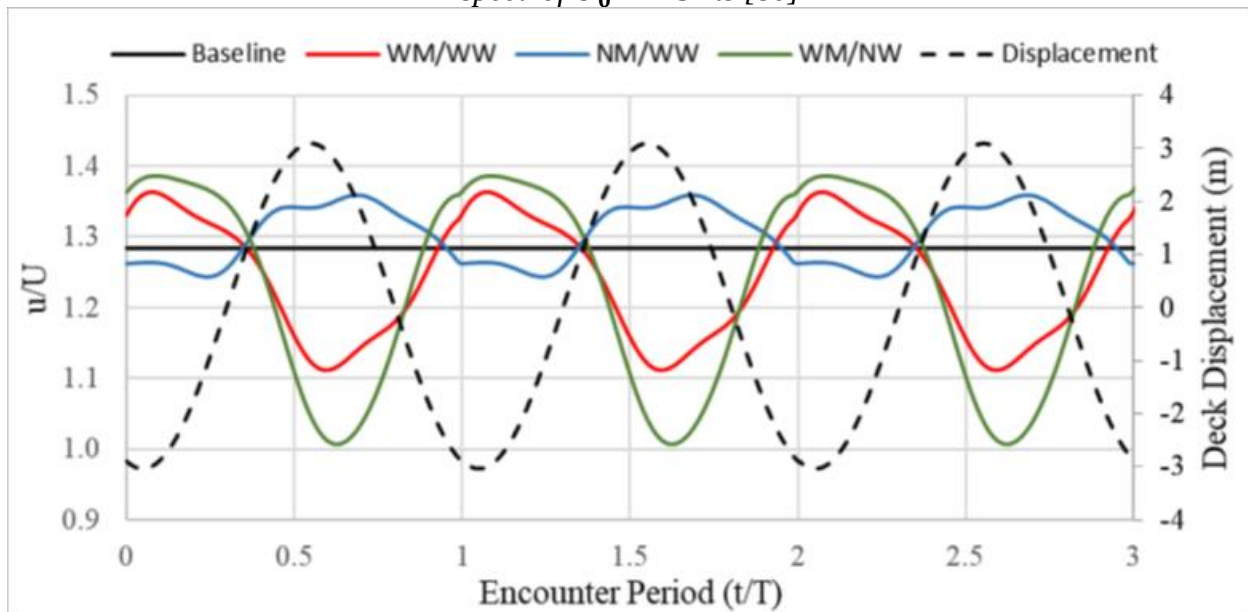


Figure 64: Phase-averaged longitudinal velocity at a point 30 ft above the center of the flight deck of ONRT advancing at 15 kts with a 35 kt headwind (SS6); wave encounter period is 8.5 s [80]

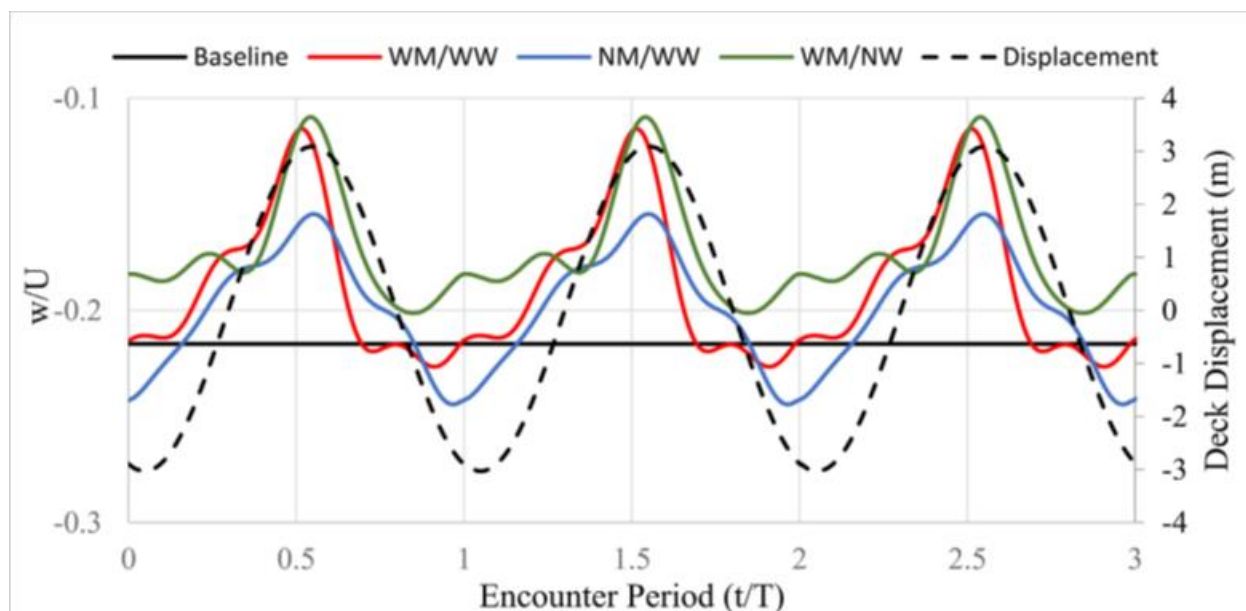


Figure 65: Phase-averaged vertical velocity at a point 30 ft above the center of the flight deck of the ONRT advancing at 15 kts with a 35 kt headwind (SS6); wave encounter period is 8.5 s[80]

Dooley et al. also investigate the influence of ship motion on helicopter dynamics using one-way and two-way coupled simulations [80]. One-way coupled simulations are performed using 3 scaled versions of a simplified SH-60 Seahawk rotor: a full-scale rotor with $D = 16.35$ m, a 50% scaled down rotor representative of a Robinson R22 rotor, and a 4.5% scaled down rotor representative of a drone. Simulations are performed with the rotor hovering 30 ft above the center of the flight deck. Time histories of rotor thrust are shown in Figure 66. All three rotors exhibit a degree of periodicity. The $0.045D$ rotor is noted to be most sensitive to the airwake, producing fluctuations with the largest magnitudes in Figure 66. In the context of the present ONR effort, this finding indicates the size of the rotor is an important factor to take into consideration when identifying airwake features that influence vehicle dynamics. The corresponding thrust frequency spectra for the three rotors are shown in Figure 67, where f_e is the wave encounter frequency. Thrust fluctuations for all three rotors are periodic with the wave encounter frequency f_e . Peaks are also evident at $2f_e$.

Two-way coupled simulations were performed using a generic helicopter loosely modeled after the SH-60 [80]. The helicopter model had a forward speed of 15 kts to hold hover 30 ft above the center of moving ship deck (SS6). Trim was not performed, and the aircraft was held stationary above the deck using constant main and tail rotor collective inputs only. Time histories of the vertical force generated by the helicopter are shown in Figure 68 for three cases: isolated helicopter, helicopter hovering above advancing ship with no motions, and helicopter hovering above advancing ship deck with motion. The black line indicates instantaneous values while the red line represents a moving-average over one main rotor rotation. Ship motion causes the vertical force to change in a sinusoidal manner with peak changes of approximately 5 kN, relative to the 85 kN mean baseline. Note that these changes are a combined result of periodic ship airwake and dynamic ground effect from the moving deck, where part of the helicopter rotor downwash is redirected back towards to the rotor. Power spectral density plots of the vertical force shown in Figure 69 reveal periodicity with the wave encounter frequency f_e . Note that the

CFD results presented by Dooley et al. [80] were not validated against experiments or full-scale test data.

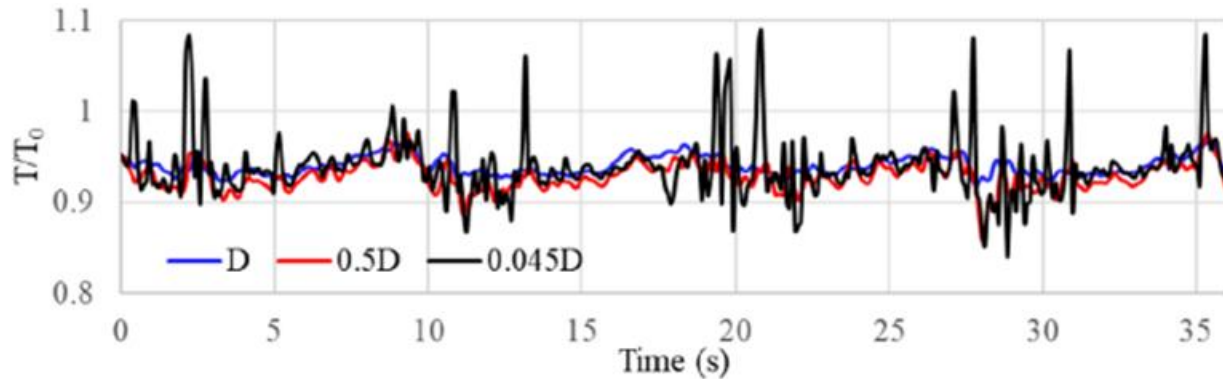


Figure 66: Instantaneous thrust produced by three scaled versions of a simplified SH-60 helicopter rotor model located 30 ft above the center of the flight deck of the ONRT ship advancing at 15 kts with a 35 kt headwind (SS6) during one-way coupled simulations [80]

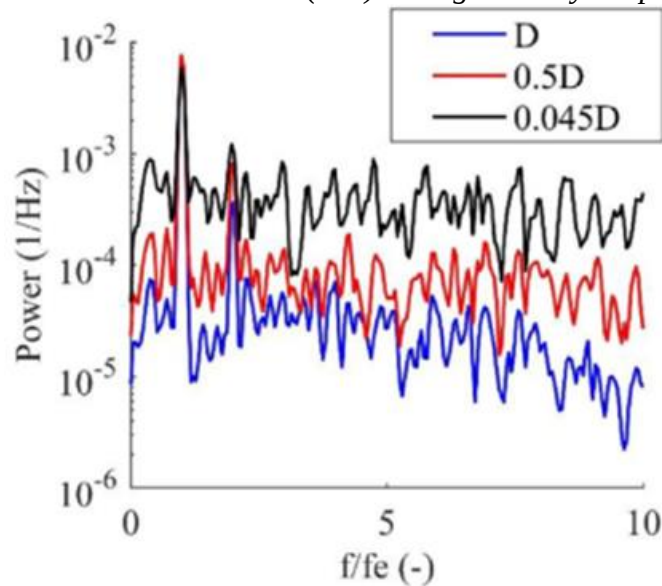


Figure 67: Frequency response function corresponding to thrust time-histories in Figure 66; f_e is the wave encounter frequency [80]

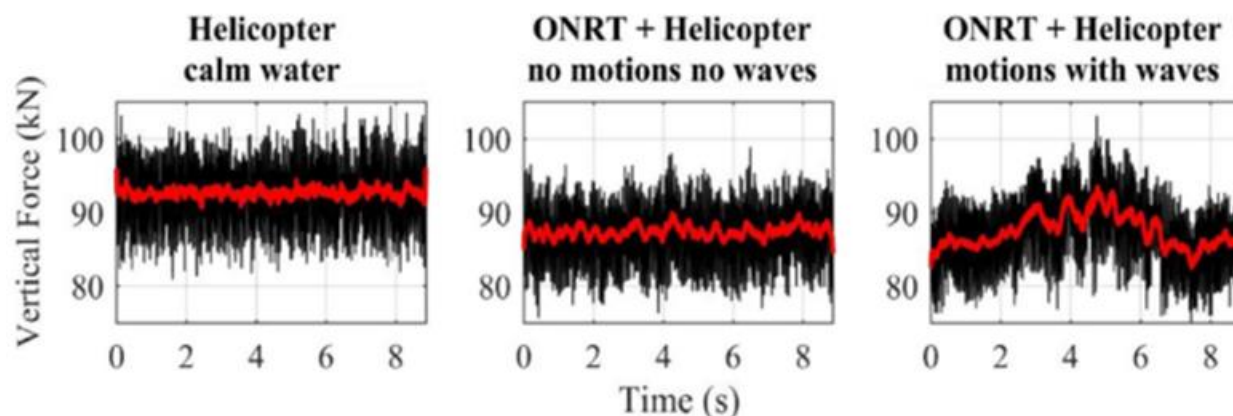


Figure 68: Time-histories of vertical force generated by a simplified SH-60 helicopter model located 30 ft above the center of the flight deck of the ONRT ship advancing at 15 kts with a 35 kt headwind (SS6) during two-way coupled simulations; black line indicates instantaneous values and red line represents a moving-average over one main rotor rotation [80]

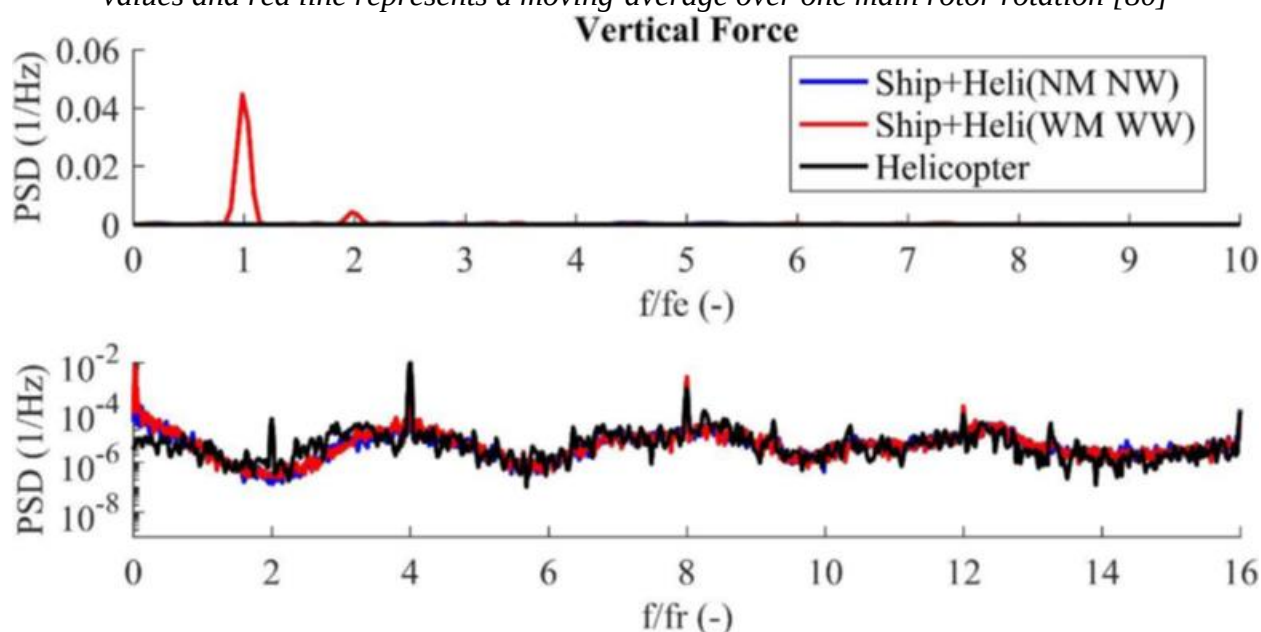


Figure 69: Frequency response functions corresponding to vertical force time-histories in Figure 68; f_e is the wave encounter frequency, and f_r is the main rotor rotational frequency [80]

Experimental investigations of the influence of ship motion on ship airwake were performed by Krebill [81] using a 1/77 scale model of the ONRT ship geometry in a water channel as shown in Figure 70. Heave and pitch motions were considered. These motions were applied at the CG of the ship model. PIV measurements were made for both static and dynamic ship configurations along the planes shown in Figure 71. Laser Doppler velocimetry (LDV) measurements at the point indicated in Figure 71 within PIV domain was used to verify the PIV measurements. Sea states #3 to #6 were investigated using different combinations of motion amplitude, frequency, and periods as indicated in Mean longitudinal velocity contours at different phase angles of ship motion are shown in Figure 74 [81], and illustrate that the shear layer impingement location translates up and down in the $0.77 \leq x/L \leq 0.85$ region of the deck

during the motion. Mean vertical velocity contours at different phase angles of ship motion are shown in Figure 75 [81]. From the figure, the shear layer shed from the superstructure top-aft-edge juts into the recirculation region for phase angles 45° and 90° . The shear layer then starts receding to its original location from 135° onwards. Also evident from the figure is existence two distinct downwash regions separated by a smaller magnitude downwash region in the 90° and 135° phase contour plots, which are a consequence of the ship model changing directions.

Similar to the previous studies discussed in this section, Krebill observed airwake velocities at a position $(x/L, z/L) = (0.888, 0.110)$ (see Figure 71) to be periodic with wave encounter frequency f_e for all sea states considered as shown in Figure 76 [81]. For SS5 and SS6, a secondary peak is evident at $2f_e$, which the author attributes the secondary peak to the measurement location going in and out of the two vertical velocity downwash regions shown in Figure 75 for phase angles of 90° and 135° . Krebill also examines mean velocities measured for static ship orientations corresponding to different phases of ship motion and notes larger velocity fluctuations in the static measurements, implying static tests provide conservative estimates of the influence of ship motion [81].

Table 1: Test matrix used to emulate sea states #3 - #6 in Ref. [81]

Sea-State		SS3	SS4	SS5	SS6
Motion Frequency (Hz)		0.33	0.28	0.26	0.17
Motion Period (s)		3.00	3.53	3.78	5.93
Heave	Amplitude (m)	0.00527	0.0082	0.0153	0.025
	Offset from mean (m)	-0.00141	-0.00141	-0.00139	-0.00147
Pitch	Amplitude (m)	1.07	1.50	2.49	3.58
	Offset from mean (deg)	-0.0734	-0.0783	-0.0947	-0.141
	Lead (deg)	37.2	43.3	53.3	59.0

. From the mean longitudinal and vertical velocity contours for the four sea states shown in Figure 72 and Figure 73, respectively, flow features remain similar between the sea states [81]. In this report, results from SS5 investigations will be considered.

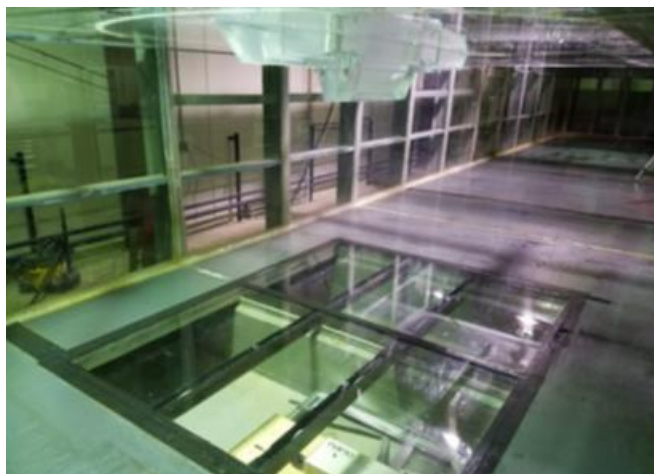


Figure 70: ONRT model in flume channel [81]

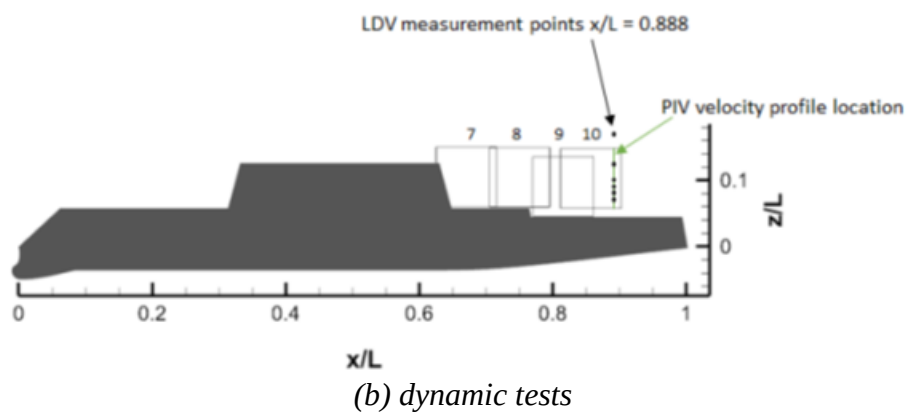
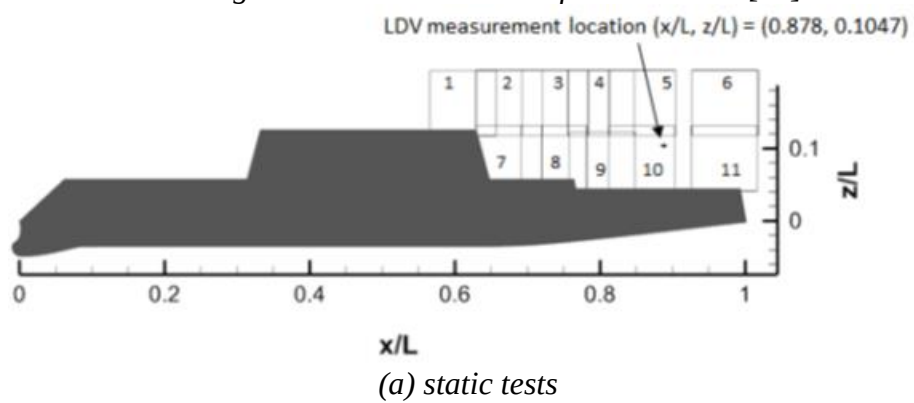


Figure 71: PIV and LDV measurement locations [81]

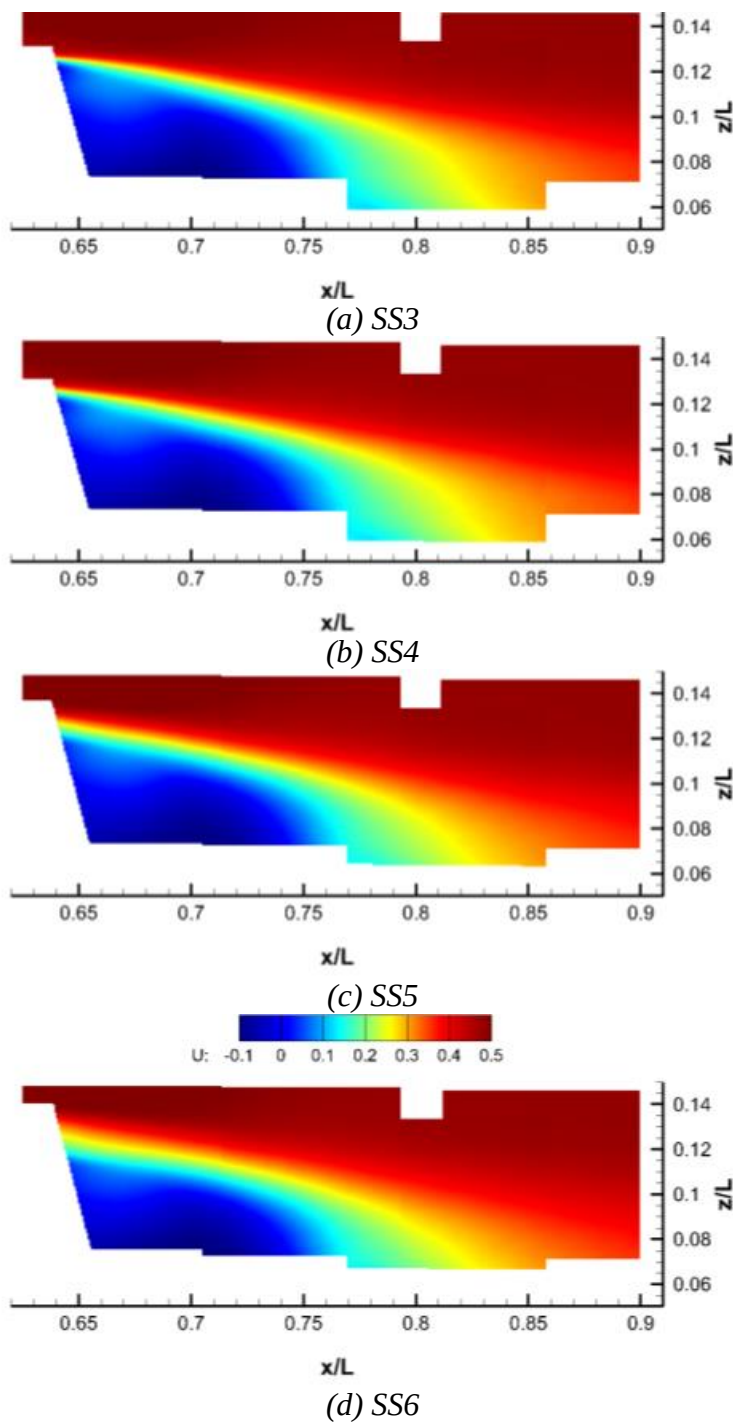


Figure 72: Mean longitudinal velocity contours recorded during water channel experiments for SS3 to SS6; velocity values are given in meters-per-second [81]

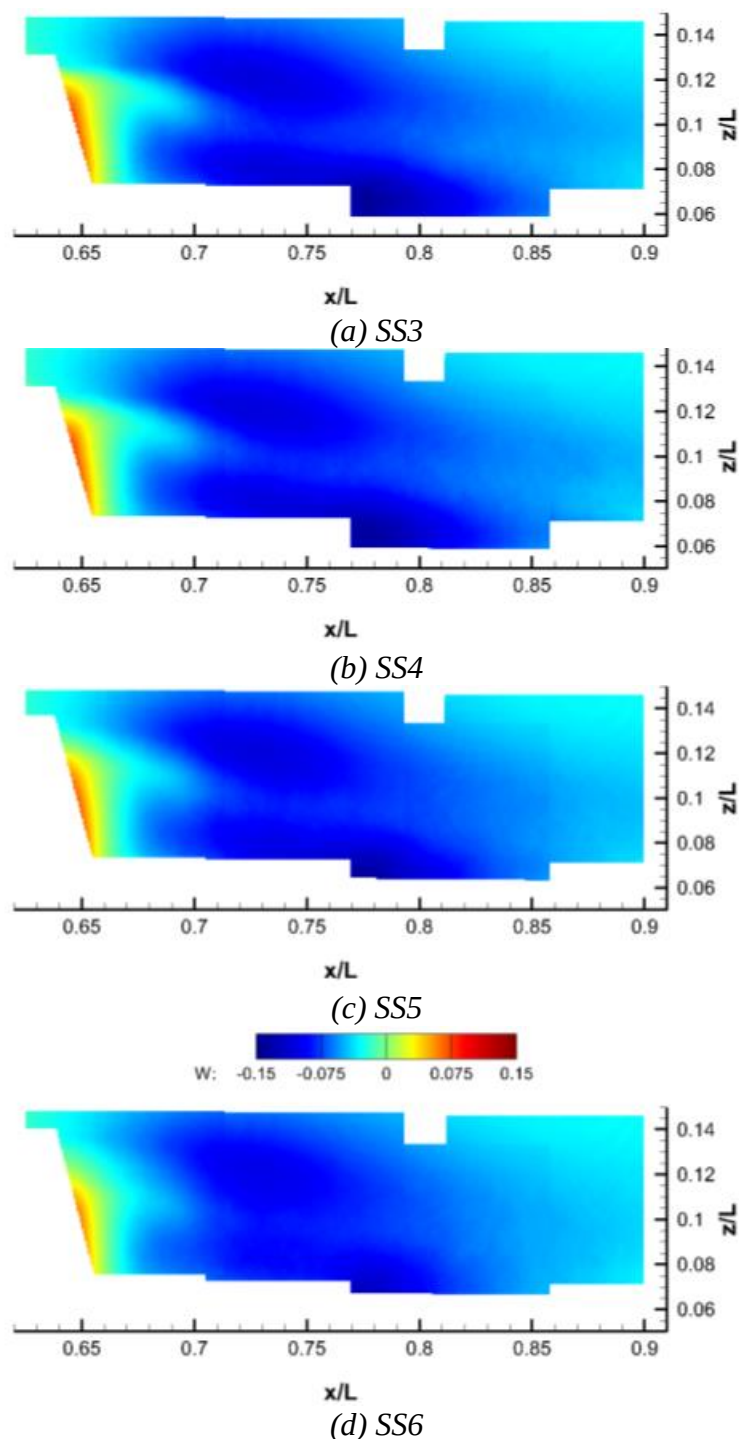


Figure 73: Mean vertical velocity contours recorded during water channel experiments for SS3 to SS6; velocity values are given in meters-per-second [81]

Mean longitudinal velocity contours at different phase angles⁵ of ship motion are shown in Figure 74 [81], and illustrate that the shear layer impingement location translates up and down in

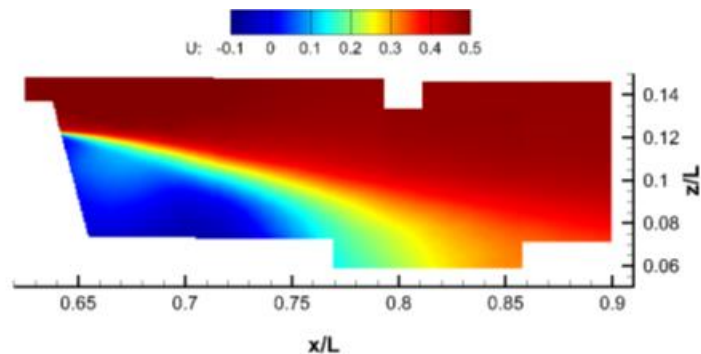
⁵ Note that a definition of ship motion in terms of phase angle is not provided by the author. However, based on the translation of the shear layer impingement location in Figure 74, it is probable that motion is defined such that the

the $0.77 \leq x/L \leq 0.85$ region of the deck during the motion. Mean vertical velocity contours at different phase angles of ship motion are shown in Figure 75 [81]. From the figure, the shear layer shed from the superstructure top-aft-edge juts into the recirculation region for phase angles 45° and 90° . The shear layer then starts receding to its original location from 135° onwards. Also evident from the figure is existence two distinct downwash regions separated by a smaller magnitude downwash region in the 90° and 135° phase contour plots, which are a consequence of the ship model changing directions.

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	Offset from mean (deg)	-0.0734	-0.0783	-0.0947	-0.141
	Lead (deg)	37.2	43.3	53.3	59.0

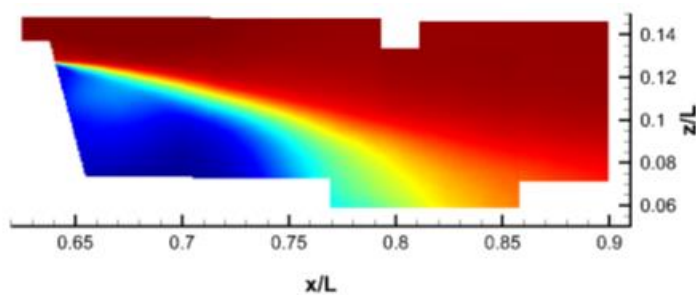


ship bow pitches downward and the deck upwards between 0° and 180° , and in the opposite directions between 180° and 360° , with the switch in directions occurring at 90° and 270° .

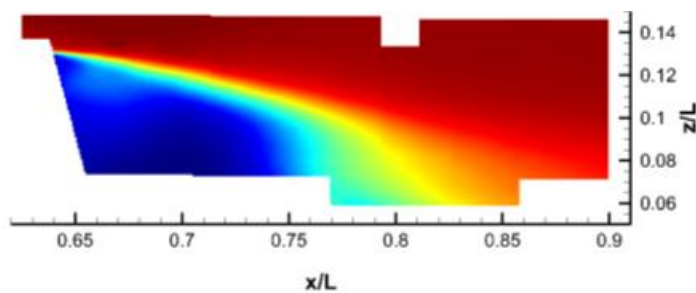
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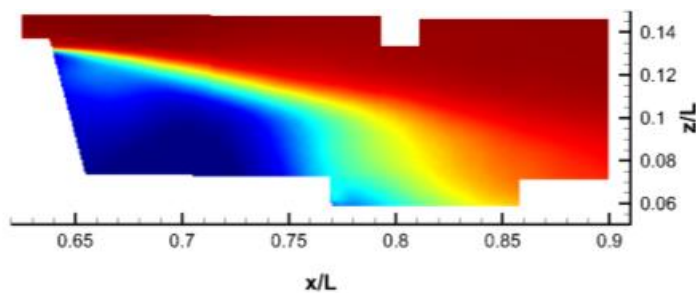
(a) 0°



(b) 45°

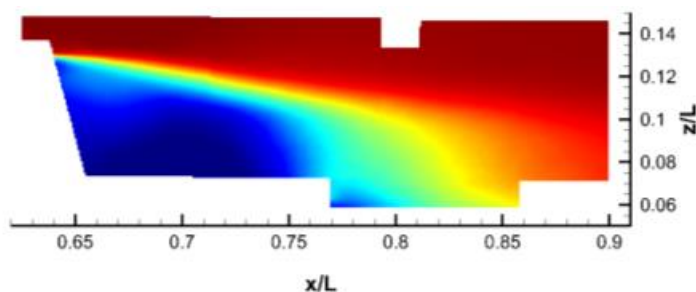


(c) 90°



(d) 135°

Figure 74: Mean longitudinal velocity contours for different phase angles of ship motion; velocity values are given in meters-per-second [81]



(e) 180°

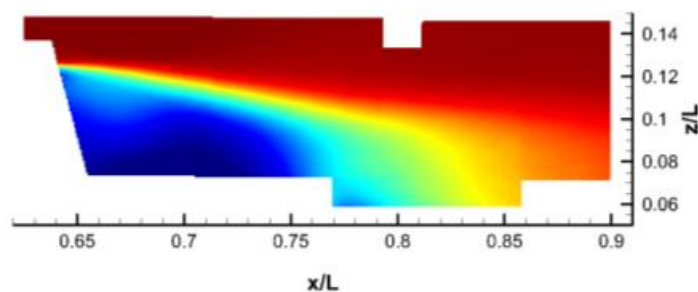
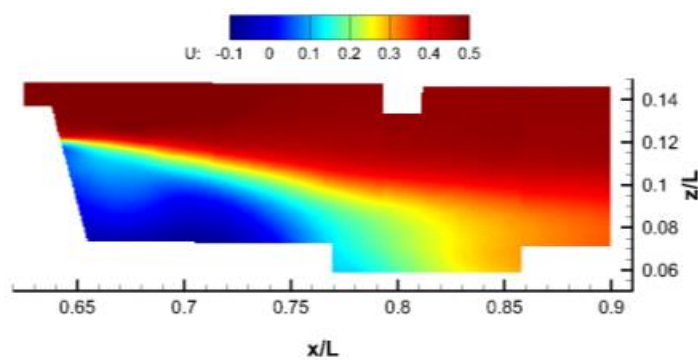
(f) 225° (g) 270°

Figure 74: Mean longitudinal velocity contours for different phase angles of ship motion; velocity values are given in meters-per-second [81]

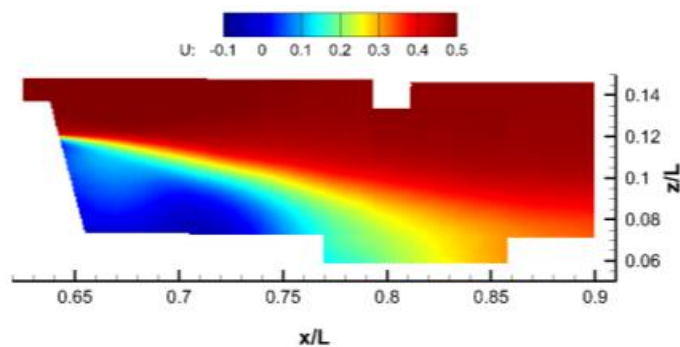
(h) 315°

Figure 74: Mean longitudinal velocity contours for different phase angles of ship motion; velocity values are given in meters-per-second [81]

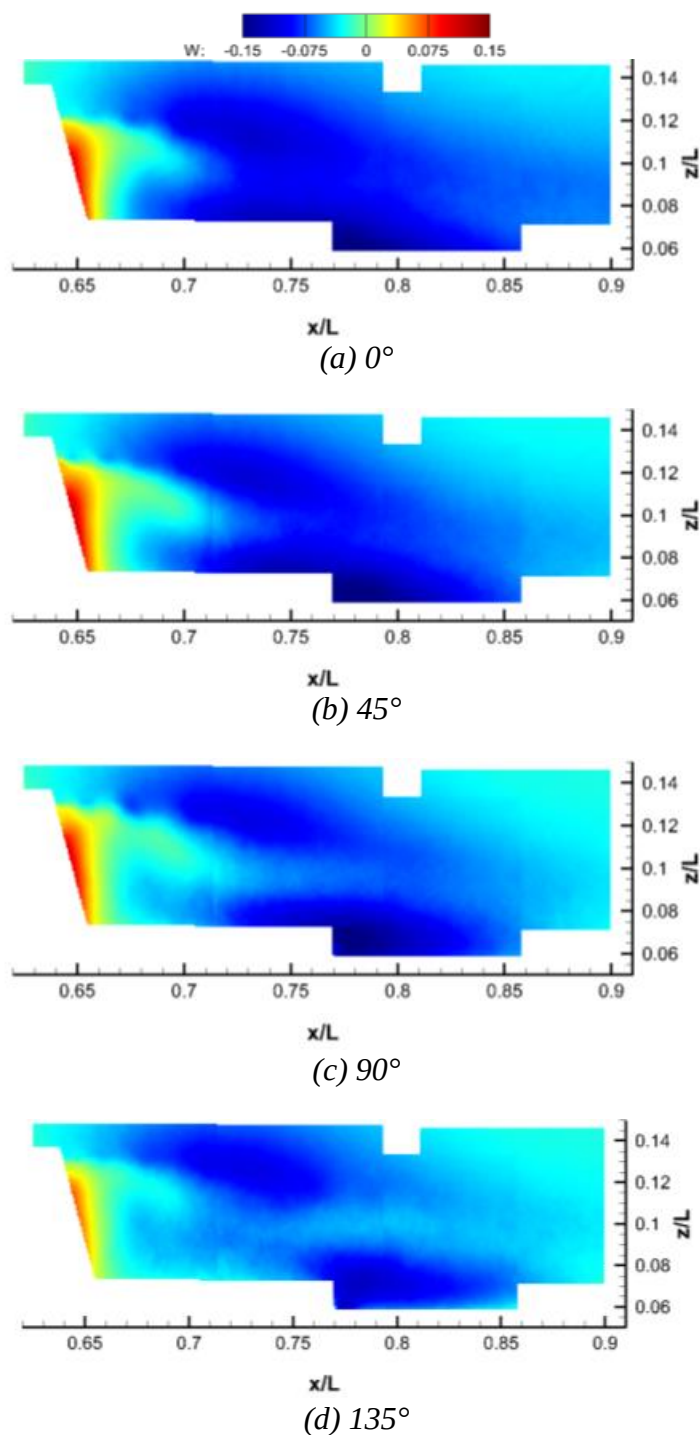
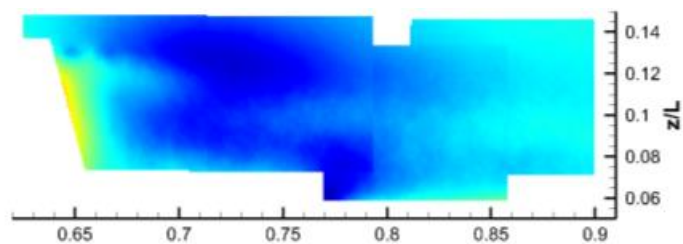
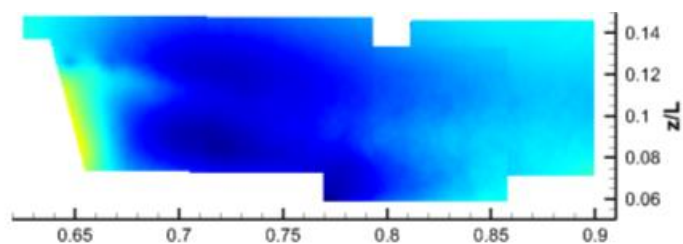


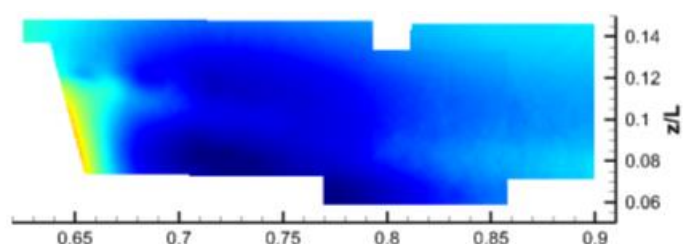
Figure 75: Mean vertical velocity contours for different phase angles of ship motion; velocity values are given in meters-per-second [81]



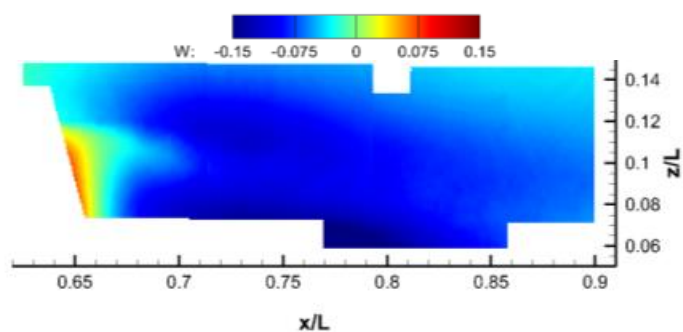
(e) 180°



(f) 225°



(g) 270°



(h) 315°

Figure 75: Mean vertical velocity contours for different phase angles of ship motion; velocity values are given in meters-per-second [81]

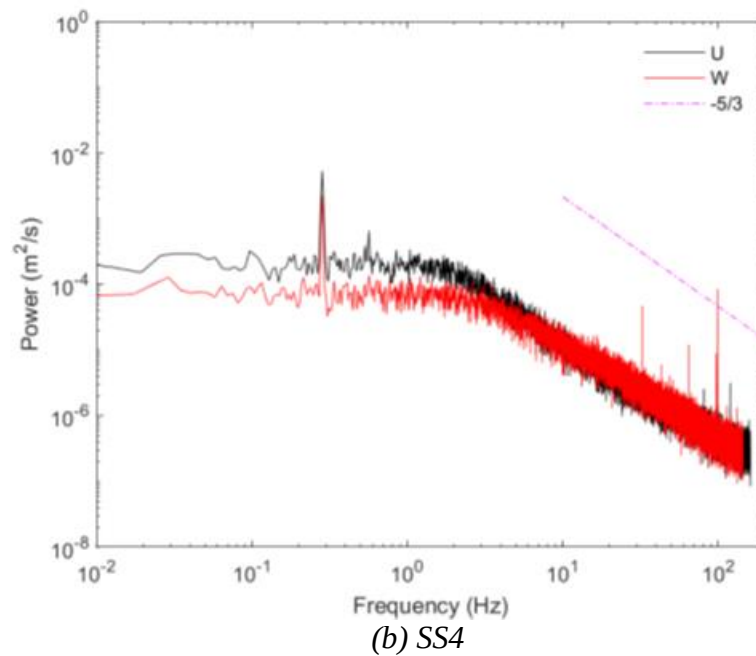
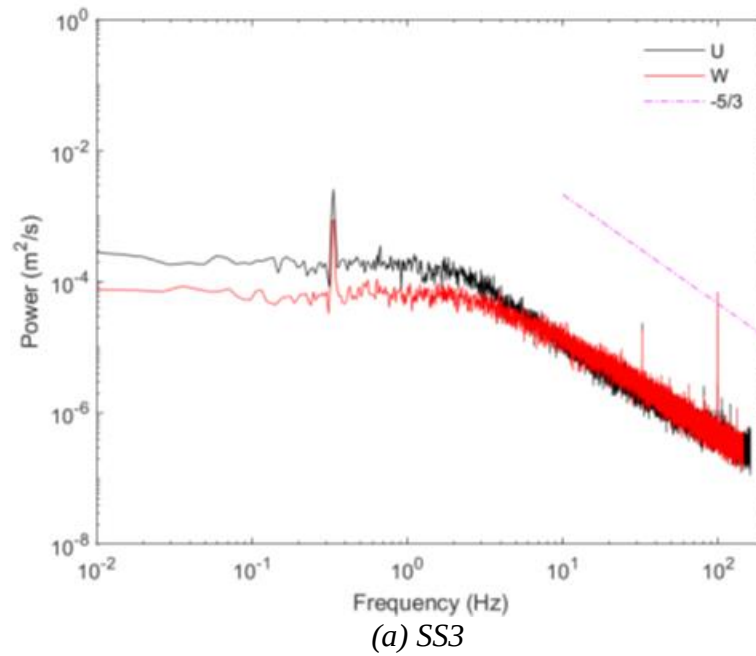


Figure 76: Frequency spectra of longitudinal and vertical velocities recorded at $(x/L, z/L) = (0.888, 0.110)$ (see Figure 71) during water channel experiments [81]

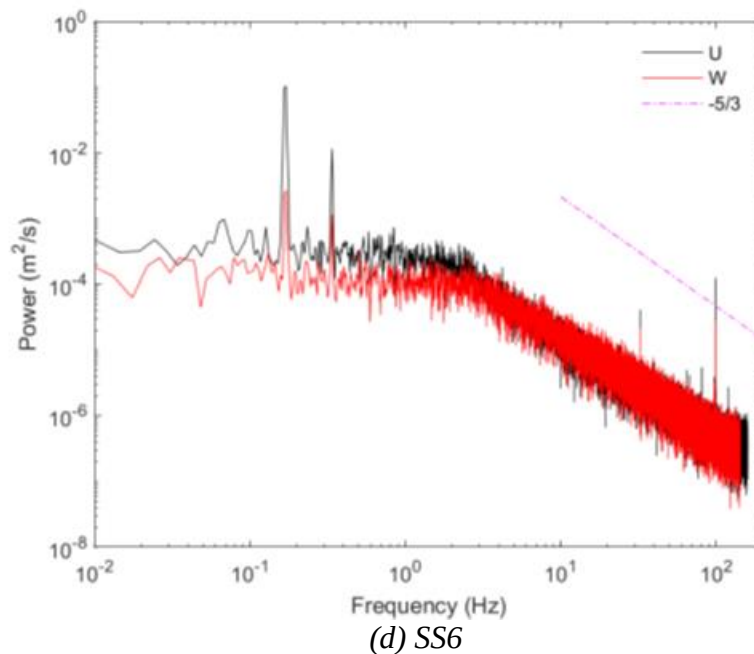
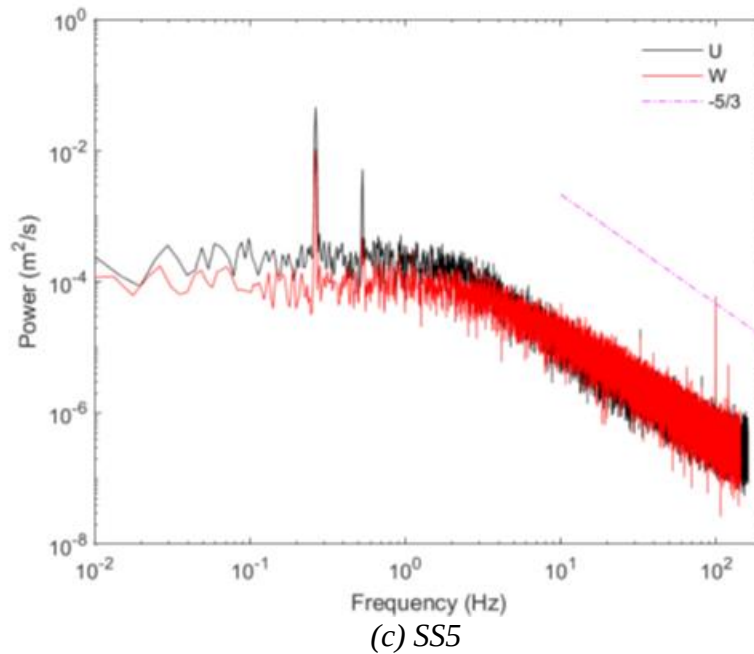


Figure 76: Frequency spectra of longitudinal and vertical velocities recorded at $(x/L, z/L) = (0.888, 0.110)$ (see Figure 71) during water channel experiments [81]

Wake Parametrization

Work has been previously performed at CDI to parametrize fluctuating velocity components in the airwake of a landing helicopter assault (LHA) class ship to allow for predictions of flow unsteadiness to circumvent computational and data storage limitations associated with CFD based solutions [83]. The approach used a stochastic representation of the unsteady velocity

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components and a data reconstruction algorithm that preserved the spatial correlations relationships that resulted due to vortex structures in the ship airwake. Realistic velocity fluctuation predictions were obtained from the resulting model when compared to those obtained via CFD. The model however, was dependent on previously obtained CFD data and thus dependent on the configuration under consideration. A configuration independent methodology was proposed in an early publication by Woofield et al. [84], where flow over the edges and faces of a ship structure were first characterized as one of three fundamental flow patterns depending on the incoming wind direction: corner flow (further categorized as separated or vortex corner flow), lee rotor flow and profile drag flow, illustrated in Figure 77. Each flow pattern was associated with a dataset of velocity increments which could be accessed for a given edge/surface and wind-over-deck angle. A simplified standard deviation-based turbulence model was also introduced but its influence during piloted simulation could not be assessed due to unforeseen simulation issues. The generic airwake model showed good agreement with wind tunnel velocity data in the transverse plane but discrepancies were noted in the streamwise plane. Despite the lack of turbulence, the authors note that favorable responses were obtained from pilots during helicopter-shipboard simulations with the generic model and emphasize the need for further testing of the methodology with more representative turbulence models. Generic airwake models have also been derived for flow over urban settings (e.g. Ref. [85]).

CFD Based Airwake Study

The literature review described above allows us to identify flow features that are of interest to the ship-helicopter DI scenario:

- a) shear layers shed from edges of bluff body obstacles,
- b) deck-edge vortices that develop due to pressure gradients between side and top surfaces, and
- c) corner vortices that originate in oblique WOD conditions.

Characterization of these flow features in terms of Strouhal number, and parameters such as turbulence intensity and TKE were provided above where available in the literature.

To supplement these findings, CFD simulations were undertaken of the flow around a cube in order to develop a dataset against which to compare analytical models. Flow over cube was chosen as it represents a minimum complexity canonical obstacle, and simulation results were compared to those from literature for validation. CFD predictions were performed using the CDI CGE/VorTran-M flow solver [12, 86, 87]. CDI's adaptive cut-cell octree Cartesian Grid solver (CGE) solves the compressible URANS equations using Spalart-Allmaras (SA and SA DES) or shear stress transport (SST) turbulence closures. Modern turbulence specific adaptive wall functions are available in CGE in an overset arrangement to efficiently represent the flow in boundary layers where appropriate. Resolving the evolution of vortical structures, such as those shed from bluff bodies, can be particularly expensive in primitive variable CFD solvers; therefore, CGE is coupled to the VorTran-M vorticity-velocity solver in order to reduce computational costs. The CGE/VorTran-M coupling arrangement consists of implementing the CGE solution on the grid as an "inner" solver that extends sufficiently far to contain regions with significant compressibility or flow turbulence and VorTran-M as an "outer" solver that is applied in the essentially incompressible flow region and efficiently transports and evolves the vorticity with minimal dissipation. Such an arrangement allows for computational savings in mesh resolution of several orders of magnitude when compared to pure CGE simulations.

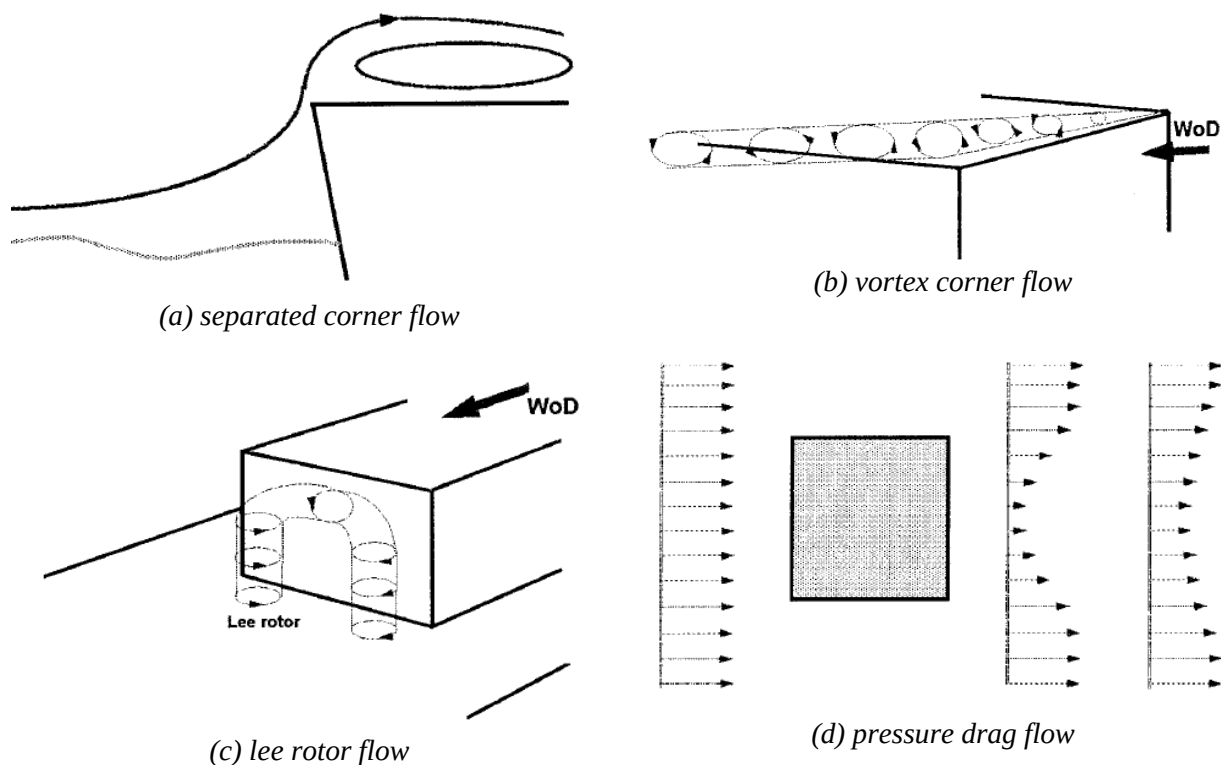


Figure 77: Flow patterns used to parametrize airwake in Ref. [84].

Primary flow features associated with a headwind flow over a cube are illustrated in Figure 78 (a duplicate of Figure 8, but included here for convenience), and include

- A horseshoe vortex that forms upstream of the obstacle where the flow stagnates and wraps around the cube (feature “A” on the figure);
- On the top and side surfaces, local separation and reattachment regions (“B” and “C” on the figure);
- In the lee of the obstacle, an arch vortex (feature “D” on the figure) forms due to the separation of the shear layers from the top and side surfaces. The streamlines aft of the obstacle indicate a recirculation region, where the direction of the flow reverses.

CGE/VorTran-M simulations were performed for a 30 m/s headwind simulation over a 10 m cube. Although this speed is larger than would be typically encountered in practice, it is necessary to use a larger freestream velocity to maintain good numerical conditioning of the compressible flow solver. An alternative approach would have been to take advantage of the preconditioners implemented in CGE for low-speed flows, however, we deemed using the higher velocity appropriate given that the flow field is dominated by separation from the sharp edges of the cube, which has a weak dependence on viscosity. This further allowed us to perform simulations for inviscid flow only as in [12] since neglecting viscosity effects allows for scaling of time-accurate flow predictions using freestream velocity. Note that no explicit atmospheric boundary layer (ABL) model was included in the simulation, though this option is available. Simulations were performed using a fixed time step of 0.005 seconds, and, after washing out the initial conditions, a transient simulation of 25 seconds was undertaken. Results were post-

processed based on the last 22 seconds of the simulations. The first three seconds of the prediction were removed in case there were any residual non-physical transient effects.

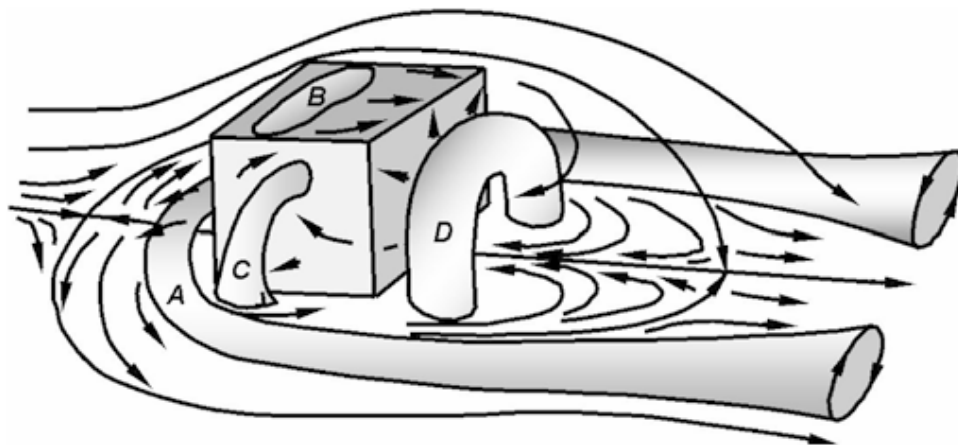


Figure 78: Schematic of principal flow features around surface-mounted obstacle [50].

Vortex shedding frequency is characterized by the Strouhal number, which is defined in Eq. (3). Results from literature indicate dominant periodicity for flow aft of a cube at a distance of $6H$ [52] to have a characteristic frequency of $St = 0.104$ [52, 88] (compared to $St = 0.2$ for a flat plate). Using the 10 m cube length as characteristic length and the freestream velocity of 30 m/s as characteristic velocity, this Strouhal number corresponds to a vortex shedding frequency $f = 0.3$ Hz, which, in turn corresponds to a period of 3.2 seconds. The trimmed 22 second simulation duration thus allows for approximately 7 cycles of vortex shedding to be captured, with approximately 640 time steps per cycle.

The qualitative flow features described previously are captured by the CGE/VorTran-M simulations as shown in Figure 79 and Figure 80. The origin of the global coordinate system shown in the figures is located at the center of the bottom face of the cube. In Figure 79(a), the separation region over the top of the cube surface and recirculation zone aft of the obstacle are evident. The velocities in the recirculation zone reach magnitudes close to 15 m/s, which is half of the freestream velocity. Figure 79(b) displays the horseshoe vortex which starts upstream of the obstacle. Longitudinal velocity contour plots along a transverse plane that passes through the center of the obstacle at $t = 4.7$ s and $t = 6.7$ s are shown in Figure 79(c) and 2(d), respectively. Flow separation on the sides of the obstacles is evident in these figures. An alternating vortex shedding pattern representative of the classic von Karman streak is noted as well.

Spectral analysis of longitudinal velocity and velocity magnitude were performed for three points aft of the cube to obtain a quantitative validation of the simulation. These points correspond to $z = \{H, 2H, 3H\}^T$ and $y = 0.5H$, where $H = 10$ m is the cube height. Velocity magnitude was considered to account for 3D flow effects in characterizing vortex shedding frequency. Time histories and Fourier Transform plots for longitudinal velocity and velocity magnitude are shown in Figure 81 and Figure 82, respectively. The dominant frequency in all spectral plots is 0.5 Hz. Secondary peaks are noted at 0.4 Hz in the $z = 2H$ and $z = 3H$ plots. The actual frequency is thus expected to be between 0.4 and 0.5 Hz. Using the cube length of 10 m as characteristic length, and the freestream velocity of 30 m/s as characteristic velocity, this corresponds to characteristic frequency of approximately 0.15. There is thus a minor discrepancy between the Strouhal frequency obtained from the CGE/VorTran-M simulation and

that from the literature, but is deemed acceptable for the current investigation which is focused on phenomenology and developing simplified representations.

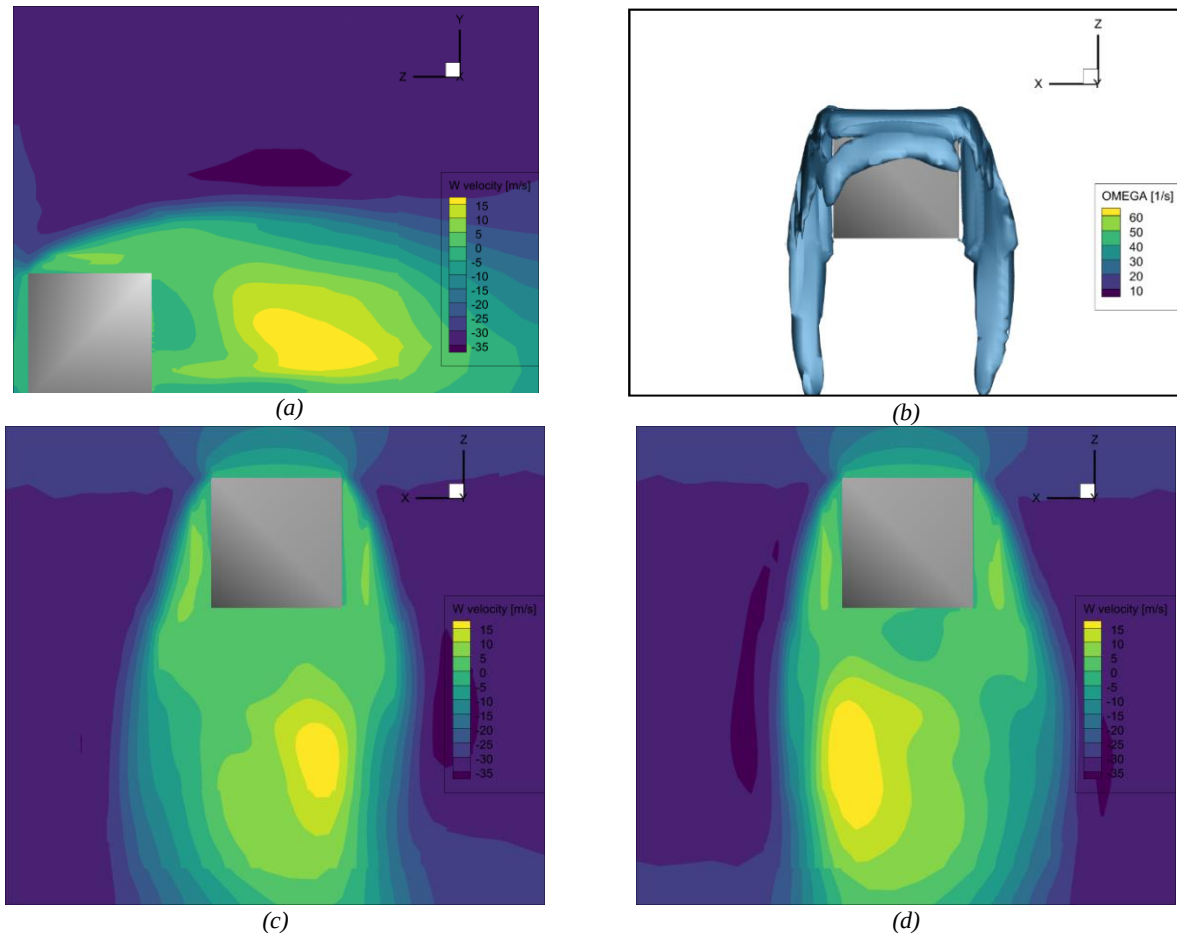
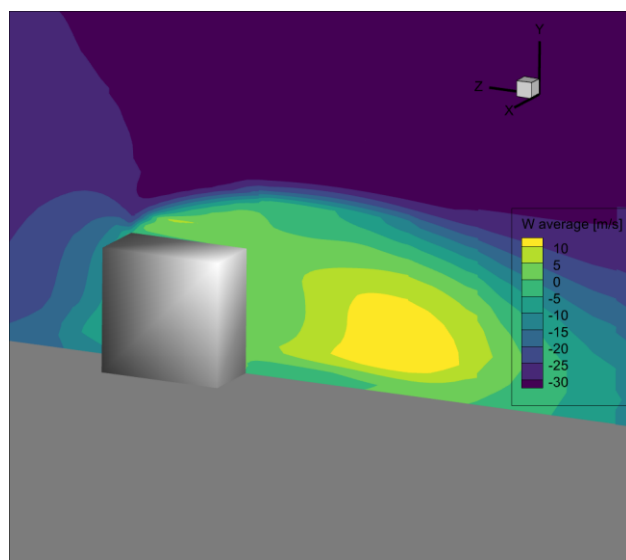
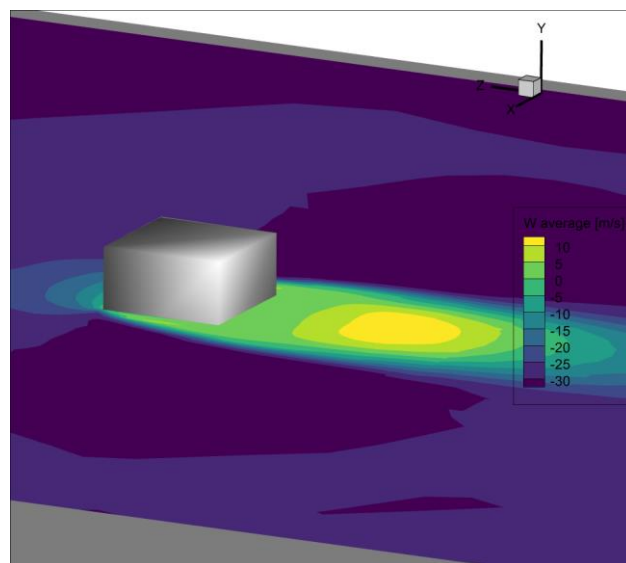


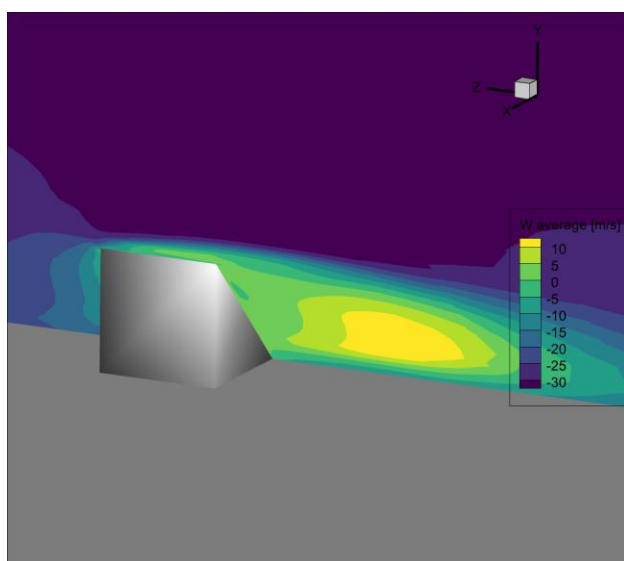
Figure 79: Flow features associated with a 30 m/s headwind flow simulation over a 10m cube using CGE/VorTran-M: (a) contour of longitudinal velocity through midplane (side view), (b) iso-surface of vorticity magnitude (top view), (c) contour of longitudinal velocity on a midplane at $t = 4.7$ s (top view) and (d) contour of longitudinal velocity on a midplane at $t = 6.7$ s (top view).



(a) $x=0\text{m}$ midplane showing top surface separation and back step recirculation

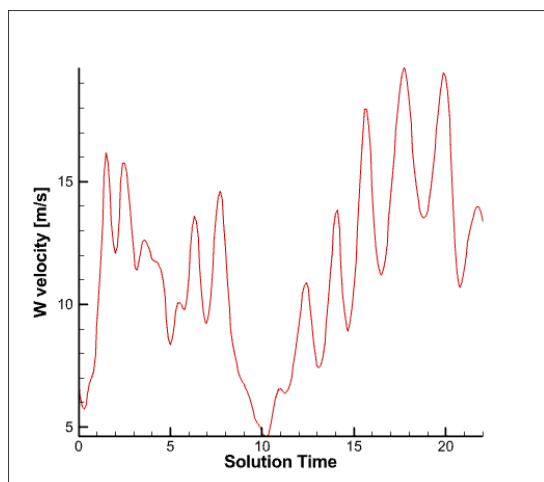


(b) $y=5\text{m}$ midplane showing side surface separation and symmetrical aft recirculation

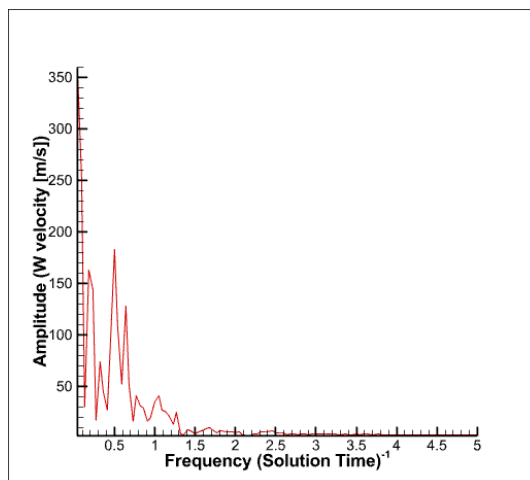


(c) diagonal plane that capture corner edge separation and aft recirculation

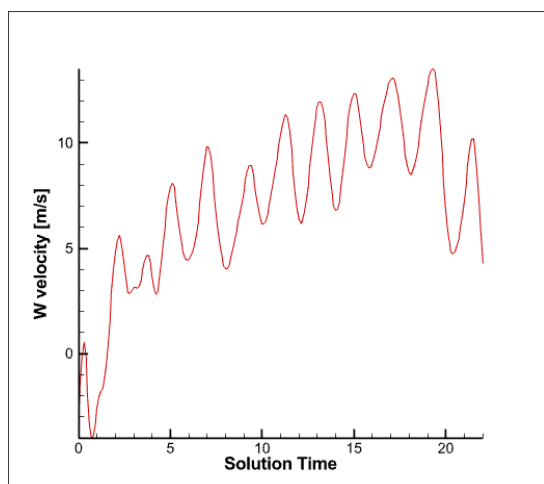
Figure 80: Contour of time-averaged longitudinal velocity for a 30m/s headwind simulation over a 10m cube using CGE/VorTran-M along planes containing primary flow features.



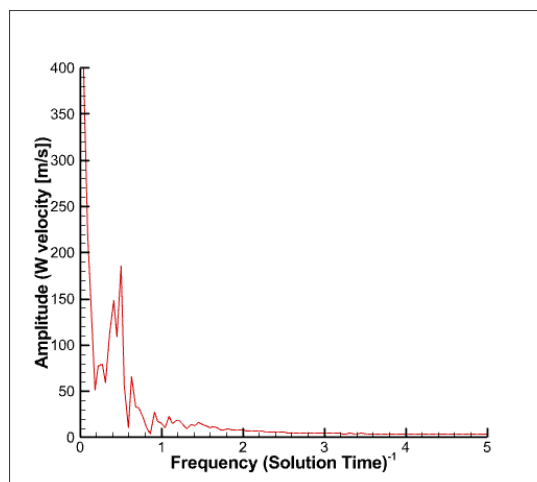
(a) $z = H$, time history



(b) $z = H$, Fourier transform

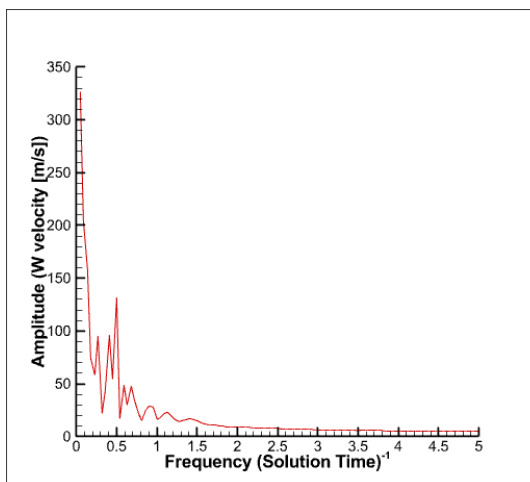
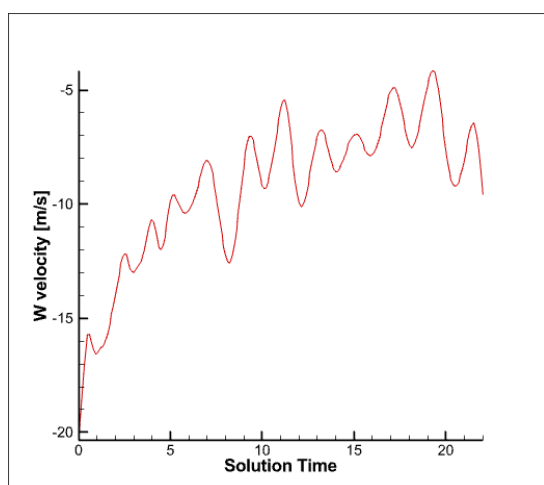


(c) $z = 2H$, time history



(b) $z = 2H$, Fourier transform

Figure 81: Time histories and corresponding Fourier transform plots of longitudinal velocity at points aft of the cube at $y = 0.5H$.



(e) $z = 3H$, time history(b) $z = 3H$, Fourier transform

Figure 81: Time histories and corresponding Fourier transform plots of longitudinal velocity at points aft of the cube at $y = 0.5H$.

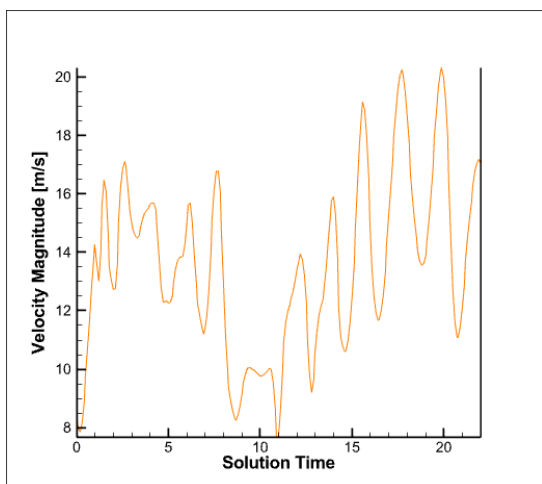
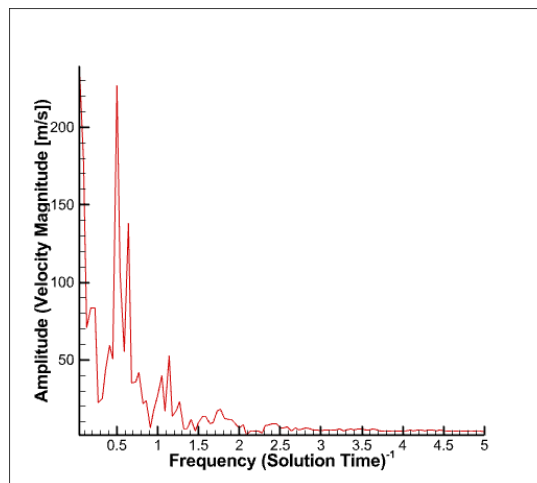
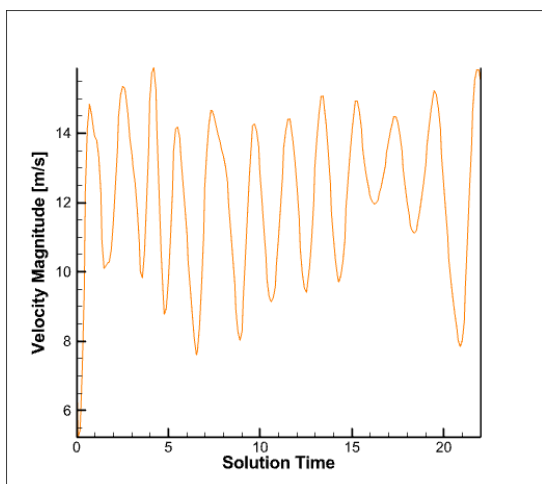
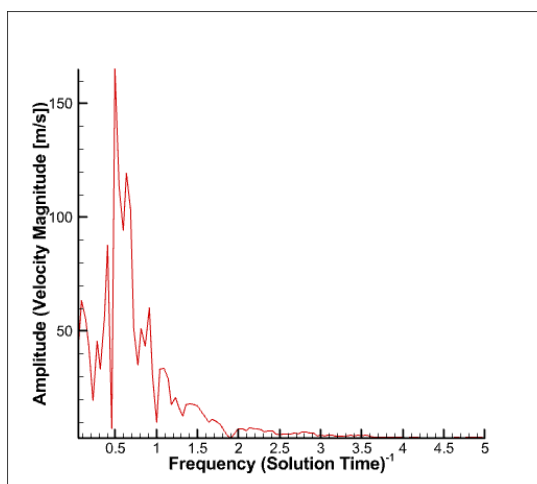
(a) $z = H$, time history(b) $z = H$, Fourier transform

Figure 82: Time histories and corresponding Fourier transform plots of velocity magnitude at points aft of the cube at $y = 0.5H$.

(c) $z = 2H$, time history(b) $z = 2H$, Fourier transform

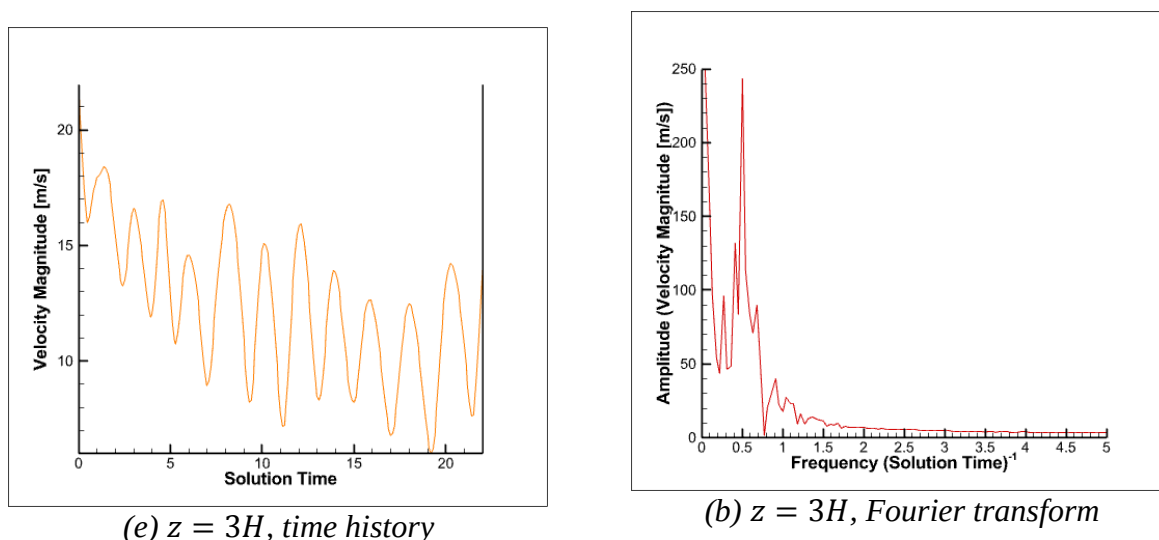
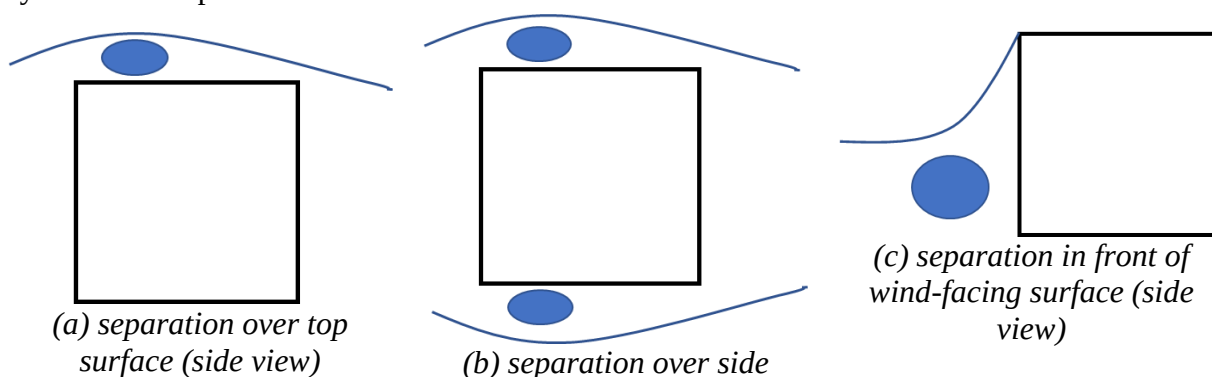


Figure 82: Time histories and corresponding Fourier transform plots of velocity magnitude at points aft of the cube at $y = 0.5H$.

Simplified Wake Representation

Vortex Model Fitting

Six primary flow features were identified from the CFD simulation simulations and are shown in Figure 83. Given these vortex-based features, we investigated the development of an airwake model that would reconstruct the airwake velocities through a linear superposition of analytical vortex models that capture the velocity profile distributions associated with each of these flow features. Two-dimensional vortices are chosen as starting point for simplicity. When superimposing these vortices to generate 3D airwake profiles, proper coordinate transformations and weightings to control the influence of the individual vortices in the 3D spatial and temporal domain will have to be implemented. The use of analytical vortex models paves the way for parametrization and scaling such that the airwake around different ship geometries may be obtained by modifying inputs to the vortex models, without having to carry out explicit CFD calculations. Model fitting has been performed for the top surface flow separation (Figure 83(a)), separation over the side surfaces Figure 83(b)), backward-facing step separation (Figure 83(d)) and edge vortices (Figure 83(f)). The von Karman streak illustrated in Figure 83(e) is only noted in time-accurate simulations. In time-averaged contours of velocity components, a symmetrical distribution of velocity is noted. Model fitting has been performed for this symmetrical representation as well. These are further described below.



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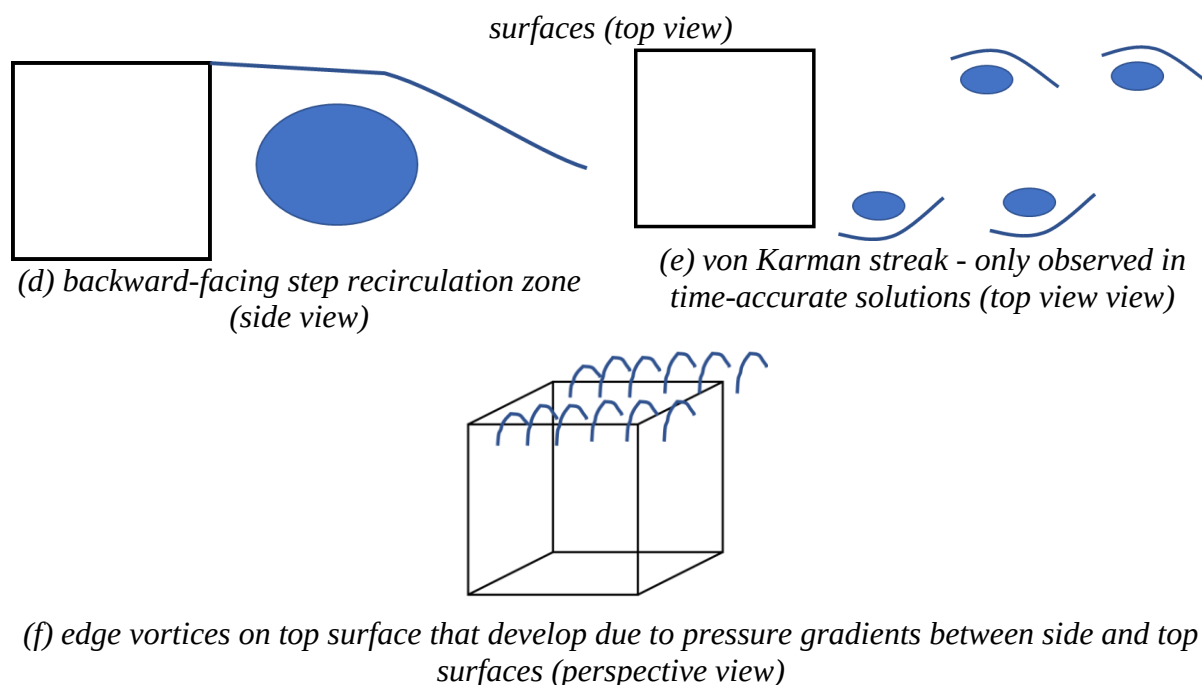


Figure 83: Primary flow features identified from CGE/VorTran-M simulation of 30 m/s headwind over 10 m cube; freestream direction from left to right.

Top-surface flow separation modeling

Flow detaches at the top edge of the wind-facing surface and reattaches in the lee of the obstacle (see Figure 78). Due to the interaction of the incoming freestream above the obstacle with this separation, a recirculation zone is created on the top surface. The flow can also reattach on the top surface, depending on the incoming free stream speed and obstacle dimensions [53]. Recirculation zones are typically associated with a swirling velocity field, making the use of vortex models suitable. For this region, a vortex model based on the one proposed Banks and Meroney [89, 90] is used to capture the longitudinal velocity profile along the midplane of the obstacle.

This validated 2D model was developed to capture the influence of conical vortices on surface pressure. Conical vortices are generated from sharp corners in oblique wind conditions (see Figure 84). These vortices have been studied extensively by the wind engineering community given that they generate high suction regions on roofs of low-rise buildings, which, in turn, can lead to severe structural damage [71, 72, 74]. A depiction of the vortex flow model is shown in Figure 85 [89], where a vortex of radius h with center at point O is placed above the surface such that it touches the surface at point S . To allow for a smooth transition from constant vorticity within the vortex to regions of zero vorticity where potential flow theory applies, a transition region is introduced, with point M on Figure 85 indicating the start of the potential flow region. Point C represents the location of maximum velocity in the transition region and is typically set to the midpoint location in the transition region. The vortex is envisioned to be driven like a wheel, spinning due to freestream velocity acting at point M [89]. Banks and Meroney provide piece-wise continuous expressions for velocity across five distinct regions of the domain, shown in Figure 86. The nondimensional coordinate $a = \zeta/h$ will be used when referencing distances relative to the vortex center. The core is assumed to have a diameter of

$0.2h$ and represents a region dominated by viscosity, where flow follows a solid body rotation motion. In the region between the core and transition ($0.2 < a < 1$), the velocity profile is based on a power curve fit to the velocity profile obtained from a Navier-Stokes based simulation for a 65° swept delta-wing at a 10° angle of attack at 70% chord and Mach number $Ma = 0.85$. This relation is used because of the similarity between conical vortices on the roof of a building and delta-wing vortices observed on delta-wing aircraft. Between the core and roof surface ($-1 < a < 0.2$), pressure changes occur due to centrifugal accelerations of the vortex, such that the resulting governing equation is [89]

$$\frac{dP}{dn} = \frac{\rho U^2}{R_c}, \quad (8)$$

where P is the pressure, n is the unit normal to the curvature, ρ is the fluid density, U is the fluid speed in the direction of the vortex, and R_c is the radius of curvature. In the potential flow region, flow merges with freestream and both Bernoulli's equation and Eq. (8) are obeyed. The equations representing the velocity profile from this model are summarized in Table 2. There are three primary inputs to the model: (1) the vortex radius (h), (2) the location of point M (R_M), and (3) the velocity at point M (U_M).

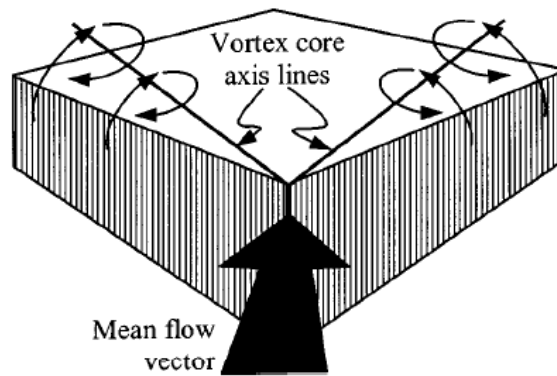


Figure 84: Illustration of conical vortices that form on roof in oblique wind conditions [89].

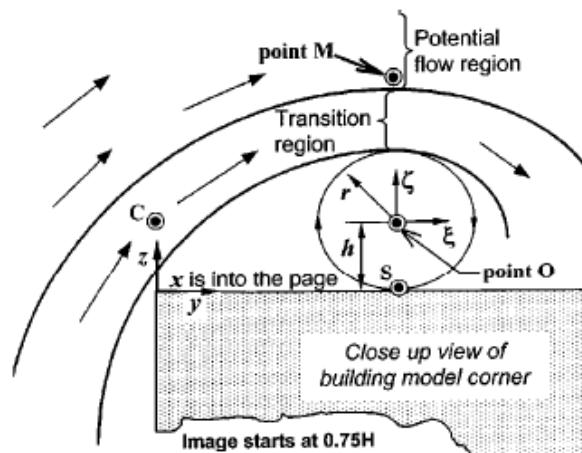


Figure 85: Illustration of the vortex model proposed by Banks and Meroney [90].

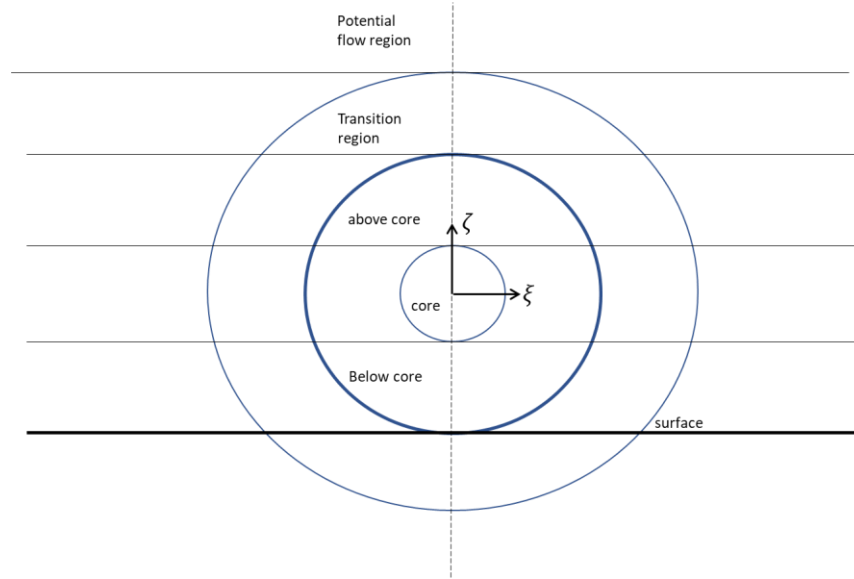


Figure 86: Illustration of the vortex model proposed by Banks and Meroney highlighting the five regions considered.

Table 2: Velocity profile equations for Banks and Meroney vortex model [89]; $a_1 = 0.2$ is used to delimit vortex core size, $a_2 = 1$ is used to delimit vortex size, a_3 is used to demark start of potential flow region, a_{max} is used to denoted the point in the transition region where maximum velocity is obtained, U_{max} is taken to be the velocity at point M, $U_{a_1} = U_{vortex}(a_1)$, $U_{a_2} = U_{transition}(a_2)$, $U_{a_3} = U_{transition}(a_3)$ and $n' = n/h$.

R_c/h	Range	Description	Velocity Equation
$-\infty$	$a = -1$	Roof surface	$U = 0$
$a/(1+a)$	$-a_2 < a < -a_1$	Between roof and vortex core	$U(a) = -U_{vortex}(a)$ $= -U_{a_2} \cdot \sqrt{a/-a_2}$
a	$ a < a_1$	Viscous vortex core	$U(a) = U_{core}(a) = U_{a_1} \cdot a/a_1$ $U(a) = U_{vortex}(a)$
$a + a^3/2$	$a_1 < a < a_2$	Vortex, above core	$= U_{a_2} \cdot \sqrt{a/a_2}$
$a + a^3/2$	$a_2 < a < a_3$	Transition region	$U(a) = U_{transition}(a)$ $= \frac{U_{max} \cdot 2 \cdot (a/a_{max})}{1 + (a/a_{max})^2}$
$a + a^3/2$	$a_3 < a$	Potential flow region	$U(a) = U_{potflow}(a)$ $= U_{a_3} \sqrt{e^{\int_{a_3}^a \frac{-2}{(R_c/h)} dn'}}$

Tecplot's vortex core extraction feature was used to identify the location of the time-averaged vortex for the top separation, highlighted in Figure 87. This location corresponds to $z = 2.5$ m relative to the global coordinate system. Banks and Meroney's vortex model was used to determine the longitudinal velocity profile along a vertical line that passes through the identified vortex core center. The vortex radius h was determined by manual tuning to best capture the velocity profile predicted by CGE/VorTran-M. Following the approach laid out in Ref. [90], point M can be selected to be at a location where the correlation coefficient between

the velocity at the given location and the surface pressure coefficient at point S is close to 1. Figure 88 shows a plot of the correlation coefficient as a function of height from the top surface, and suggests that any point above $y = 3.2$ m can be chosen. The location of point M was determined by manual tuning to best capture the velocity profile. The final model parameters and corresponding velocity profile are shown in Table 3 and Figure 89(a), respectively. Velocity predictions from the model closely follow those from the CGE/VorTran-M simulation, with maximum offsets of approximately 4 m/s, noted in the regions above the core center. Below the core center, model predictions deviate from CFD, which, interestingly, seem to follow a viscous boundary layer (BL) profile despite being inviscid. This appears to be due to the suction pressures associated with the separated flow over the top surface, and the deviation from CGE predictions below the vortex center is expected given the symmetry in the vortex model. The velocity profile below the vortex center can be adequately captured using a quadratic fit of the form

$$U(y) = c_1(-y^2 - y), \quad (9)$$

as shown in Figure 89(b). The constant $c_1 = 10$ was used to generate the figure.

Table 3: Vortex model parameter values; distances specified relative to top surface.

Parameter	Value
h (m)	1.5
R_M (m)	4.9
U_M (m/s)	-32.6

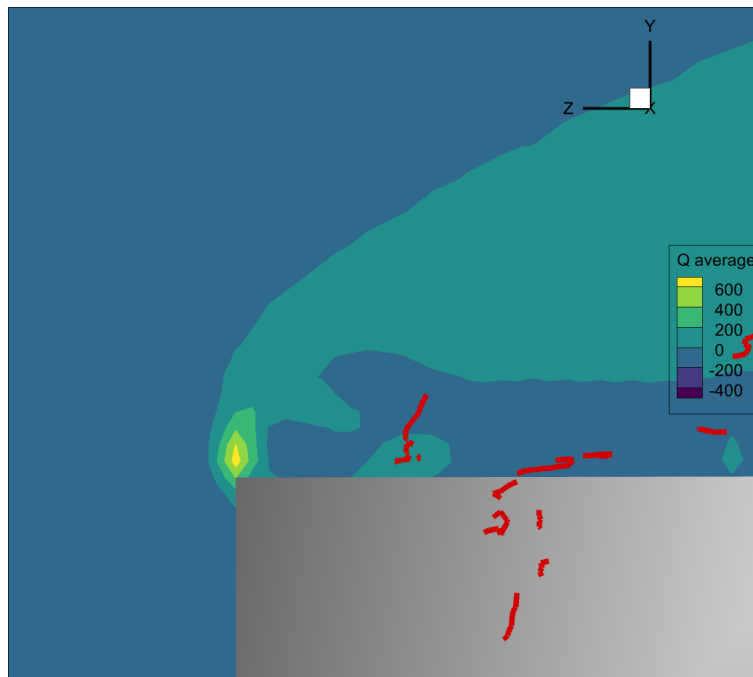


Figure 87: Contour of the time-averaged Q criterion across a midplane through the center of the obstacle; red lines indicated vortex core centers determined by Tecplot.

Steps were taken to expand the vortex model predictions from the one-dimensional vertical line across the vortex center to the full 2D region over the top surface of the cube. The equations in Table 2 are expressed in radial coordinates. However, the velocity profiles obtained using the

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model for radial lines at nonzero angular displacements from the vertical axis did not adequately capture results from CFD, as illustrated in Figure 90 where velocity profiles for radial lines at $\theta = 30^\circ$ and $\theta = 60^\circ$ (with θ measured counterclockwise from the ζ axis in Figure 86) are plotted.

An alternate method to obtain the 2D velocity profile was thus devised. Vortex model fitting was performed at five different span locations along the top surface of the cube; $z = \{5, 2.5, 0, -2.5, 5\}$ m, resulting in five corresponding values for the model inputs h , R_M and U_M , summarized in Table 4. The velocity profile prediction for $z = -2.5$ m is shown in Figure 89 whereas the other four locations are shown in **Error! Reference source not found.**. Except for the region within a height of 1 m from the top surface at $z = 5$ m (**Error! Reference source not found.**(a)), the manually tuned vortex models at the various span locations show reasonable agreement with CFD predictions. The $z = 5$ m location corresponds to the leading-edge corner of the top vortex. The discrepancy within the 1 m region from the surface is acceptable given that in practice, the aircraft will be very close to landing at that position, and it's unlikely that velocities at this height will have any influence on flying qualities. Furthermore, if this height is nondimensionalized by the cube height $H = 10$ m, we can generalize that model predictions are likely not valid in the $0.1H$ region above the obstacle near the leading edge. Maximum offset at the $z = 5$ m span location is noted in the potential flow region, where deviations of approximately 20% from the -30 m/s predicted by CGE/VorTran-M are evident. These deviations are also considered to be acceptable in the current study.

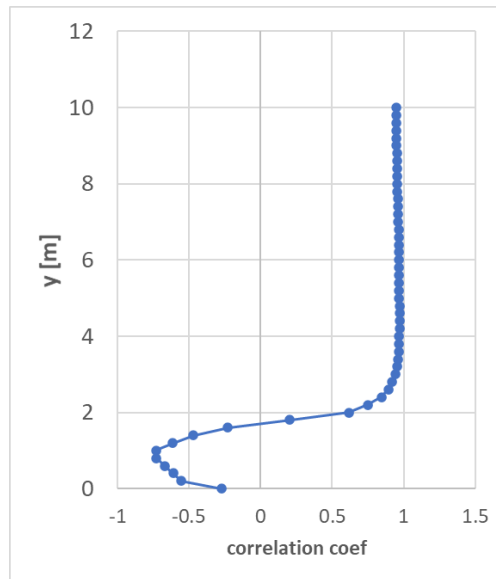
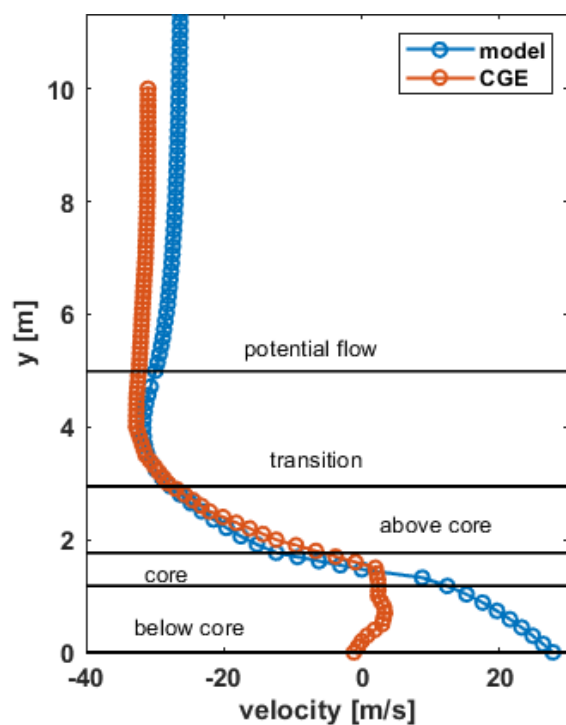


Figure 88: Correlation coefficient between longitudinal velocity at various heights above the top surface and surface pressure.

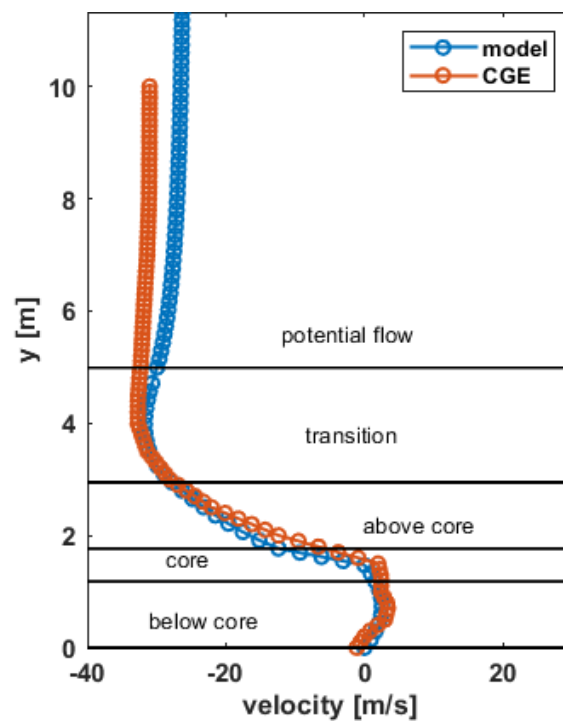
Quadratic polynomials were fitted onto the values in Table 4 to obtain spanwise functions $h(z)$, $R_M(z)$ and $U_M(z)$. The resulting equations are valid over the top surface of the cube ($-5 \leq z \leq 5$ m). Velocity profile predictions at $z = \{3.5, -3.5\}$ m are plotted in Figure 92. These locations were not considered during the fitting but still show good agreement with predictions from CFD.

The 2D vortex model used to capture the velocity profile along the top surface of the cube is effectively an extension of the Banks and Merony model [89, 90] that assumes an infinite

number of overlapping vortices of differing sizes exist along the top surface. This representation adequately captures the velocity profile for the current case study where flow reattaches in the lee of the obstacle. For flow regimes where reattachment occurs on the top surface directly, modifications to this model may be required.



(a) original vortex model equations used



(b) modified version of vortex model with quadratic fit below vortex core center used

Figure 89: Velocity profile along vertical line at $z = 2.5m$.

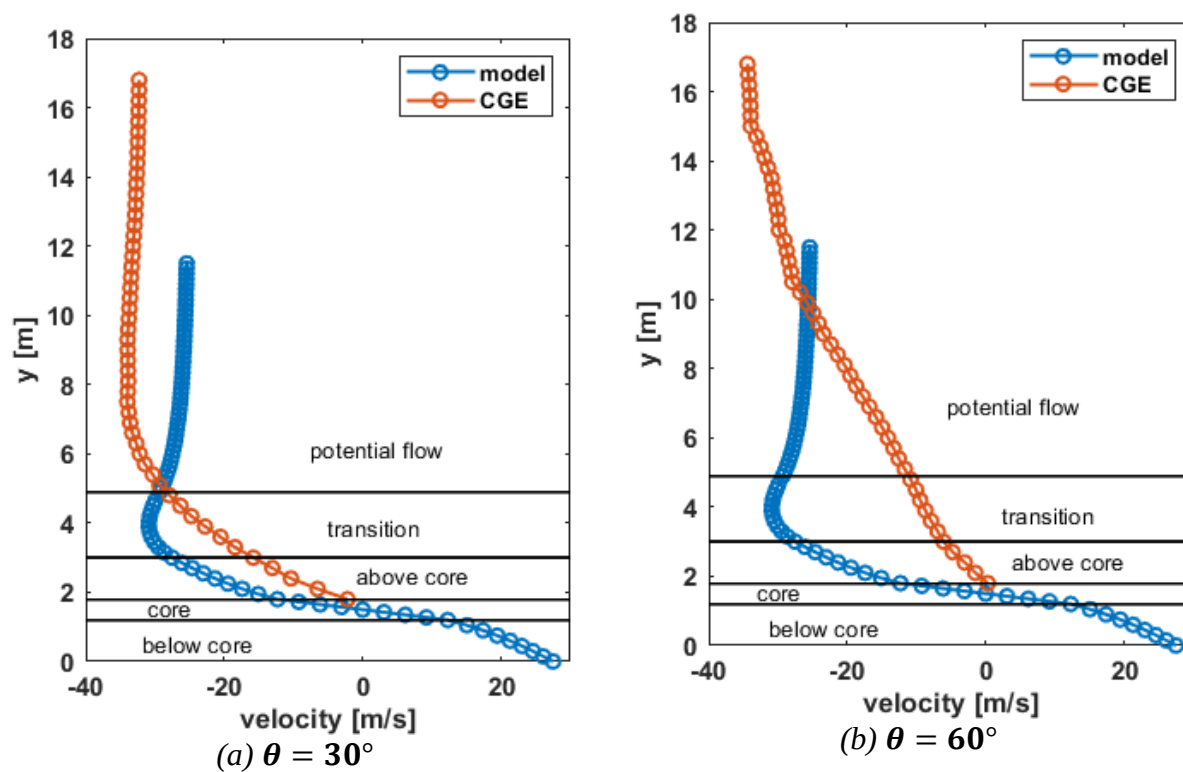


Figure 90: Velocity profile along non-vertical radial line with vortex center at $z = 2.5$ m.

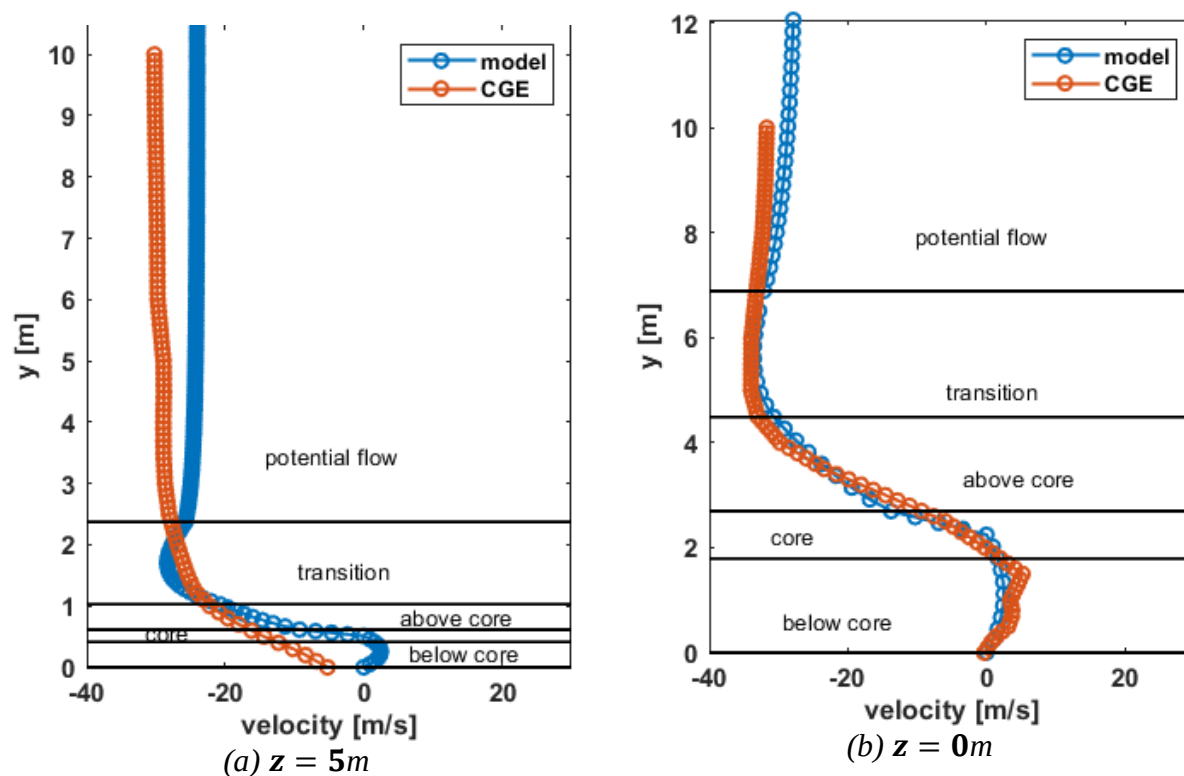
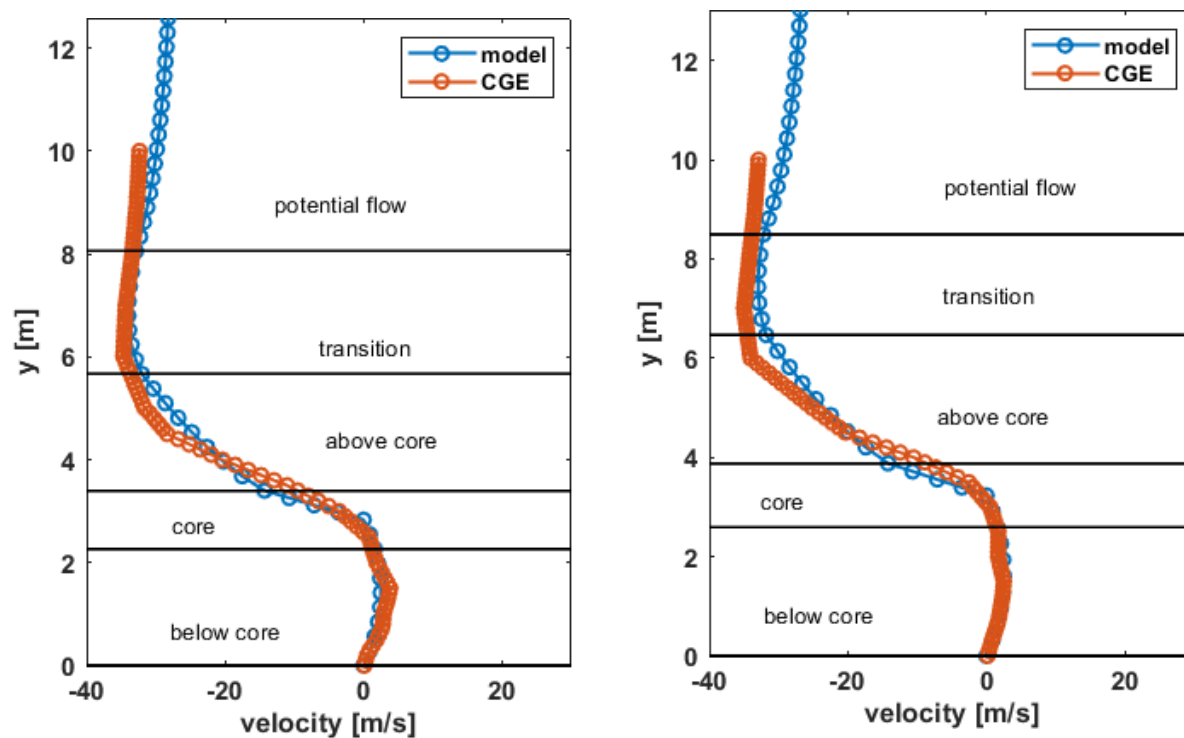


Figure 91: Velocity profile at various longitudinal span locations for a 30 m/s headwind.

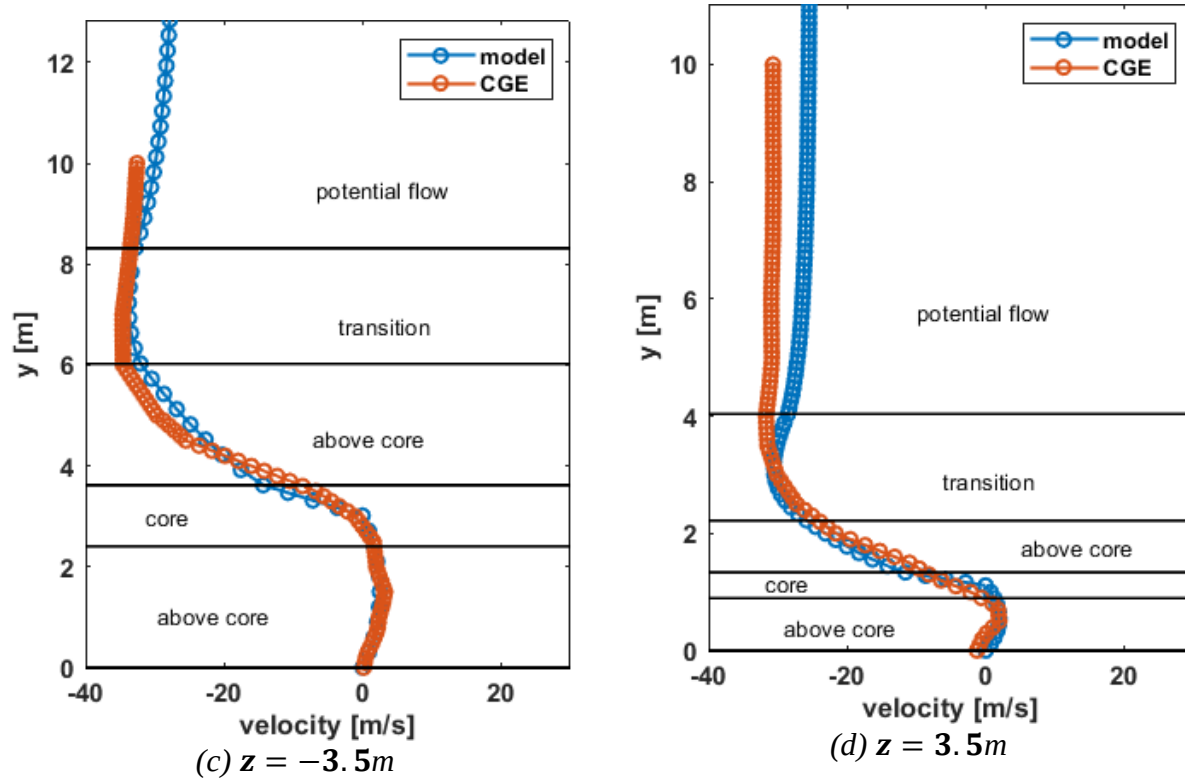


(c) $z = -2.5m$ (d) $z = -5.0m$

Figure 91: Velocity profile at various longitudinal span locations for a 30 m/s headwind.

Table 4: Vortex model input parameters for various longitudinal span locations.

z [m]	h [m]	U_M [m/s]	R_M [m]
5	0.5	-28.0	2.4
2.5	1.5	-32.6	4.9
0	2.25	-33.2	7.0
-2.5	2.8	-33.6	8.0
-5	3.25	-33.2	8.5

Figure 92: Velocity profile at two longitudinal span locations not used in determining $h(z)$, $R_M(z)$ and $U_M(z)$.

A preliminary model scaling investigation was also carried out using CGE/VorTran-M simulations of a 40 m/s headwind over the cube. It was found that the velocity profile along the top surface of the cube along the midplane could be captured by the model by simply scaling $U_M(z)$ for the 30 m/s case accordingly, see Figure 93. As noted previously, this scaling relationship is expected given the inviscid solver being used but also is a good approximation when viscous flow effects are included given the magnitude of the operating Reynolds number.

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The application limits of the vortex model presented above still need to be investigated but we anticipate it to be applicable to rectangular prisms of varying dimensions. Model input parameters h and R_M can be potentially parametrized for scaling using either the height, length, or width of the obstacle, and the rotational velocity U_M using freestream velocity.

Backward-facing step flow separation modeling

Aft of the cube, shear layers are shed from the top and side edges of the backward-facing surface, see Figure 78. The shear layers shed from the top edge are considered here (Figure 83(d)). In practice, vortex shedding in this region is dynamic; the location where the shear layer impinges on the wall fluctuates, shifting the reattachment zone upstream and downstream in a low frequency motion [40, 41, 45, 46], with the recirculation bubble shrinking and expanding accordingly. We focus here on time-averaged velocity profiles.

Time-averaged streamlines across the midplane of the cube are shown in Figure 94, and a single leeside primary vortex structure is evident. The center of the vortex was found to be at $(z, y) = (-18.8, 10.9)$ m using Tecplot's vortex core extraction feature.

Vortex model fitting was performed to capture the longitudinal velocity profile along a vertical line through the identified vortex center. Peak longitudinal velocity of 14.7 m/s was noted at $y = 2.9$ m along that line. A Rankine vortex model was first used with a velocity profile given by

$$U(y) = \begin{cases} \Omega r & r \leq r_c \\ \frac{\Omega r_c^2}{r} & r > r_c \end{cases}, \quad (10)$$

where Ω is the angular rotation speed of the vortex, determined using the peak longitudinal velocity and vortex core radius r_c (set here to 8 m, which is the distance from the vortex center to the location where peak longitudinal velocity is noted). As evident in Figure 95, the linear velocity variation associated with the Rankine vortex core failed to adequately represent the velocity profile predicted by CGE/VorTran-M.

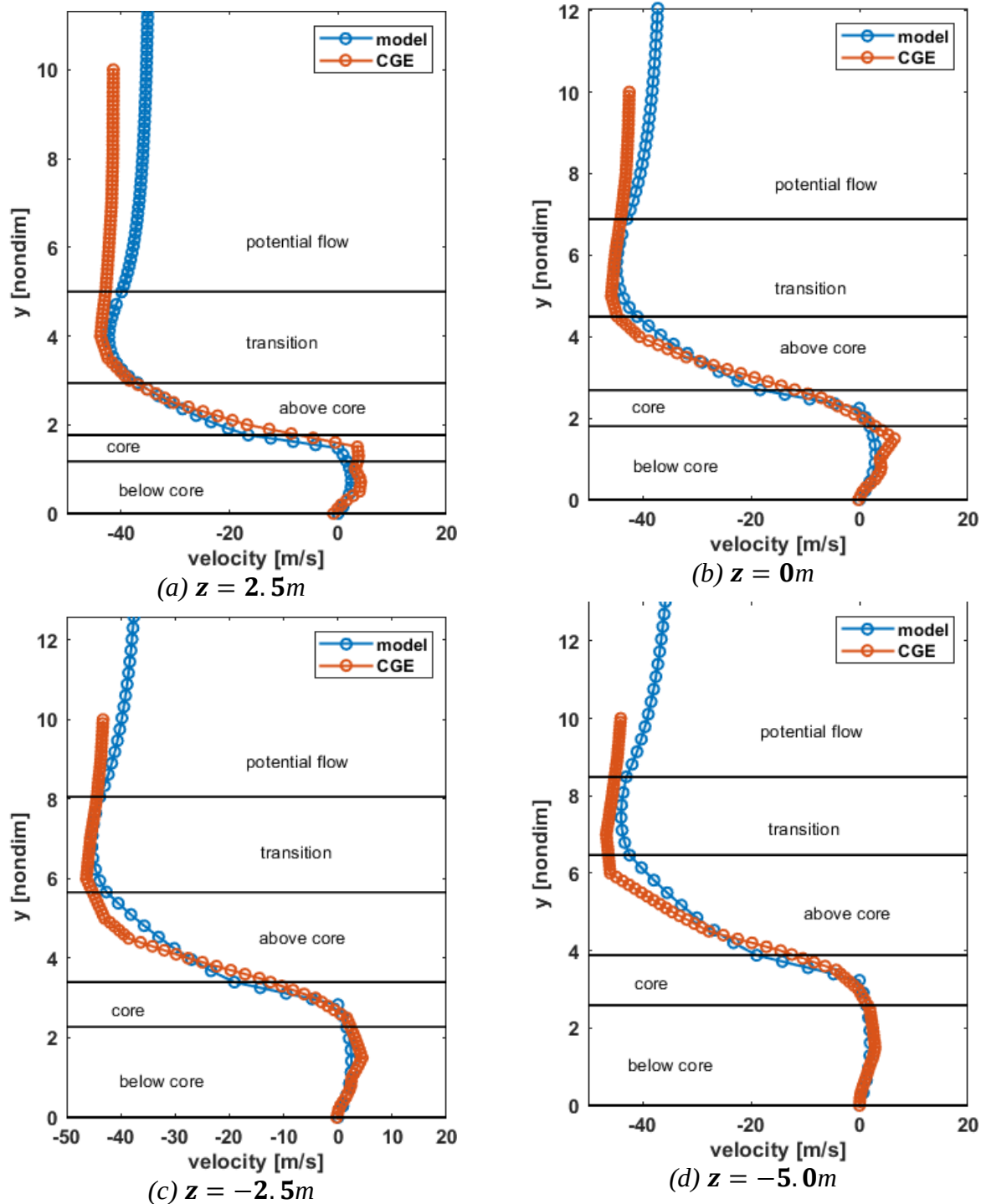


Figure 93: Velocity profile at various longitudinal span locations for a 40m/s headwind.

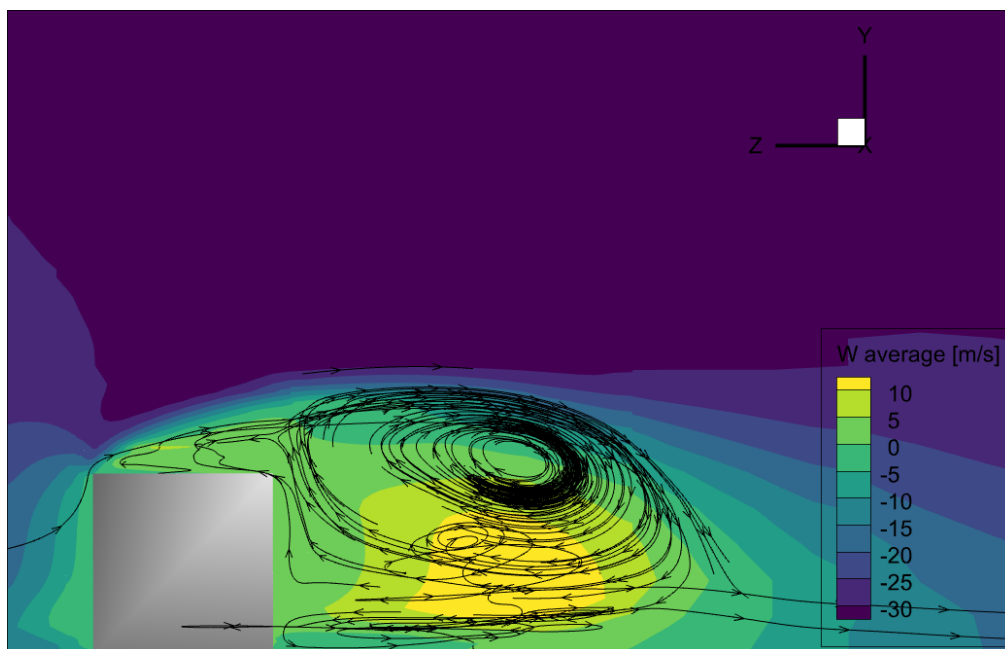


Figure 94: Contour of time-averaged longitudinal velocity profile along midplane with streamlines indicating presence of aft of the obstacle.

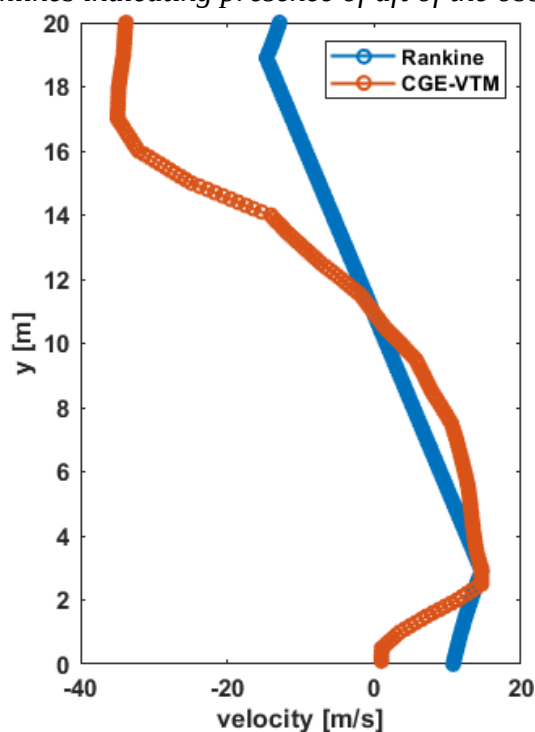


Figure 95: Rankine vortex-based velocity profile along vertical line through vortex center.

A modified version of the Banks and Meroney model [89, 90] introduced in the previous section was found to produce a better agreement. The equations in Table 2 were still applied but in this case, the vortex was shifted upwards such that a transition region existed below the vortex center as well, as depicted in Figure 96. With this shift, the vortex model can be thought of as

wheel being spun at two points: point M_{upper} at a velocity $U_{M_{upper}}$ and point M_{lower} at a velocity $U_{M_{lower}}$. Inputs to the model thus consisted of

1. the vortex radius (h),
2. the location of points M_{upper} and M_{lower} (assumed to be equidistant from the vortex center)
3. the velocity at point M_{upper} ($U_{M_{upper}}$) and
4. the velocity at point M_{lower} ($U_{M_{lower}}$).

To maintain the piece-wise continuity of the vortex model velocity profile predictions, the maximum velocity U_{max} (see Table 2) used in the core, below core, and lower transition regions was set equal to $U_{M_{lower}}$. In the upper transition and potential flow regions, U_{max} was set equal to $U_{M_{upper}}$. In the above core region, linear interpolation between $U_{M_{lower}}$ and $U_{M_{upper}}$ was used to determine U_{max} . Two additional modifications were made to the vortex model introduced in the previous section:

1. the location where the maximum velocity was obtained in the transition region (a_{max}) was changed from the midpoint location to the end point of the transition region; and
2. the size of the vortex core was increased from $0.2h$ to $0.4h$.

Velocity predictions in the region below the lower transition were obtained using linear interpolation between the velocity at point M_{lower} and 0 m/s at a point y_0 , below which the velocity was assumed to be zero. Vortex parameters and resulting velocity profiles are shown in Table 5 and Figure 97, respectively, where the model shows good agreement with the velocity profile predicted by CGE/VorTran-M, with a maximum deviation of approximately 7 m/s noted at $y = 14$ m.

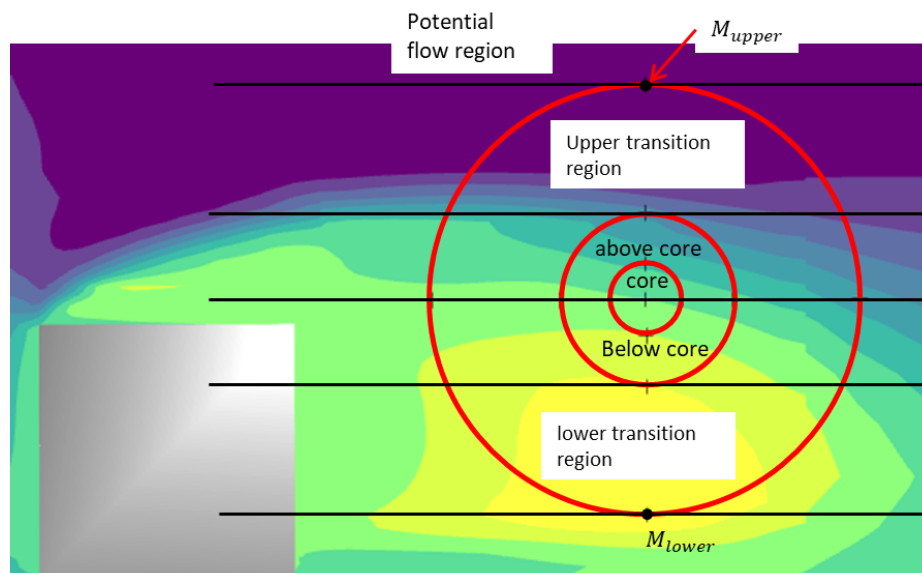


Figure 96: Illustration of modified Banks and Meroney vortex model shifted upwards.

Table 5: Modified vortex model parameter values; distances specified relative to global CGE coordinate system.

Parameter	Value
h (m)	3.4
R_M (m)	8.4
$U_{M_{lower}}$ (m/s)	-14.7 ¹
$U_{M_{upper}}$ (m/s)	-34.2
y_0 (m)	0.5

¹ Value was negated to obtain correct profile.

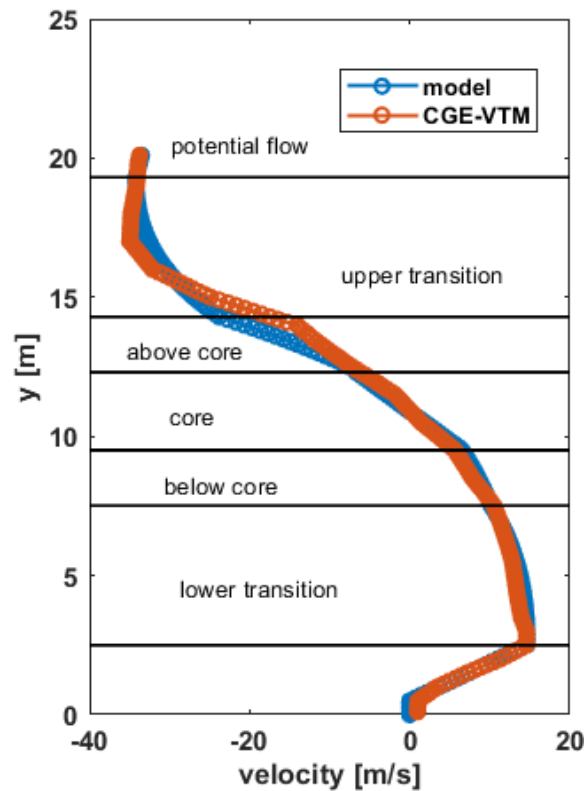


Figure 97: Longitudinal velocity profile along a vertical line through vortex center in the lee of the cube.

Next, velocity profiles along radial lines at $\theta = \{30^\circ, 45^\circ, 135^\circ, 150^\circ\}^T$ were considered, where θ is measured counterclockwise from an axis z' , which has its origin at the vortex center and is parallel to the global CGE z axis shown in the contour plots above. We found that a circular vortex model, as depicted in Figure 96, failed to fully capture the velocity predictions from CGE/VorTran-M (see Figure 98). Reasonable agreement is noted for $\theta = 135^\circ$ and $\theta = 150^\circ$ in Figure 98, but large discrepancies are noted for the $\theta = 30^\circ$ and $\theta = 45^\circ$ cases. These discrepancies are attributed to the influence of the top surface separation considered in the previous section, and the corresponding vortex model parameters are summarized in Table 6. Note that longitudinal velocity values were used as rotational velocities $U_{M_{lower}}$ and $U_{M_{upper}}$. Using tangential velocities resulted in significant offsets in model predictions.

Table 6: Modified vortex model parameter inputs for the radial lines considered.

Parameter	$\theta = 30^\circ$	$\theta = 45^\circ$	$\theta = 135^\circ$	$\theta = 150^\circ$
h (m)	3.4	3.4	3.4	3.4
R_M (m)	8.4	8.4	8.4	8.4
$U_{M_{lower}}$ (m/s)	-2.5 ¹	-8.0 ¹	-8.9 ¹	-7.0 ¹
$U_{M_{upper}}$ (m/s)	-20.9	-34.7	-31.8	-28.2
y_0 (m)	0.5	0.5	0.5	0.5

¹ Values negated to obtain profile.

To better match CGE/VorTran-M velocity predictions, we then investigated the use of an ellipse-shaped vortex following the streamline pattern shown in Figure 94. The dimensions in Table 5 were used to define the semi-minor radii, and the semi-major radii were obtained by applying a scaling factor to the semi-minor radius values. A scaling factor value of 3.3 was used in this work based on manual tuning. The ellipse-based vortex was also assumed to have a built-in slant angle γ , measured counterclockwise relative to the z' axis. Using position coordinates of several points along the streamlines shown in Figure 94, the slant angle was determined to be approximately 16° . The resulting vortex shape is illustrated Figure 99.

Velocities obtained using the ellipse-based vortex model are shown in Figure 100. Results show good agreement with predictions from CGE/VorTran-M, with the largest discrepancies again noted for the $\theta = 30^\circ$ and $\theta = 45^\circ$ cases. These discrepancies, however, are significantly less pronounced than those noted in Figure 98.

It is important to clarify here that this “elliptical vortex” is a mathematical modeling construct that was found to better represent the CFD source data in the recirculation region. Physically, such a construct does not exist but can be viewed as an approximation of a vortical structure in which the vorticity is distributed over a spatial dimension (e.g., a vortex line or sheet). In this physical case, the actual velocity field associated with this vortical structure may not be precisely elliptical in shape, depending on the distribution of vorticity. This distinction was not deemed critical to the formulation of the canonical airwake models.

Note further that the velocities $U_{M_{lower}}$ and $U_{M_{upper}}$ were initially probed manually from Tecplot. To facilitate the vortex model fitting process, the value of $U_{M_{lower}}$ at a given span location aft of the cube was set to the maximum longitudinal velocity along the vertical line at that location. Similarly, $U_{M_{upper}}$ was set to the minimum longitudinal velocity. These definitions are based on the CGE problem setup, where freestream is in the negative z direction. A Tecplot macro script was used to query the velocity profiles at various span location aft of the cube, which were then post-processed to obtain quadratic functions $U_{M_{lower}}(z)$ and $U_{M_{upper}}(z)$. These functions were used to generate the profiles in Figure 100.

Although this ellipse-based vortex fit showed good agreement for radial lines at nonzero θ angles, it failed to capture the velocities along z span locations that did not traverse all three vortex regions shown in Figure 99, producing good predictions primarily in the $-23 \leq z \leq -15$ m region. Further tuning of the model was performed, and better predictions were obtained using a semi-minor radius to semi-major radius of 10 and a slant angle of approximately 8° . The resulting vortex shape is illustrated Figure 101. Note that the vortex core region only extends to $z \approx -32$ m downstream of the obstacle. Velocity predictions were obtained using a coordinate system that has its origin at the center of the ellipse, as illustrated in Figure 102. At any given

span location, it was assumed that the radial coordinate from the center of the ellipse to any point below the intersection point between the ξ axis and the vertical line at the span location was negative. Similarly, any radial coordinate above the intersection point was positive. This sign convention was adopted for consistency with the Banks and Meroney model equations shown in Table 2.

Velocities obtained using the updated ellipse-based vortex model are shown in Figure 103 and Figure 104. Results show good agreement with prediction from CGE/VorTran-M up to $z = -26$ m. Apart from velocity predictions at $z = -18$ m, a kink in the predictions is noted in the core region at all span locations in Figure 103. This is due to the sudden switch in radial coordinate sign from negative to positive at those locations. The $z = -18$ m location is close to the vortex center ($z = -18.8$ m) and thus the magnitude of the radial coordinate at the intersection of the ξ axis and the vertical line at $z = -18$ m is relatively small (see Figure 102). From Figure 103 and Figure 104, predictions from the model deteriorate downstream from $z = -26$ m onwards. Sharp changes in velocities are also noted beyond $z = -30$ m, where the vortex core region does not exist (see Figure 101).

To better match CGE/VorTran-M predictions beyond $z = -26$ m, a secondary vortex model was adopted in this downstream region. This model follows a similar structure to that presented for the top surface separation above and assumes that an infinite number of overlapping vortices of differing sizes exist in the downstream region. Linear functions for the vortex center location $y_0(z)$, vortex radius $h(z)$, vortex core fraction $c(z)$, and location of $M_{upper}(z)$ were derived by manually tuning vortex model parameters at various z locations aft of $z = -26$ m:

$$y_0(\bar{z}) = -0.1514\bar{z} + 11.481, \quad (11)$$

$$h(\bar{z}) = 0.2045\bar{z} + 3.7321, \quad (12)$$

$$c(\bar{z}) = 0.0124\bar{z} + 0.4, \quad (13)$$

$$M_{upper}(\bar{z}) = 0.0661\bar{z} + 8.604, \quad (14)$$

where $\bar{z} = z_1 - z$, with $z_1 = -26$ m. From Figure 104, the inviscid nature of the simulation is apparent from the nonzero velocities near the ground at span location downstream of $z = -26$ m. In practice, velocity near the ground would be close to zero. To better match the *inviscid* predictions, a linear function for the velocity near the ground was derived:

$$V_{y_0}(z) = -0.7252\bar{z} - 1.3413. \quad (15)$$

Velocity predictions in the region below the lower transition (see Figure 96) at a given span location were then obtained using linear interpolation between the velocity $U_{M_{lower}}(z)$ at point M_{lower} and $V_{y_0}(z)$ from Eq. (15) at point y_0 , below which the velocity was assumed to be $V_{y_0}(z)$.

The combined use of the ellipse-based vortex model up to $z = -26$ m and the infinite vortex model beyond that location allowed for the velocity profile aft of the obstacle to be adequately captured as shown in Figure 105 and Figure 106. Note that the velocities in the core region in Figure 105 were obtained using an explicit linear interpolation between the velocity in the above core and below core regions to avoid the kinks seen in Figure 103.

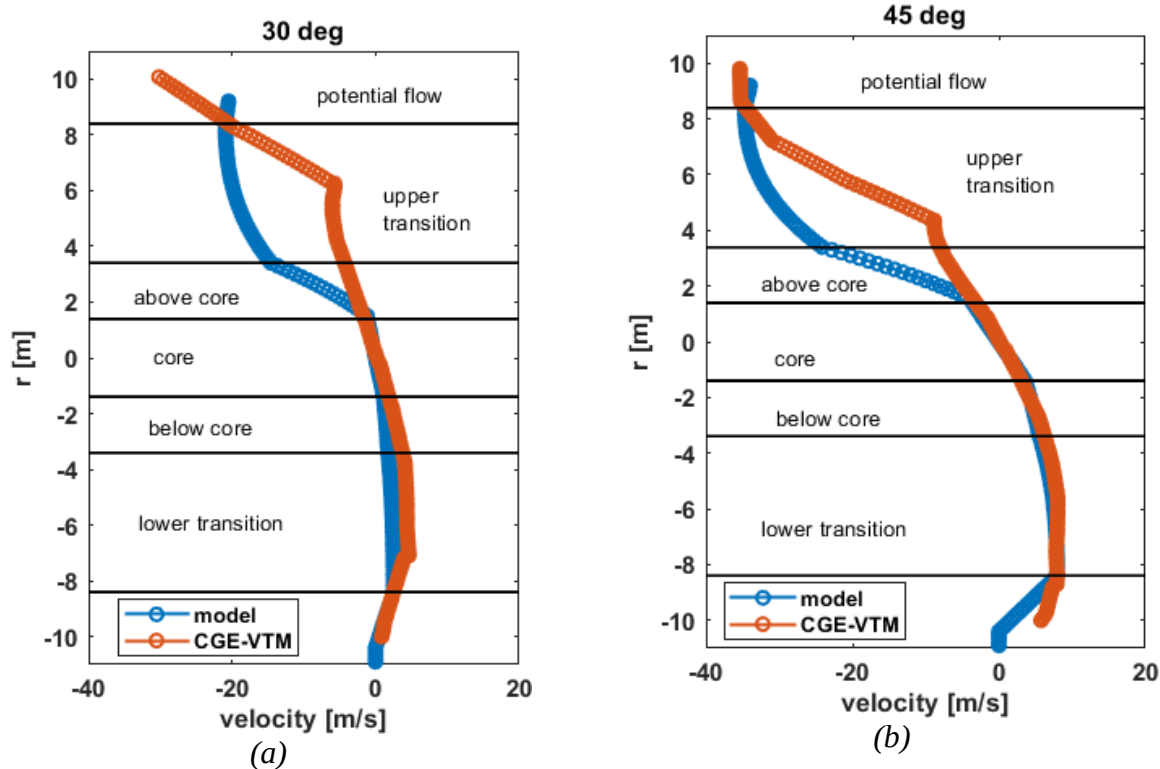


Figure 98: Velocity profiles along radial lines at nonzero angular displacements in the lee of the obstacle; vortex assumed to be circular.

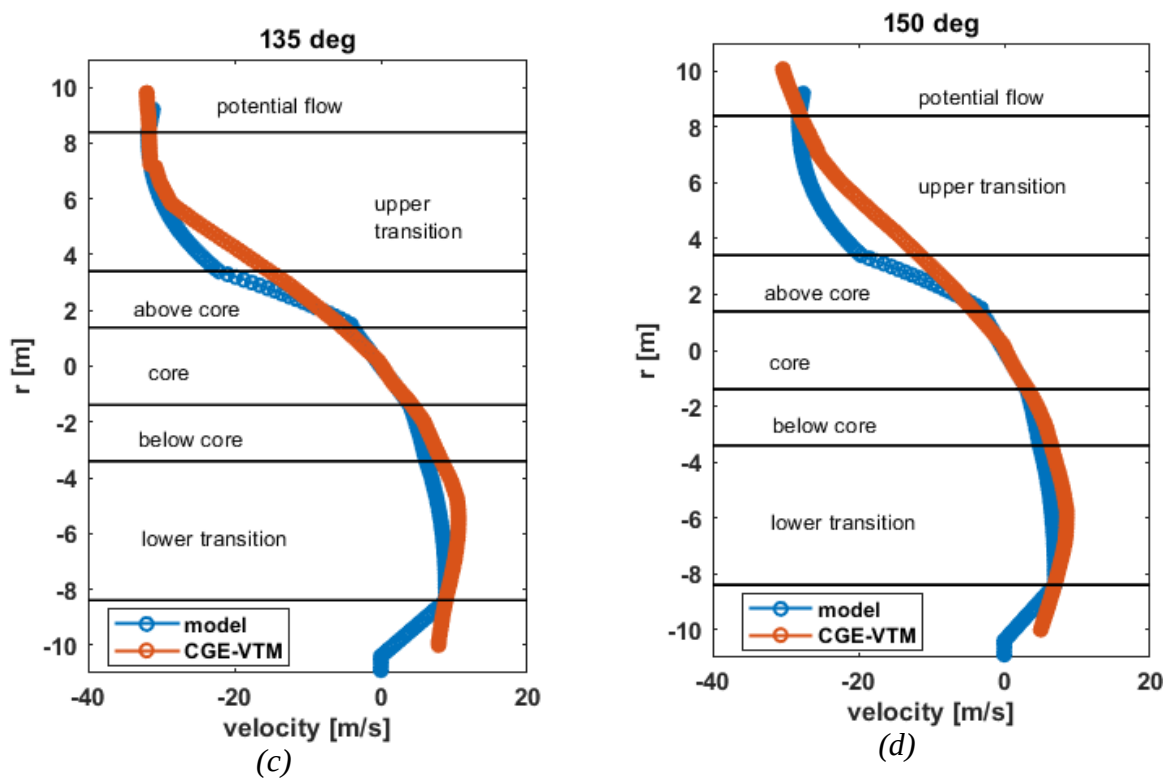


Figure 98: Velocity profiles along radial lines at nonzero angular displacements in the lee of

the obstacle; vortex assumed to be circular.

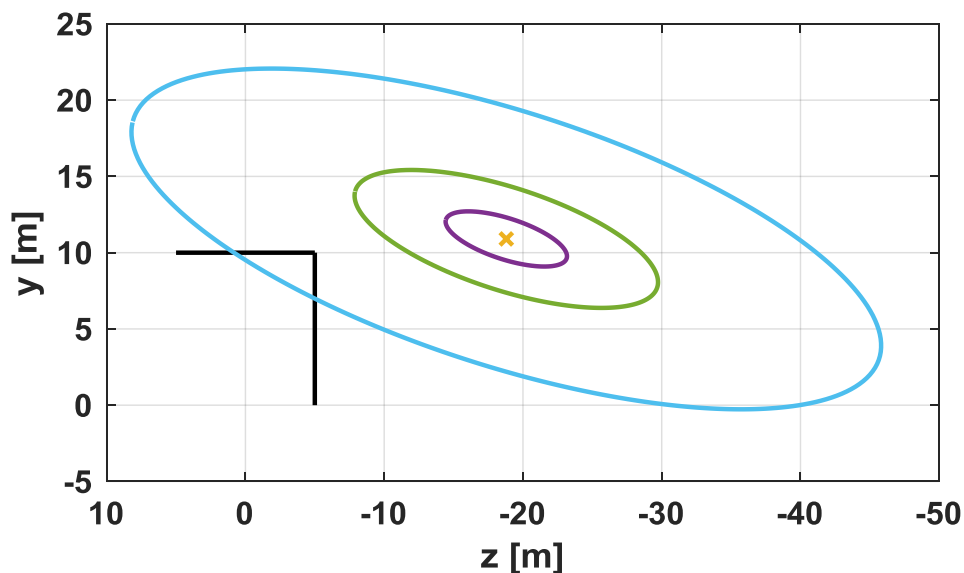


Figure 99: Illustration of the ellipse-based vortex; black line outlines the obstacle; orange cross indicates vortex center; purple line delimits vortex core; green line delimits vortex size; and blue line indicates the end of the transition region.

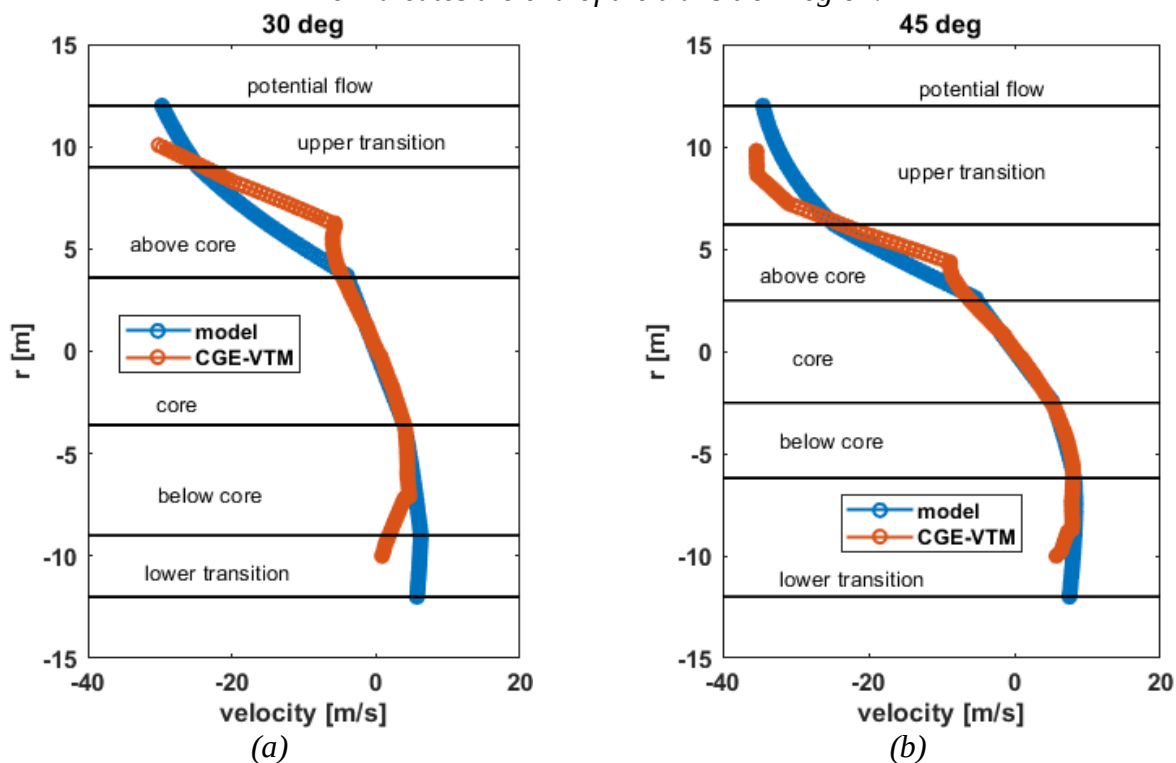


Figure 100: Velocity profiles along radial lines at nonzero angular displacements in the lee of the obstacle; vortex assumed to be a slanted ellipse.

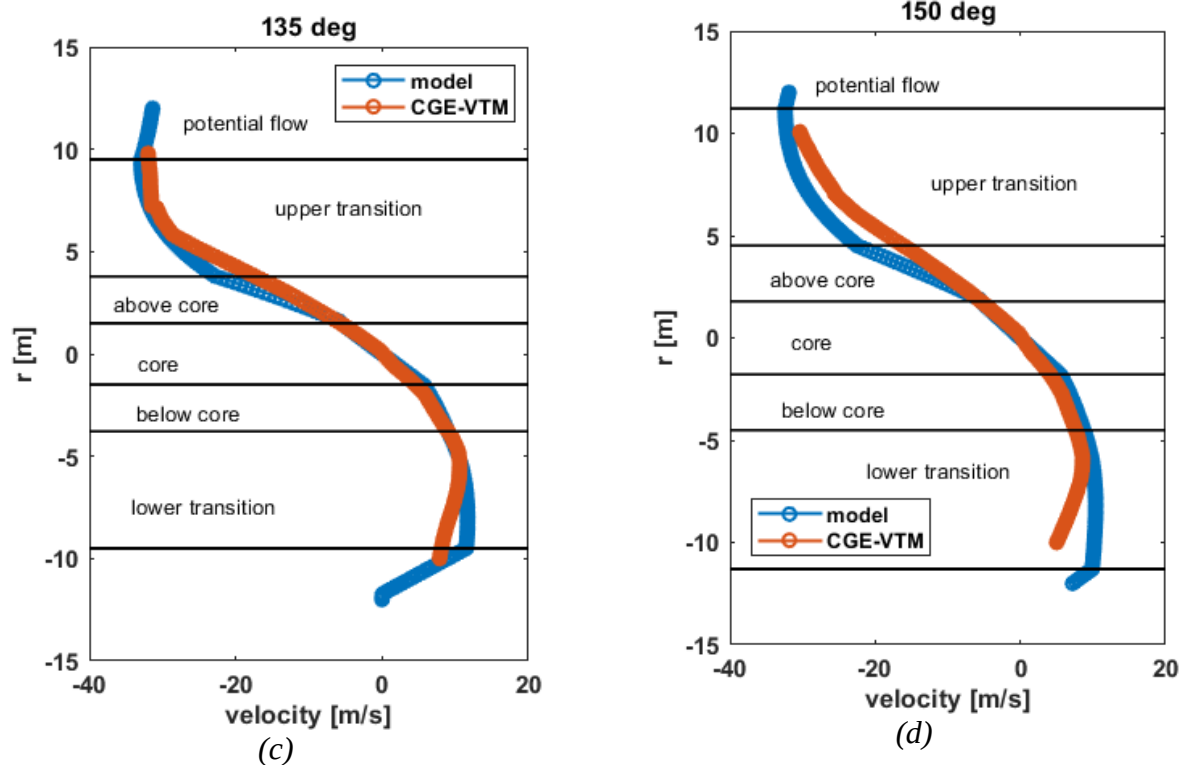


Figure 100: Velocity profiles along radial lines at nonzero angular displacements in the lee of the obstacle; vortex assumed to be a slanted ellipse.

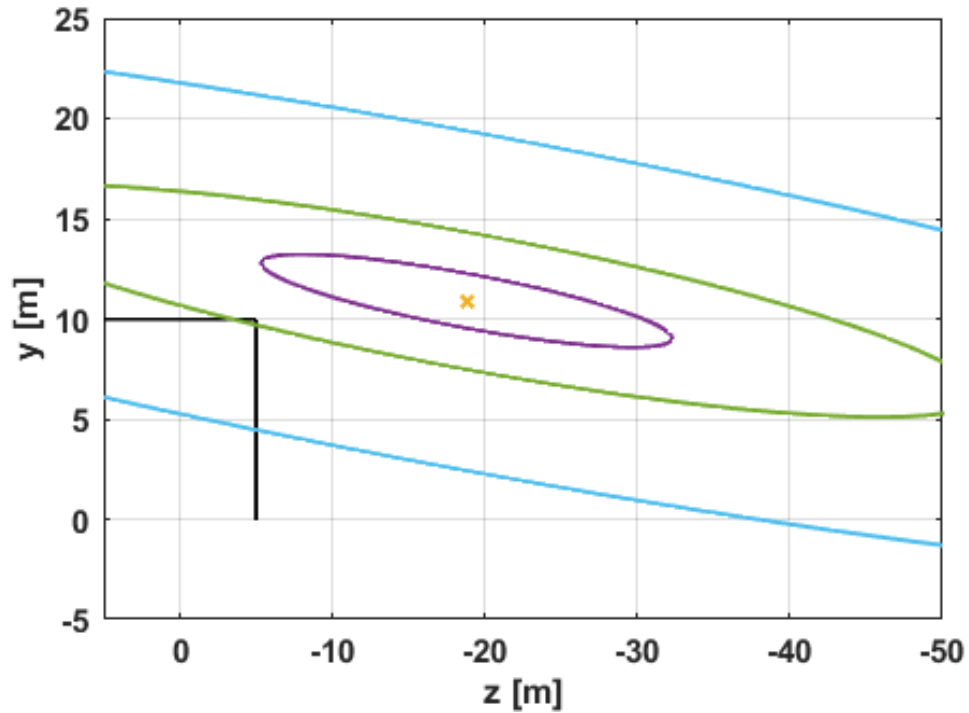


Figure 101: Illustration of modified ellipse-based vortex; black line outlines the obstacle; orange cross indicates vortex center; purple line delimits vortex core; green line delimits vortex size; and blue line indicates the end of the transition region.

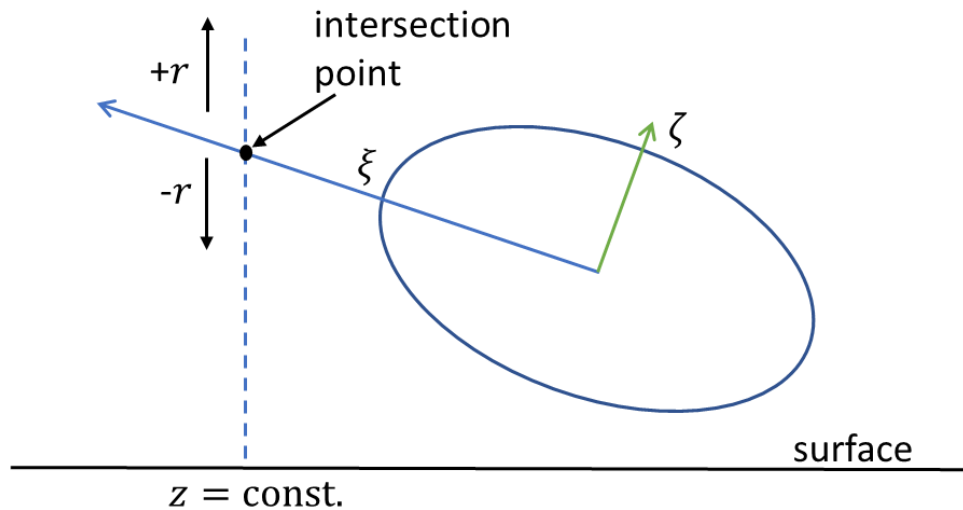


Figure 102: Illustration of slanted ellipse coordinate system and sign convention employed for radial coordinates from the center of the ellipse.

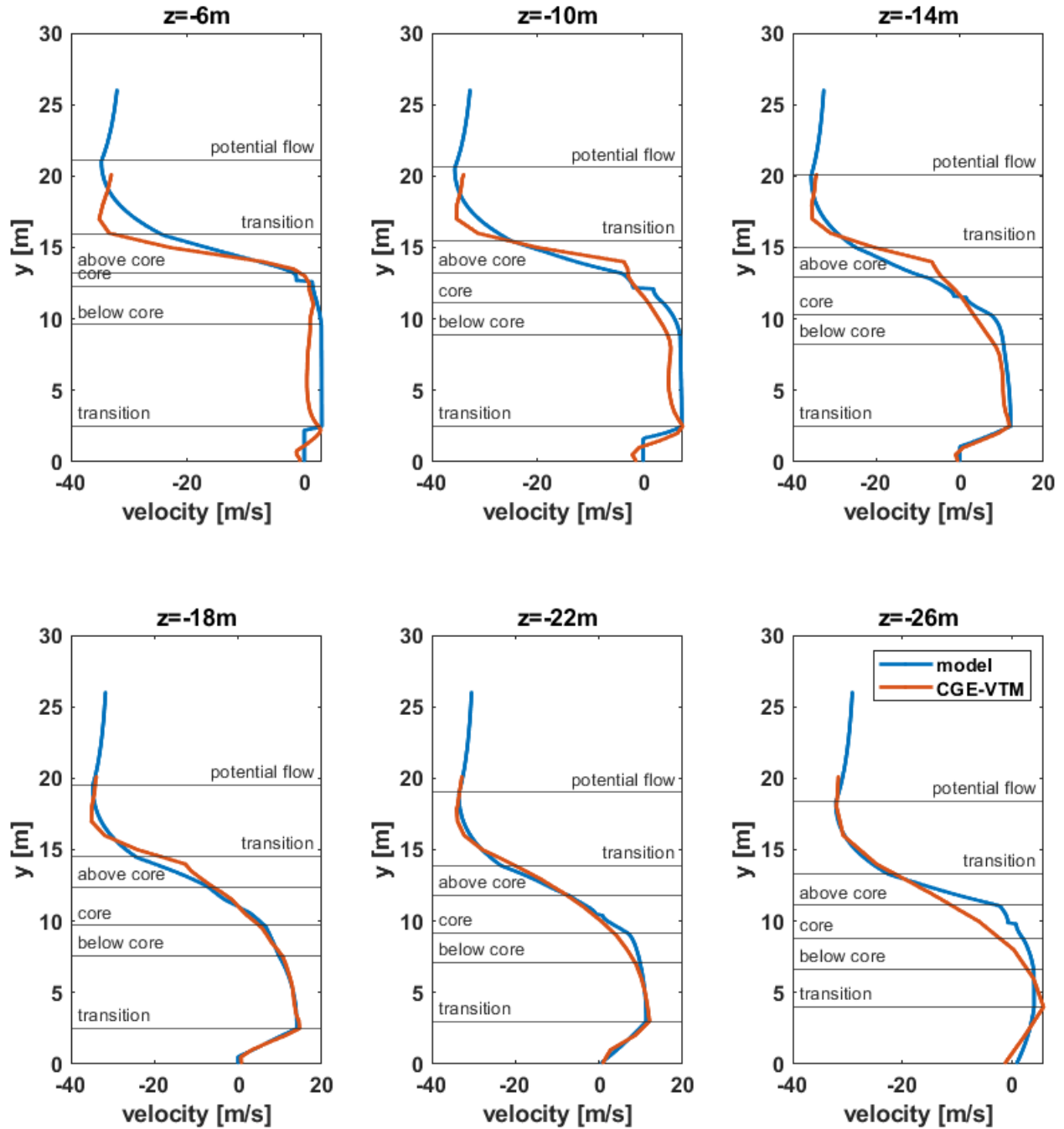


Figure 103: Longitudinal velocity profile at various span locations in the $-6\text{ m} \geq z \geq -26\text{ m}$ range aft of the obstacle along the $x = 0\text{ m}$ plane; only the slanted ellipse-based vortex model used; CGE/VorTran-M data queried in 0.1 m height increments at every span location.

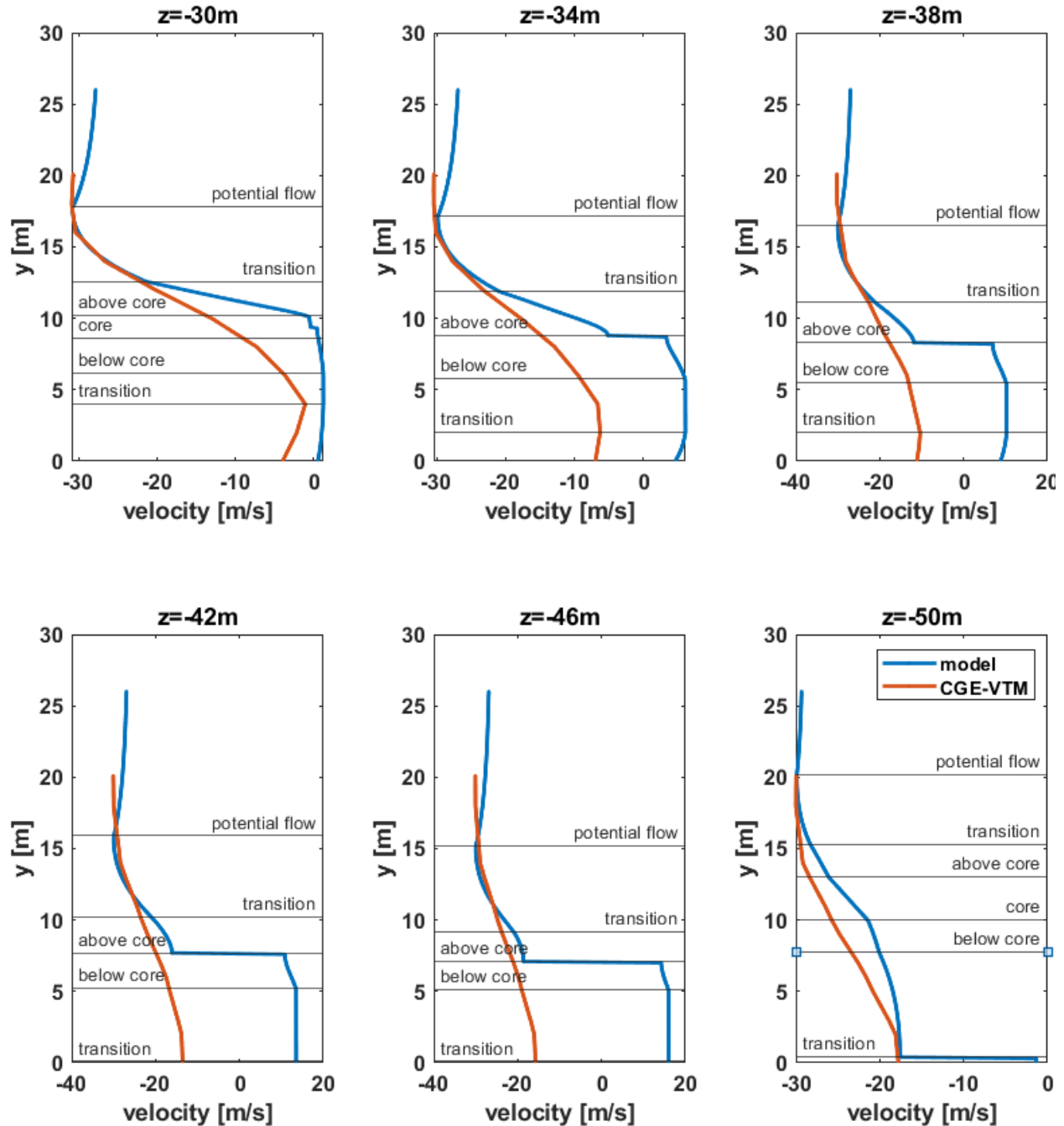


Figure 104: Longitudinal velocity profile at various span locations in the $-30\text{ m} \geq z \geq -50\text{ m}$ range aft of the obstacle along the $x = 0\text{ m}$; only the slanted ellipse-based vortex model used; CGE/VorTran-M data queried in 0.1 m height increments at every span location.

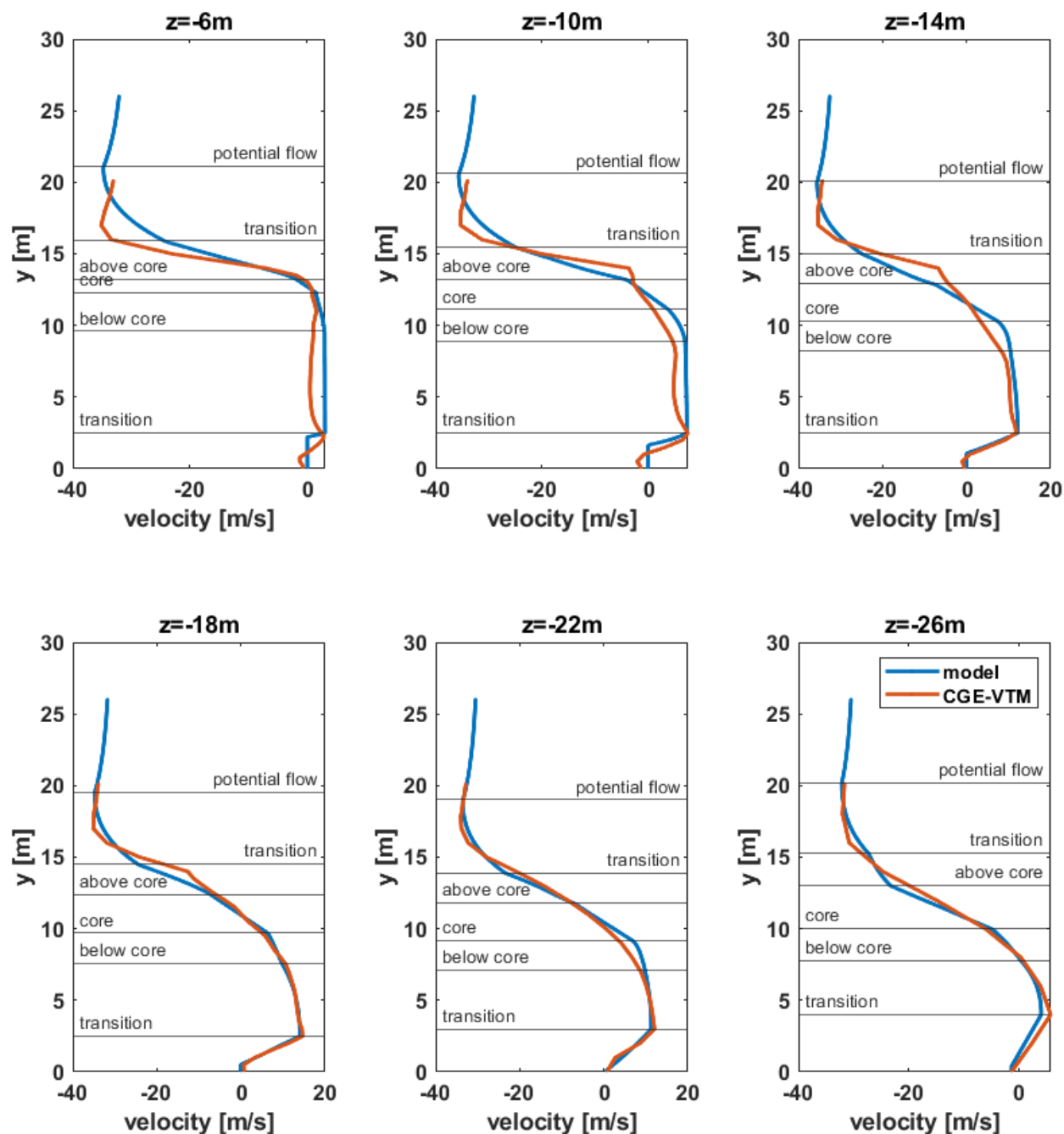


Figure 105: Longitudinal velocity profile at various span locations aft of the obstacle along the $x = 0$ m plane using the slanted ellipse-based vortex model for $-6 \text{ m} \geq z > -26 \text{ m}$ and the infinite vortex model for $z = -26 \text{ m}$; CGE/VorTran-M data queried in 0.1 m height increments at every span location.

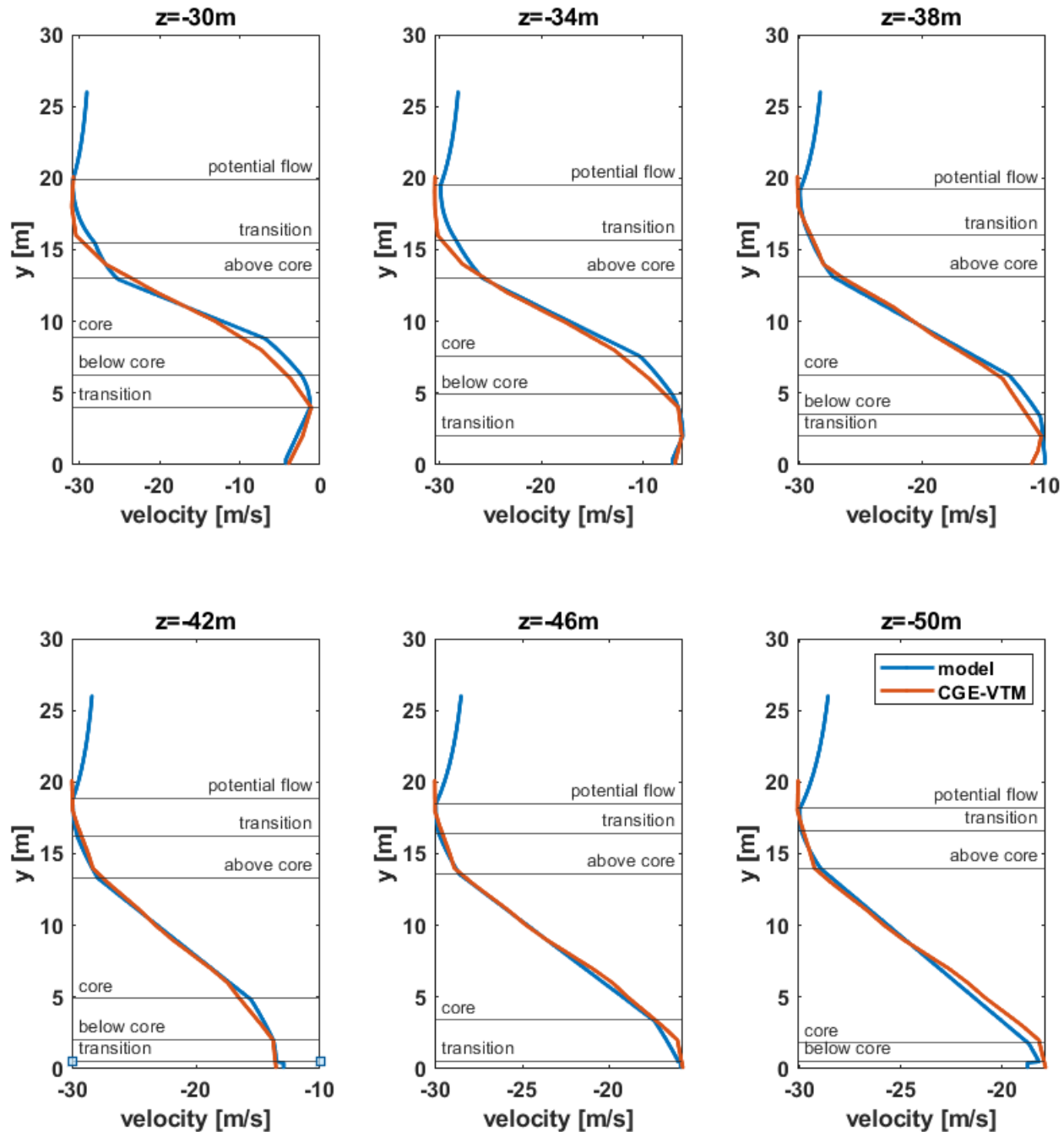


Figure 106: Longitudinal velocity profile at various span locations in the $-30\text{ m} \geq z \geq -50\text{ m}$ range aft of the obstacle along the $x = 0\text{ m}$ plane using the infinite vortex model; CGE/VorTran-M data queried in 0.1 m height increments at every span location.

Side surface flow separation modeling

In addition to the top edge, flow also detaches at the side edges of the wind-facing surface and reattaches aft of the obstacle as depicted in Figure 78. A recirculation zone is created on the side surfaces due to interaction of the incoming freestream with this separation (see Figure 83(b) and Figure 107). Given the similarity of the side surface separation to the top surface separation considered previously, the infinite vortex model used to capture the velocity distribution across

the top surface was also employed here. Results are shown in Figure 108 across the $y = 5$ m plane for separation over the left surface ($x = 5$ m in Figure 107). Except for the region within a height of 1m from the side surface at $z = 5$ m, reasonable agreement with CFD predictions is obtained without any modifications to the model. The discrepancy within the 1m region from the surface was also noted for the top surface and is deemed acceptable given that in practice, it is unlikely that the aircraft will be flying that close to a given superstructure.

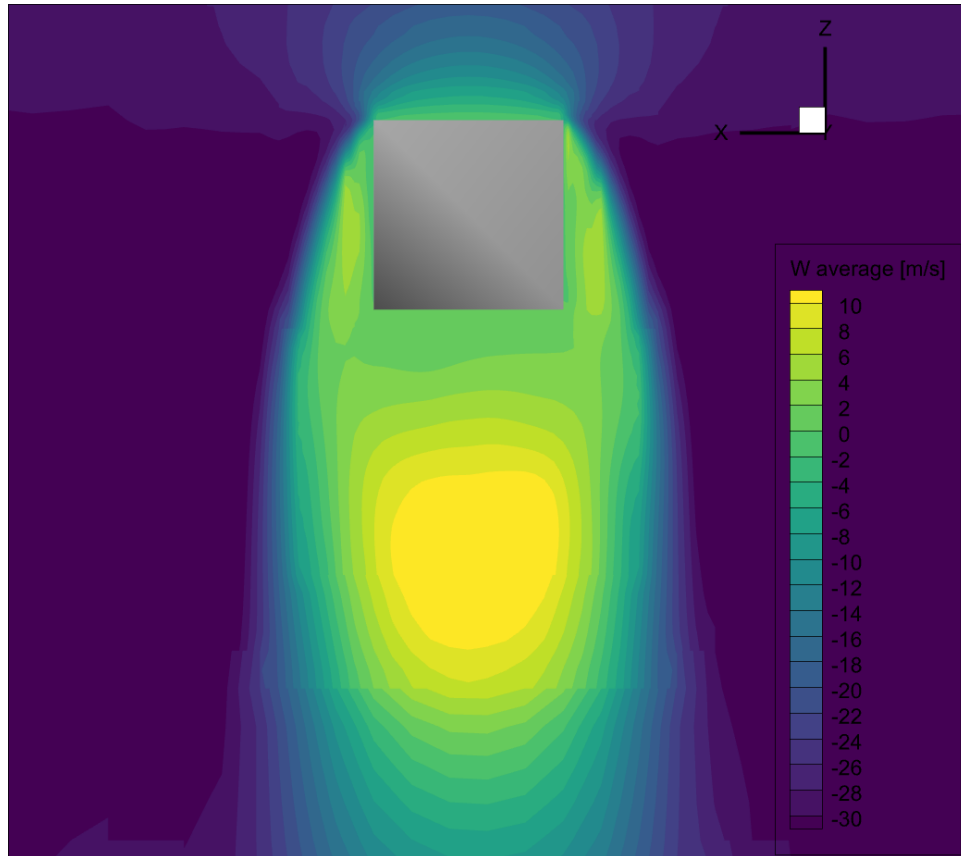


Figure 107: Contour of time-averaged longitudinal velocity for a 30m/s headwind simulation over a 10m cube using CGE/VTM along a midplane at $y = 5$ m (top view).

Aft of the cube, the von Karman streak shown in Figure 83(e) is only observed during *time-accurate* solutions. Contours of *time-averaged* longitudinal velocity instead display a symmetrical distribution aft of the obstacle, as evident in Figure 107. It was surmised that the piece-wise ellipse-based and infinite vortex model used for the backstep separation would be applicable here as well, with the primary difference being the starting height from where the velocity predictions would be made. In the previous section, velocity predictions were made from $\hat{y} = 0$ m given that this coordinate represents the ground surface. The $\hat{y}$ axis is introduced to represent vertical axis used by the model. For the backstep separation considered in the previous section, the vertical axis used by the model corresponded with the CGE/VorTran-M y axis shown in Figure 107, and thus $\hat{y} = y$. In the current side surface separation case, $\hat{y} = x$. Unlike the backstep separation case, no ground is present in the current case, but a symmetry condition exists along the $x = 0$ m plane. From manual tuning, start heights of $\hat{y} = 0.4H$ and $\hat{y} = 0.25H$ for the ellipse-based and infinite vortex models, respectively, were found to produce reasonable agreement with prediction from the CGE/VorTran-M simulation as shown in Figure

109 and Figure 110, where velocity distributions at various span locations for $x > 0$ m (left) are shown. Large discrepancies of approximately 15 m/s are noted at $z = -6$ m in the above core and transition regions, but those are deemed acceptable since it is unlikely that the aircraft would be flying at that location. Offsets between model CGE/ VorTran-M predictions remain ≤ 3 m/s at the remaining span locations. Similar results are obtained for the $x < 0$ m (right) region (not shown for brevity). With regards to parametrization, the results presented for the side flow separation are promising in that we were able to obtain adequate predictions for this flow feature by simple modifications to the inputs of the analytical vortex models for the backstep flow separation.

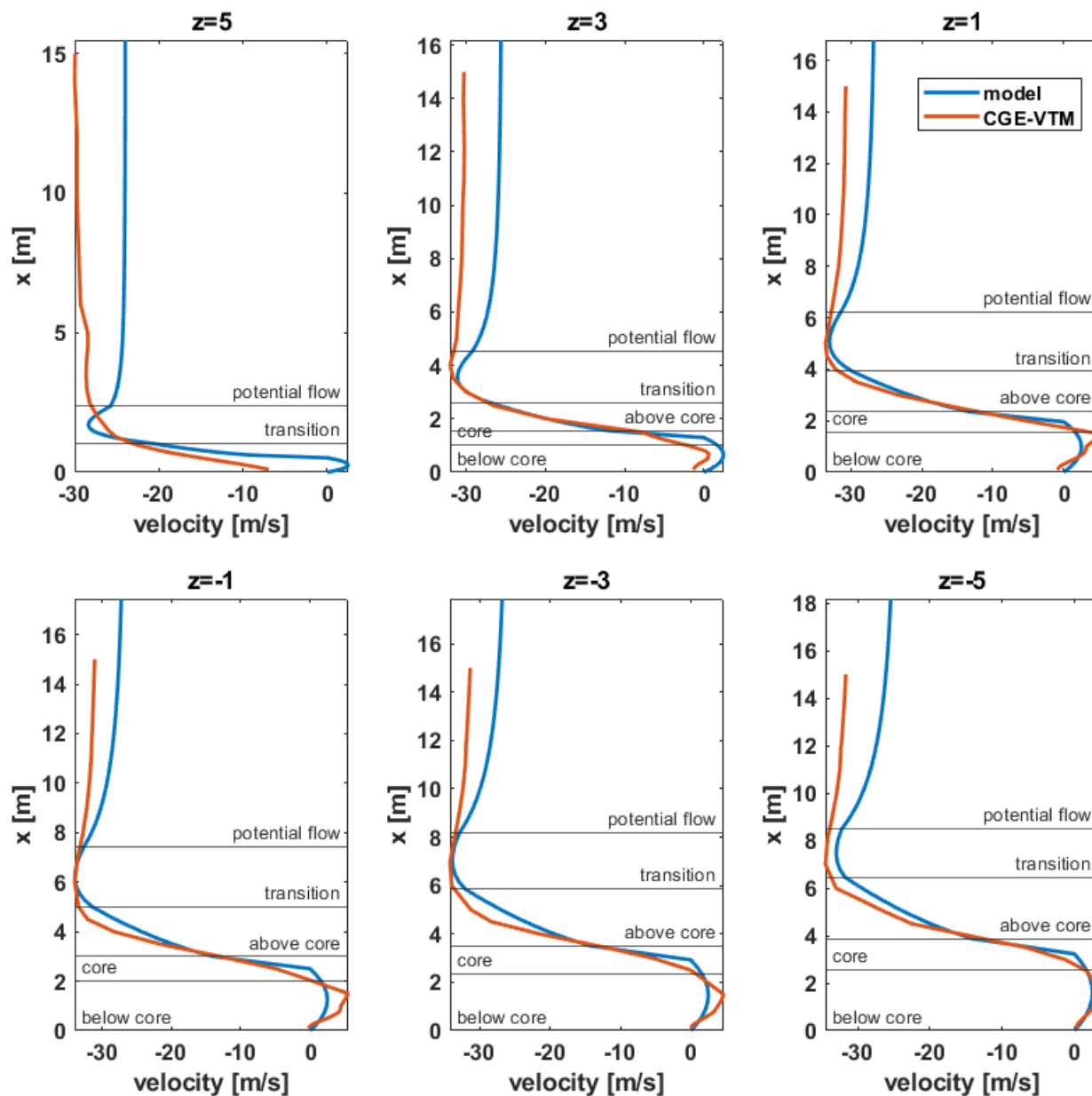


Figure 108: Longitudinal velocity profile over left side surface of cube ($x = 5$ m); x distance plotted relative to surface; CGE data queried in 0.1 m increments at every span location.

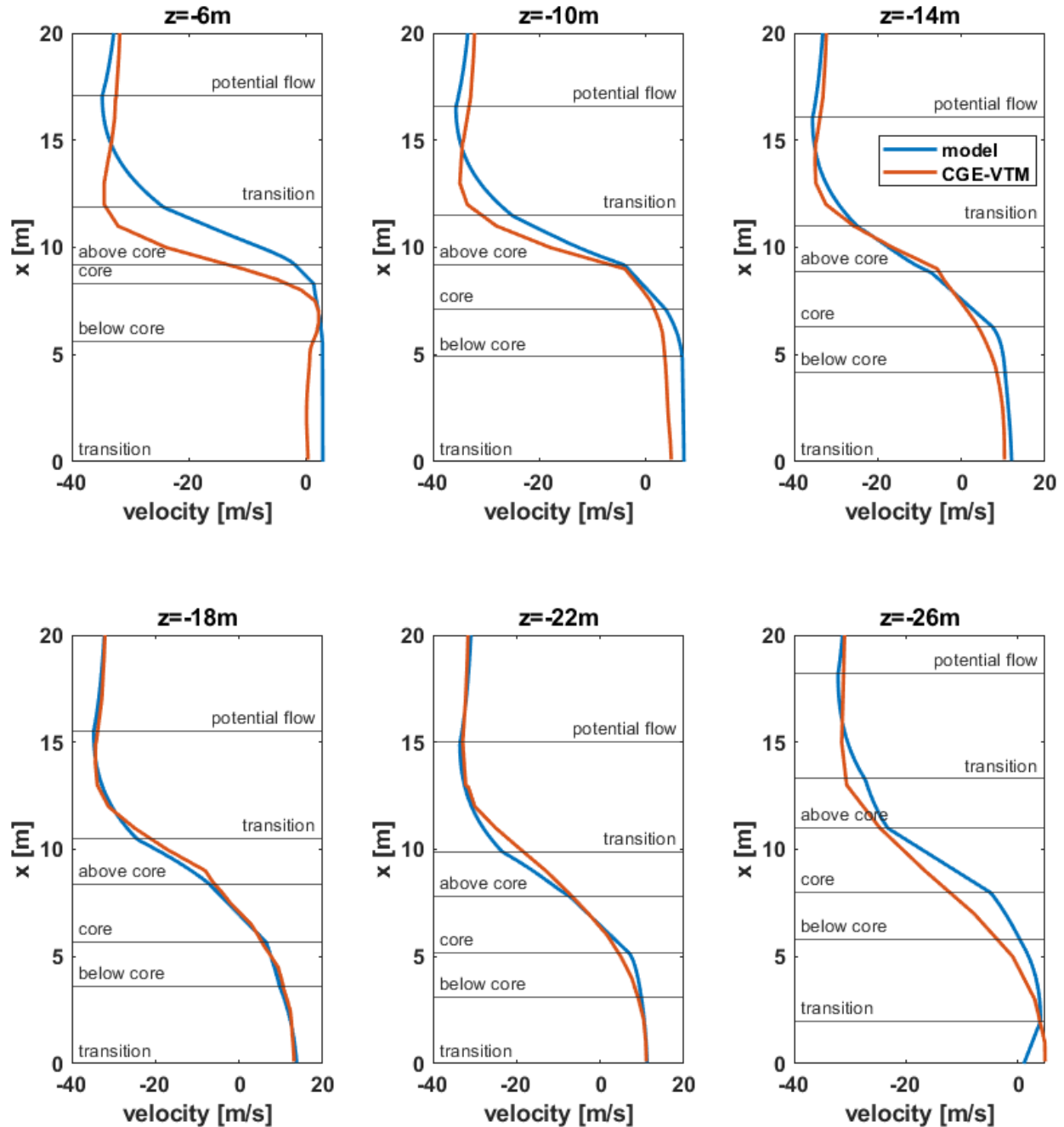


Figure 109: Longitudinal velocity profile at various span locations aft of the obstacle for $x > 0$ m along the $y = 5$ m plane using the slanted ellipse-based vortex model for $-6 \text{ m} \geq z > -26 \text{ m}$ and the infinite vortex model for $z = -26 \text{ m}$; CGE/VorTran-M data queried in 0.1 m increments at every span location.

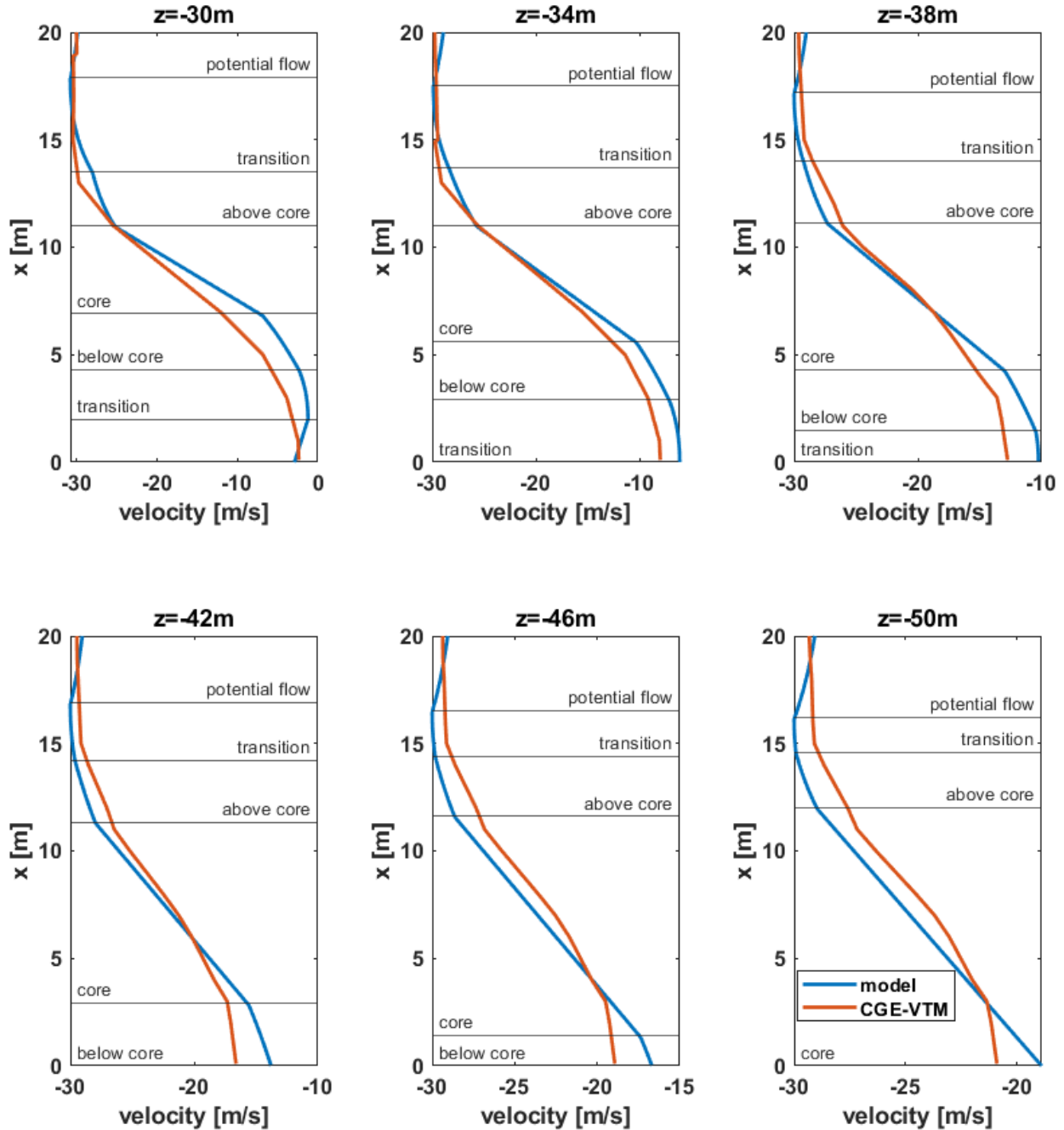


Figure 110: Longitudinal velocity profile at various span locations in the $-30\text{ m} \geq z \geq -50\text{ m}$ range aft of the obstacle for $x > 0\text{ m}$ in the $y = 5\text{ m}$ plane using the infinite vortex model; CGE/VorTran-M data queried in 0.1 m increments at every span location.

Corner flow separation modeling

For headwind flow conditions, edge vortices develop due to pressure gradients between side and top surfaces of the obstacle as illustrated in Figure 83(f). These edge vortices closely resemble roof corner vortices that the model was derived for, and thus, the original model equations shown in Table 2 were found to be applicable. Model inputs consist of the (1) the vortex radius (h), (2) the location of point M (R_M), and (3) the velocity at point M (U_M). Model

parameter values and the corresponding velocity profile are shown in Table 7 and Figure 111, respectively, for the dashed 45° line shown in Figure 112. Good agreement is noted between the model predictions and those from CGE/VorTran-M.

Table 7: Vortex model parameter values.

Parameter	Value
h (m)	1.5
R_M (m)	6.0
U_M (m/s)	-32.8

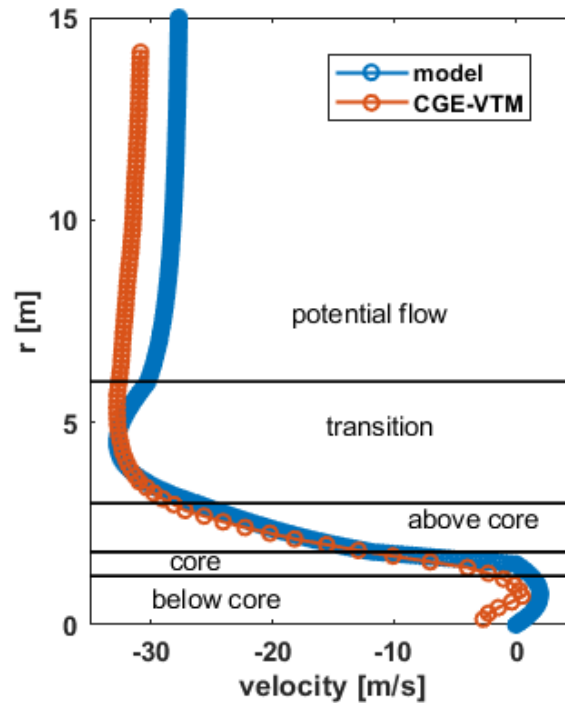


Figure 111: Longitudinal velocity profile along a 45° radial line from the corner of the cube.

In a similar manner to the top surface separation, the 1D prediction along the 45° line was extended to cover the entire 2D region over the surface ($-5 \leq z \leq 5$ m). Here however, a function for the radial location of the vortex center $r_0(z)$ was also derived in addition to functions for the vortex radius $h(z)$, location of vortex rotation point $R_M(z)$, and velocity $U_M(z)$, which represent standard inputs of the vortex models:

$$h(\bar{z}) = 0.07\bar{z} + 1, \quad (16)$$

$$R_M(z) = 0.24\bar{z} + 3.2, \quad (17)$$

$$V_M(\bar{z}) = -0.0014\bar{z}^2 + 0.251\bar{z} + 1.004, \quad (18)$$

$$r_0(\bar{z}) = \min[-0.024\bar{z}^2 + 0.456\bar{z} - 0.46, h(\bar{z})] \quad (19)$$

where $\bar{z} = z_{LE} - z$, and where the leading-edge span location $z_{LE} = 5$ m. From Eq. (19), the vortex center position can result in the vortex overlapping with the obstacle but is constrained to

always being in contact with the edge. That is, there cannot be a gap between the vortex and the edge. This definition allowed for behavior at the $z = 5$ m (leading edge) location to be adequately captured, as evident in Figure 113. At this location, the vortex center is located “inside” the obstacle along the 45° line. As flow progresses further downstream over the obstacle, both the location of the vortex center and vortex size increase radially in Figure 113, in a manner that parallels the expected physical behavior of the corner edge vortices. The model thus allows for physical insights into the flow phenomenon. Note that leading edge flow profiles for top and side surfaces separations can also potentially be captured by introducing $r_0(z)$ as an additional input to the fitting models.

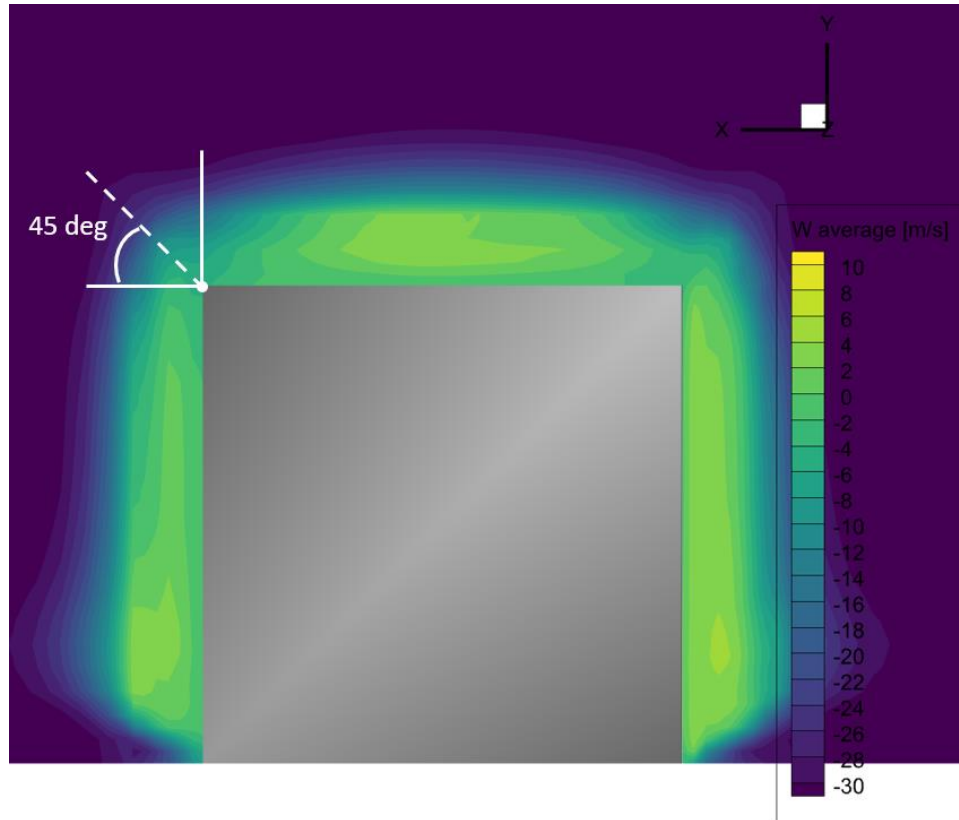


Figure 112: Contour of time-averaged longitudinal velocity for a 30 m/s headwind simulation over a 10 m cube along $z = 0$ m plane; dashed line represents line along with corner vortex model fitting was initially performed.

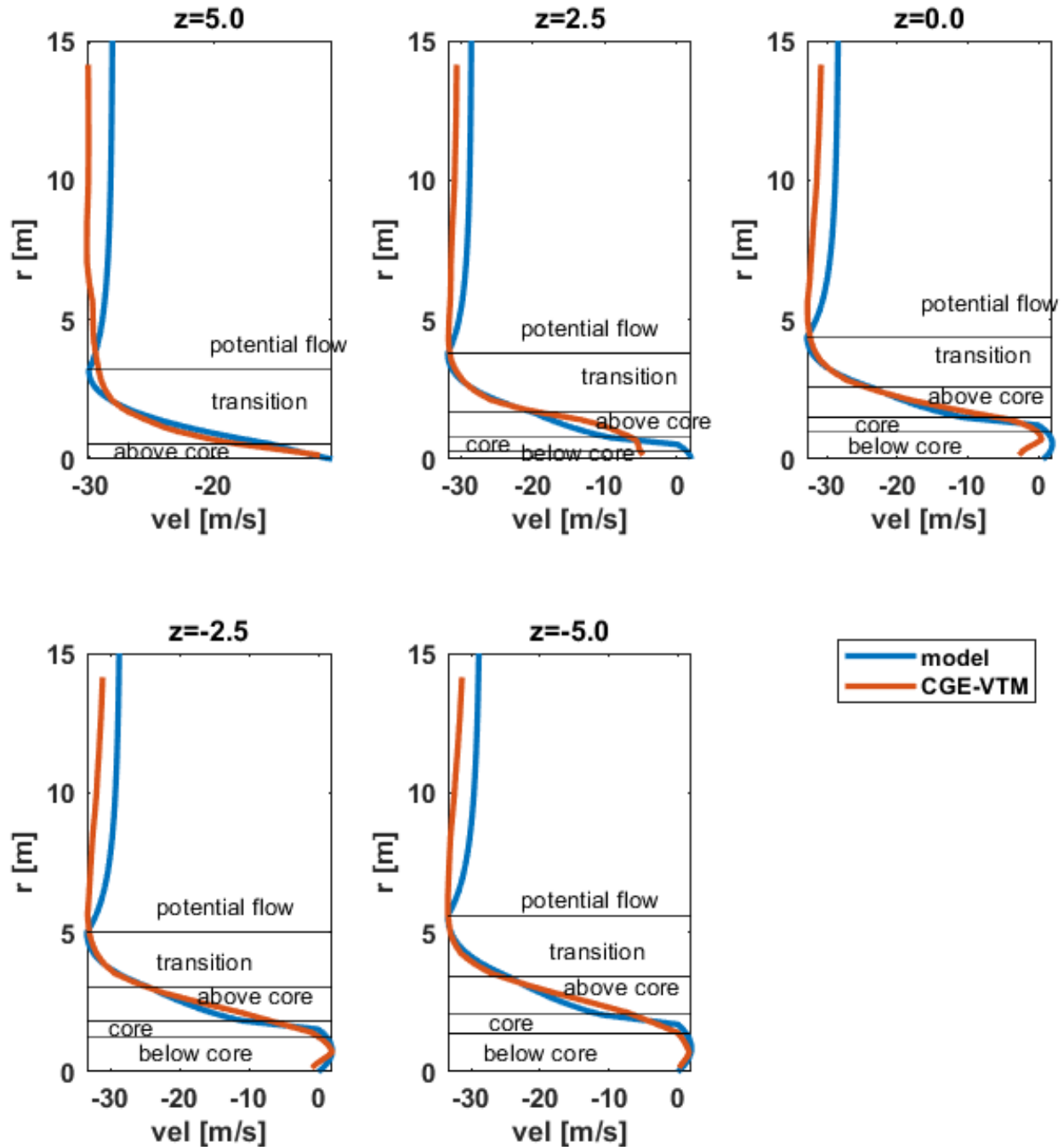


Figure 113: Longitudinal velocity profile along a 45° radial line from the corner of the cube at various span locations over the obstacle; CGE/VorTran-M data queried in 0.1 m increments.

3D Longitudinal Velocity Field Reconstruction

As described earlier, the analytical models adequately captured time-averaged longitudinal velocity profiles across the three planes. We performed 3D reconstruction of the longitudinal velocity profile to further test the viability of the models. We generated a database of position coordinates and corresponding velocities along the three planes and used MATLAB's

`scatteredInterpolant`⁶ capability to “blend” the velocities for 3D reconstruction. Provided a 2D or 3D scattered dataset, this MATLAB function returns a function handle that outputs interpolated values for user specified position coordinates. This capability supports several interpolation and extrapolation methods. In this work, we employ the `linear` method for interpolation and extrapolation.

To evaluate the parametric model velocity field reconstruction, predictions were made along the transverse plane shown in Figure 114. Corresponding results are shown in Figure 115 and show good agreement between the model predictions and CGE/VorTran-M data. The discontinuities noted in the analytical contours represent the planes along which the 2D model fitting was performed. Note that an additional horizontal plane at $y=3\text{m}$ was employed to obtain the results shown in Figure 115. These results further confirm the viability of the analytical models.

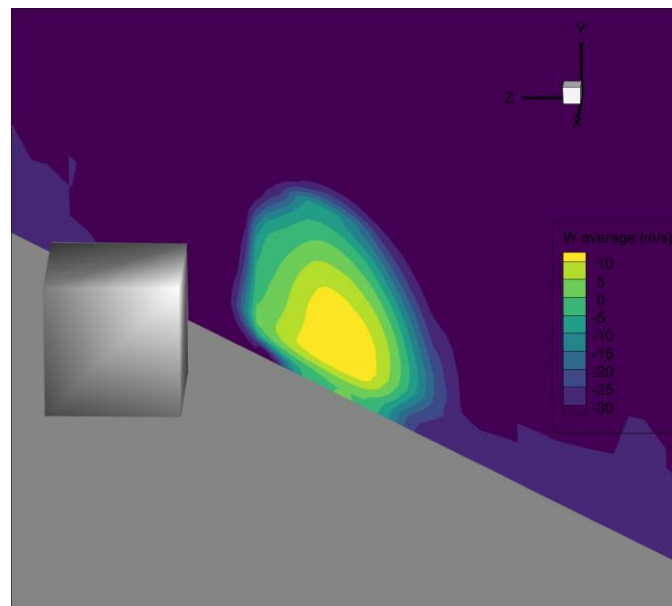


Figure 114: Transverse plane considered in parametric model evaluation.

⁶ <https://www.mathworks.com/help/matlab/ref/scatteredinterpolant.html>

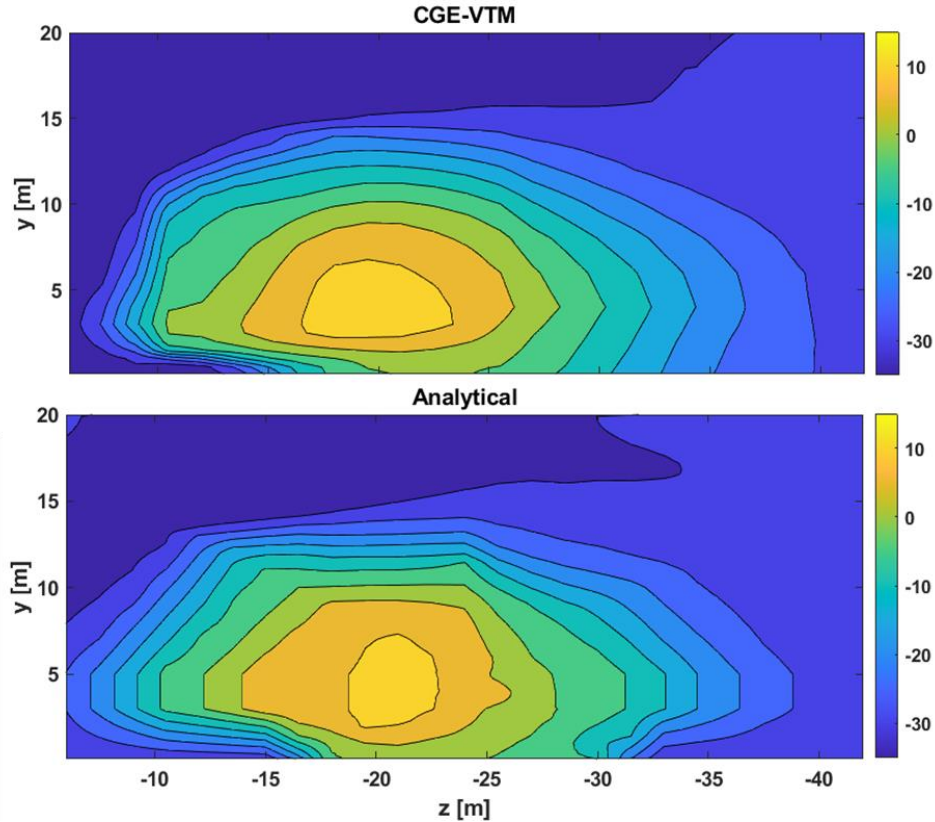


Figure 115: Mean longitudinal velocity contours along transverse plane; truth (top) and parametric model (bottom).

Automation of Parameter Extraction

The 3D velocity reconstruction results presented in the previous section gave confidence in the canonical airwake modeling approach. Work was performed to overhaul the initial model implementation scripts, which were based on manual tuning, and replace them with modular parameter extraction scripts that automatically determine model fitting parameters via an optimization procedure using MATLAB's `fmincon` function. The optimization problem formulation consisted of varying model input parameters to minimize the error between truth and 1D model prediction data subject to lower and upper bounds on parameter values. To expand the predictions to 2D planes (for instance, along the top surface region of the midplane), an outer level script was written to automatically perform optimization-based parameter extraction at multiple span locations (along the z axis) to obtain polynomial functions for the model input parameter variation. To further expand the predictions to 3D, an additional script that performed automatic model fitting along a transverse (x axis) plane was implemented. This script was used to determine polynomial functions for *changes* in model parameters. As such, longitudinal velocities along the vertical line through any position coordinate on the top surface obstacle could be determined using model input parameters obtained as follows:

$$X = X_L + \Delta X_T, \quad (20)$$

where X is a generic variable to represent a model input parameter, X_L represents the value of the parameter obtained using the spanwise z coordinate polynomial functions, and ΔX_T represents

the change in X_L as function of the transverse x coordinate. The scripts are modular such that model input parameters for 1D, 2D, and 3D spatial domains can be readily setup and obtained.

Vertical Velocity Fitting

Vertical velocity profiles are particularly important for the ship-helicopter dynamic interface given that this velocity component directly influences rotor inflow, which, in turn, affects rotor loads and consequently, aircraft flight dynamics and handling qualities. Initial tests were performed using the modified Banks and Meroney model described above. However, the model was not globally applicable to all locations in the domain. We thus devised a custom piece-wise analytical model for the fitting consisting of first and second order polynomials. This is depicted in Figure 116, which shows a typical vertical velocity profile and the four regions where polynomial fitting is performed. A linear fit is employed in regions #1 and #3 and a second order fit in regions #2 and #4. Model input parameters consist of:

1. velocity near ground (v_0);
2. location where region #1 ends and region #2 starts (r_1);
3. velocity at r_1 (v_1);
4. location where region #2 ends and region #3 starts (r_2);
5. velocity at r_2 (v_2);
6. velocity in the middle of region #2 (v_{2m});
7. location where region #3 ends and region #4 starts (r_3);
8. velocity at r_3 (v_3);
9. location where region #4 ends (r_4);
10. velocity at r_4 (v_4); and
11. velocity in the middle of region #4 (v_{4m}).

Model fitting results for the vertical velocity profile over the top surface along the midplane are shown in Figure 117. We used the automated scripts to determine the optimal parameters. Note that velocities near the ground are not necessarily zero. This is because CGE/VorTran-M enforces the no-penetration boundary condition at walls by setting the convective flux, not velocity, through the cell face corresponding to the wall to zero. The model shows good agreement with CGE/ VorTran-M results. Regions #3 and #4 are the primary ones present closer to the leading edge at $z=5\text{m}$ and $z=2.5\text{m}$. As we proceed further downstream, region #2 emerges indicating the presence of recirculation. This is evident from the slight negative vertical velocities noted at $z=-2.5\text{m}$ in the $y \in (0,2]\text{m}$ range and further confirmed from the Tecplot contour plot of time-averaged vertical velocity and shown in Figure 118.

Similar results were obtained for the vertical velocity profile on either side and along the diagonal edge of the obstacle, as well as aft of the obstacle. We were also able to successfully reconstruct 3D velocity profiles using MATLAB's reconstruction utilities as describes for the time-averaged longitudinal velocity component (results not shown for brevity).

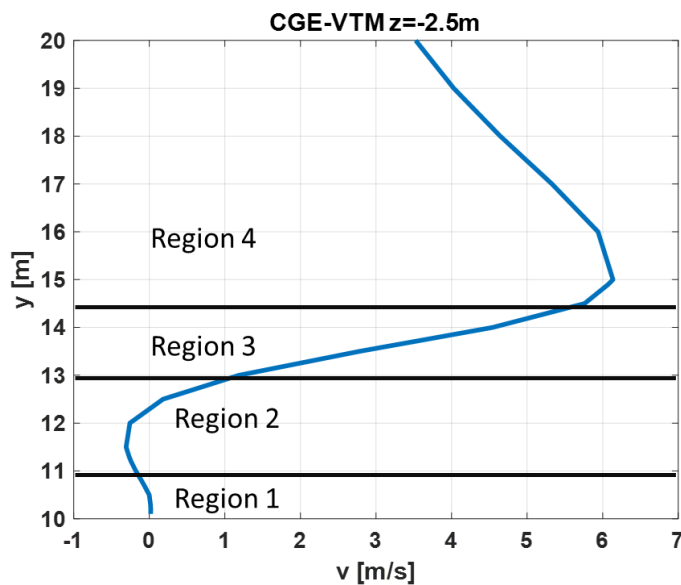


Figure 116: Typical vertical velocity profile from CGE/VorTran-M highlighting the four regions considered in the fitting model.

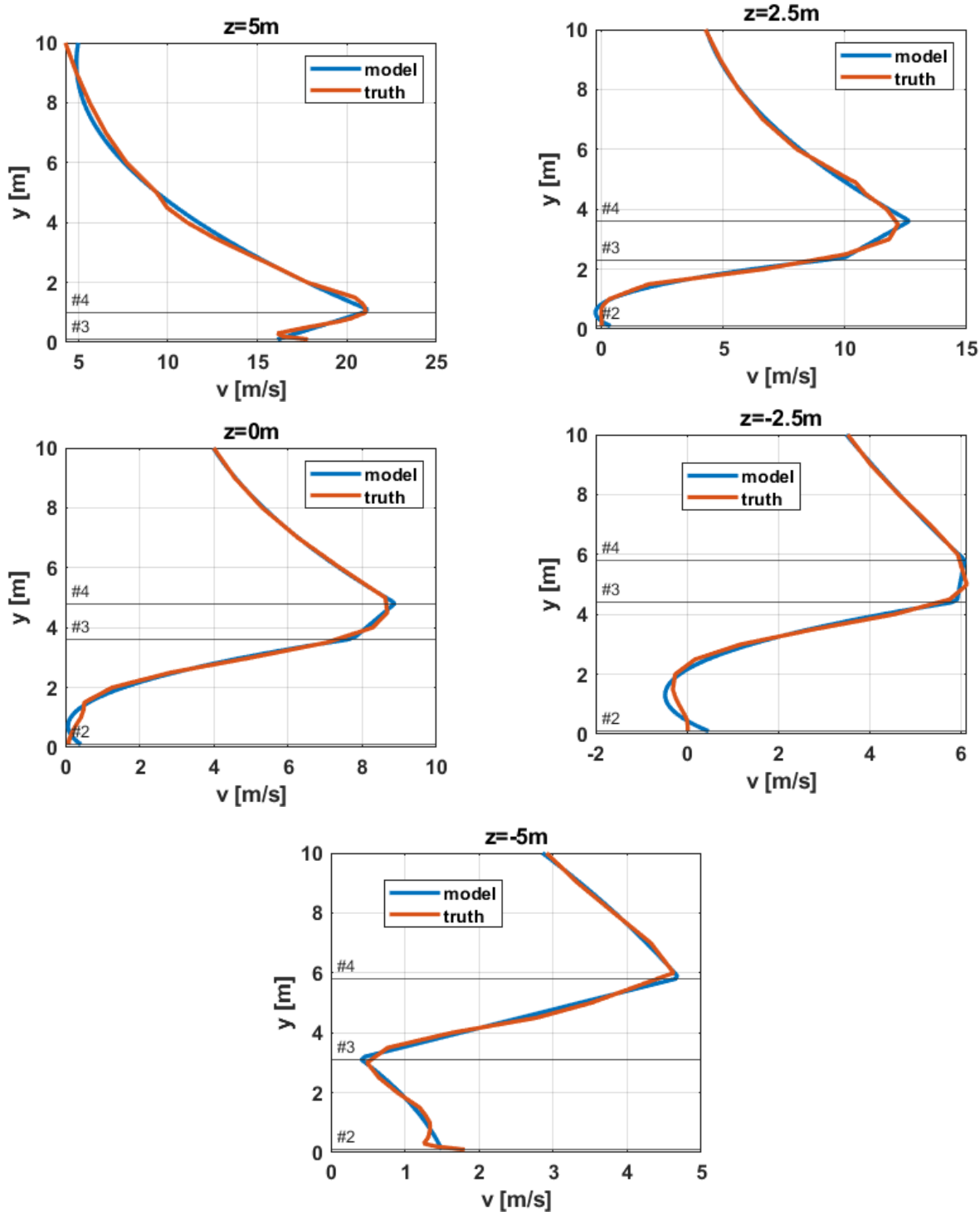


Figure 117: Time-averaged vertical velocity over top surface along $x=0$ m plane.

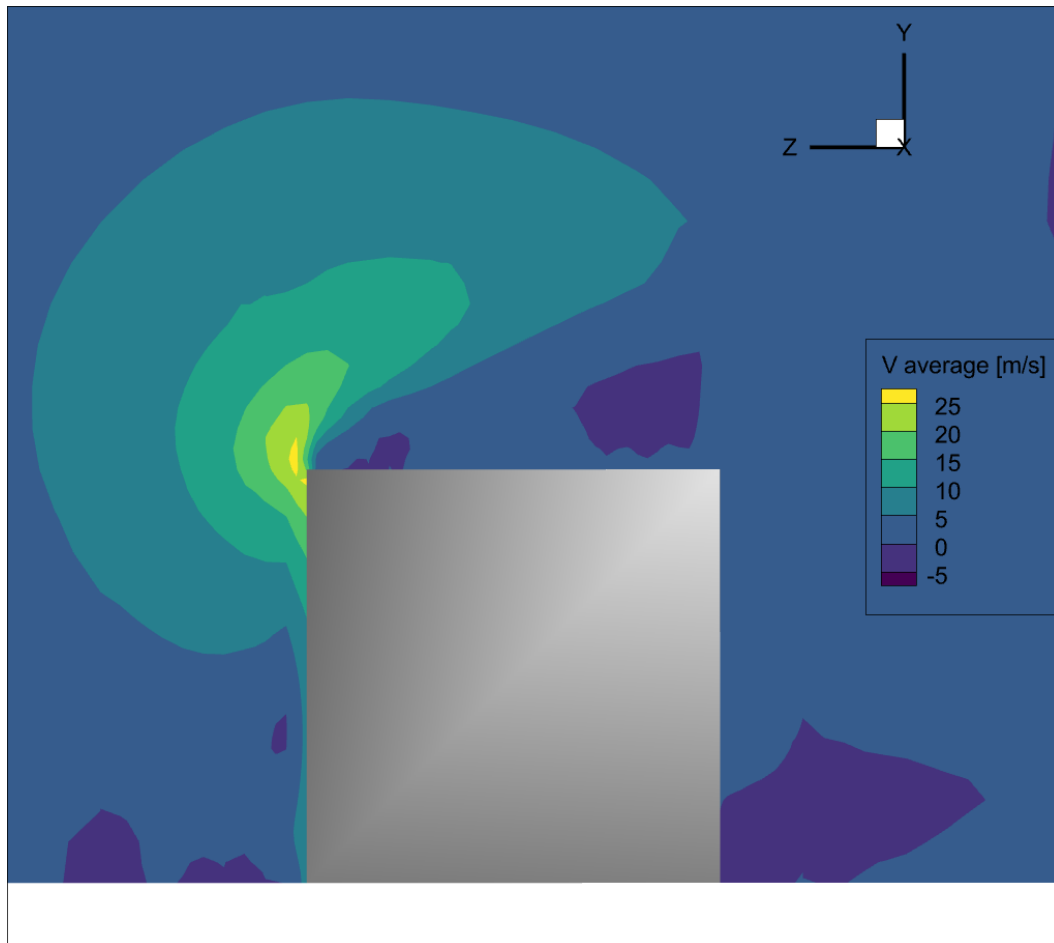


Figure 118: Time-averaged vertical velocity contour along $x=0m$ plane from Tecplot.

Dynamic Characterization

A spatiotemporal characterization of the flow features associated with dynamic interface operations is critical for a better understanding of the ship-helicopter dynamic interface. Although this fact is generally accepted, work in literature has typically focused on the identification of coherent structures with limited characterization of the time dynamics associated with them. Spatiotemporal characterization of fluid dynamics is typically performed using modal decomposition methods that attempt to represent unsteady flows with random fluctuations as superpositions of deterministic basis vectors or "modes" that can provide better insights into flow behavior [91]. Two popular methods are the proper orthogonal decomposition (POD) [91-94] and dynamic modal decomposition method (DMD) [95-98].

The POD method follows the classical separation of variables approach to solving partial differential equations (PDEs) and represents quantities of interest (e.g., velocity) as a superposition of spatial basis vectors and corresponding temporal coefficients. Emphasis is typically placed on determining the orthonormal spatial basis functions that are computed using the spatial covariance matrix of quantities of interest (e.g., velocity, pressure) [91]. The temporal coefficients are determined from the spatial basis vectors and provided quantity data. The covariance matrix indicates how a change in a quantity of interest at any given spatial location is associated with changes in the rest of the domain. That is, it gives a measure of data correlation,

which, also gives a measure of turbulent kinetic energy (TKE) when the quantity of interest is velocity [91]. The TKE is a metric used to quantify flow turbulence and is defined as [53]

$$TKE = \frac{1}{2} (\overline{u'^2} + \overline{v'^2} + \overline{w'^2}), \quad (21)$$

where, $\overline{u'}$, $\overline{v'}$, $\overline{w'}$ represent average fluctuating longitudinal, lateral, and vertical velocity components,

$$\overline{u'} = \frac{1}{T} \int_0^T (u(t) - \bar{u}) dt, \quad (22)$$

and where $u(t)$ is the instantaneous velocity and $\bar{u}$ is the average velocity at the location. The $\overline{v'}$, and $\overline{w'}$ terms have similar definitions. The spatial modes obtained using POD are orthonormal and each have different TKE levels associated with them. The most energetic ones can be further examined to identify primary coherent structures responsible. The POD method is considered an energy decomposition method [93]. A downside of the method, however, is that each mode can be associated with a wide range of time scales (frequencies), making a proper spatiotemporal characterization of flow features a nontrivial task.

The DMD method on the other hand, is a frequency decomposition method where each resulting mode is associated with one unique frequency, thus providing information on both the length- and time scales of coherent structures [93, 95, 97]. In this method, nonlinear systems are mapped onto a linear space (generally with infinite-dimension) using a linear operator. The formulation is such that eigenvalue decomposition of the linear operator allows for the identification of coherent structures in the flow using the DMD modes, and their growth/decay rates and frequencies using the eigenvalues. Note that the DMD modes are different from the eigenvectors obtained from eigenvalue decomposition [95, 97]. The DMD modes are not ranked by energy levels and are not orthonormal. Since both spatial *and* temporal aspects are considered in the DMD characterization, various criteria have been proposed to identify the dominant DMD modes, each with varying performance for given flow configurations [95]. This method is generally preferred for flow reconstruction of periodic system since it places emphasis on spectral purity of the decomposition (distinct frequencies for each mode). For unsteady, nonlinear systems, frequency modulations, phase jitter and other localized frequency events are not readily captured. Moreover, the large number of modes required to capture major flow dynamics in the time domain often leads to redundancy in the structure of the spatial domain [93].

Given the limitations of POD and DMD methods, several hybrid methods have been developed to characterize flow dynamics in terms of energy content and spectral purity. In this work, we employ a multi-resolution POD method proposed based on the recent work by Zhu et al. [94], where frequency ranges or "bins" that contain the most energetic features are first identified, before performing POD to identify the associated coherent structures. Resolving the energetic features into frequency bins allows for a more intricate spatiotemporal characterization of flow dynamics, when compared to the traditional POD method, as the frequency ranges are typically narrow and more specific to coherent structures.

The multi-resolution POD approach is described next, followed by initial results obtained. The quantities of interest in this work will be the longitudinal and vertical velocity components.

Multi-Resolution Proper Orthogonal Decomposition

Several coherent structures spanning a range of length and time scales exist in typical ship airwake. For the ship-helicopter DI, the most energetic coherent features are of interest since these influence aircraft flight dynamics and handling qualities. The multi-resolution POD method isolates these coherent features by identifying and analyzing airwake data only within frequency ranges where these energetic features exist [94]. There are three primary steps involved: (a) identify the most energetic features and the N_{bin} frequency bins in which they exist, (b) filter the original data using bandpass filters to generate N_{bin} datasets corresponding to the ranges of each frequency bin, and (c) perform POD of the individual filtered datasets to identify the primary coherent features within each frequency range. This approach allows for a more intricate characterization of the spatial *and* temporal scales of the most energetic features of the airwake.

The method begins with arranging CFD (or experimental) velocity data into a 2D matrix as follows [94]

$$D(x, t) = \begin{bmatrix} d(x_1, t_1) & d(x_1, t_2) & \cdots & d(x_1, t_{N_t}) \\ d(x_2, t_1) & d(x_2, t_2) & \cdots & d(x_2, t_{N_t}) \\ \vdots & \vdots & \ddots & \vdots \\ d(x_{N_v \cdot N_s}, t_1) & d(x_{N_v \cdot N_s}, t_2) & \cdots & d(x_{N_v \cdot N_s}, t_{N_t}) \end{bmatrix}, \quad (23)$$

where each quantity d represents a mean-subtracted flow velocity at a given spatial location x and instant in time t . There are N_t time steps (uniform discretization assumed), N_v velocity components and N_s spatial locations. In this work, longitudinal and vertical velocities will be considered individually such that only one velocity component is included in the data matrix D . The order of the spatial discretization as vectors in the data matrix is not exclusive. The procedure of obtaining the vectoral discretization from the physical space should be reversed when interpreting results from the POD [94]. The next step is to take the fast Fourier transform (FFT) in time for each row of the data matrix

$$\widehat{D}(x, \omega) = \text{FFT}\{D(x, t)\} \quad (24)$$

and subsequently compute the cross-spectral density matrix

$$\widehat{K} = \frac{1}{N_t} \widehat{D}^* \widehat{D}, \quad (25)$$

where “*” indicates conjugate transpose. Each element on diagonal of matrix $\widehat{K}$ represents the TKE at given frequency [94]. Half of the values correspond to positive frequency values and the other half to negative frequencies, evident from symmetry in the diagonal values. The TKE values can be plotted to determine the N_{bin} frequency ranges or bins where highly energetic flow features are present. These are readily identified from the peaks in the plot.

The next step is to filter⁷ the initial data matrix D from Eq. (23) row by row to generate N_{bin} data matrices, which are then analyzed using POD. The POD formulation diagonalizes the data matrix as follows

$$D = \Phi \Sigma \Psi^*, \quad (26)$$

⁷ In this work, we employ a bandpass filter derived from the one presented in <https://tomroelandts.com/articles/how-to-create-simple-band-pass-and-band-reject-filters>

where the columns of Φ contain the orthonormal spatial basis functions, Ψ describes the temporal evolution of the data, and Σ is a diagonal matrix with each entry σ providing a measure of the relative TKE of each spatial mode [94]. The most energetic modes can be identified by plotting the relative TKE contribution α_i of each mode

$$\alpha_i = \frac{\sigma_i}{\sum \sigma_i}. \quad (27)$$

The corresponding spatial modes can then be plotted to identify the primary coherent structures contributing to the TKE. For a more detailed and practical explanation of the POD method, the reader is directed to Ref. [91].

Results

The multi-resolution POD approach was employed for the dynamic characterization of flow features using longitudinal and vertical velocity data aft of the obstacle along the midplane.

Dynamic Characterization Along $x=0\text{m}$ Plane: Longitudinal Velocity

The approach laid out in the previous section was used to determine the cross spectral-density and the TKE associated with each frequency in data. The resulting normalized TKE values are plotted in Figure 119 as a function of Strouhal number (see Eq. (38)). The freestream speed of 30 m/s and cube height of 10m were used as characteristic velocity and length, respectively. The largest peak is noted for the $St \in (0,0.02]$ bin. Although these St values seem small, similarly small values are reported by Zhu et al. in Ref. [94]. Further investigation remains warranted to ascertain their validity. A bandpass filter was applied to the original data matrix to isolate flow features within this frequency range. The normalized TKE plot obtained from the cross-spectral density matrix of the filtered data is shown in Figure 120, where it is evident that the band-pass filter isolates the frequency bin of interest.

A POD of the filtered data is then performed. The TKE contribution of each mode α_i is plotted in Figure 121. Modes #1 and #2 contribute 58% and 26%, respectively, to the total TKE in that bin. Contour plots of these modes are shown in Figure 122 and Figure 123. From Figure 122, the primary coherent structure in mode #1 is the shear layer that is shed from the top edge of the cube and the associated back step recirculation aft of the cube. Note that flow does not reattach on the top surface of the cube in the simulation considered. The shear layer and aft recirculation contribute to 58% of the TKE associated with flow for $St \in (0,0.02]$. For mode #2, three distinct flow features are noted immediately aft of obstacle in Figure 123: the shear layer between $y=10\text{m}$ and $y=15\text{m}$, a recirculation region immediately below it and a secondary recirculation of opposite sign closer to the ground. The second and third features are likely due to the arch vortex aft of the cube and the ground horseshoe vortex, respectively. The three features contribute to 26% of the TKE associated with flow for $St \in (0,0.02]$.

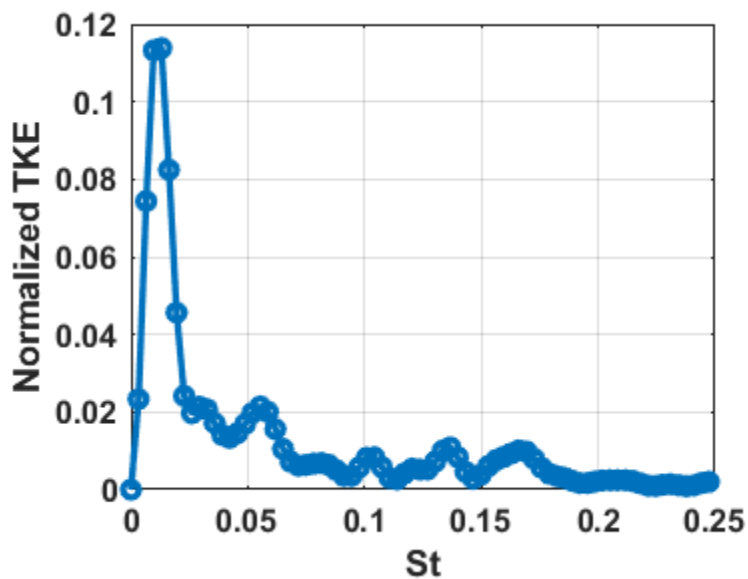


Figure 119: Normalized TKE for longitudinal velocity aft of the obstacle along $x=0m$ plane.

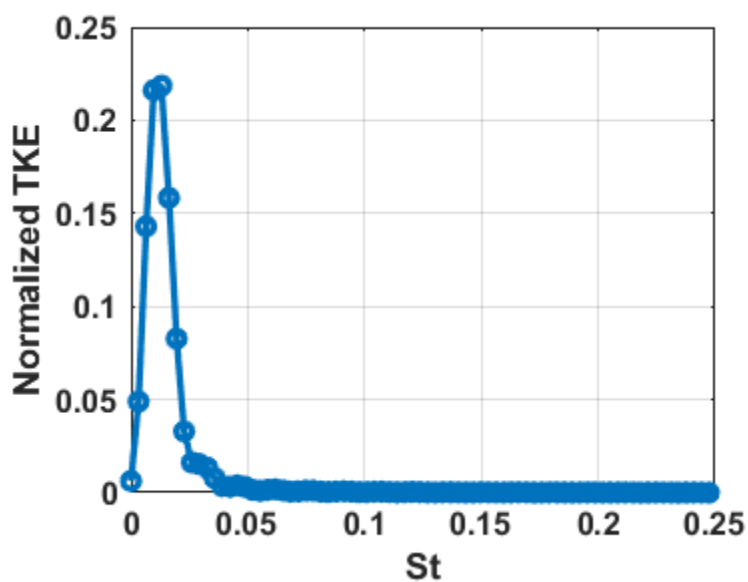


Figure 120: Normalized TKE for filtered longitudinal velocity data aft of the obstacle along $x=0m$ plane.

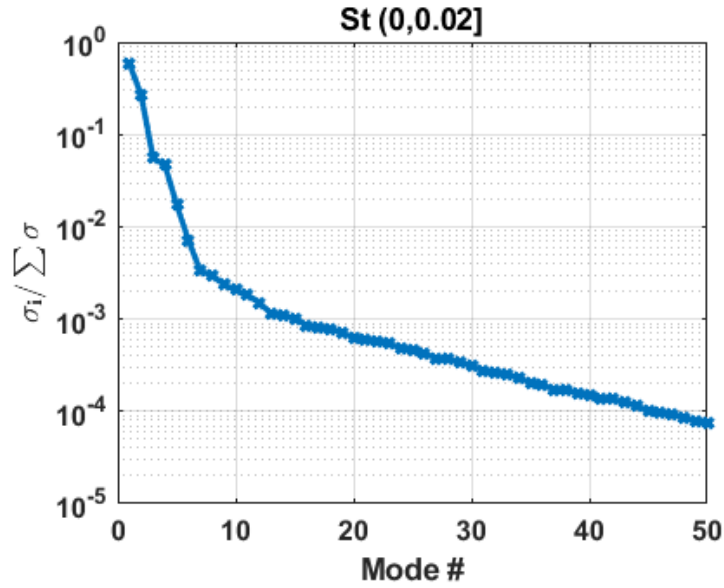


Figure 121: Contribution of each mode to total TKE in $St \in (0,0.02]$ bin; longitudinal velocity aft of the obstacle along $x=0m$ plane considered.

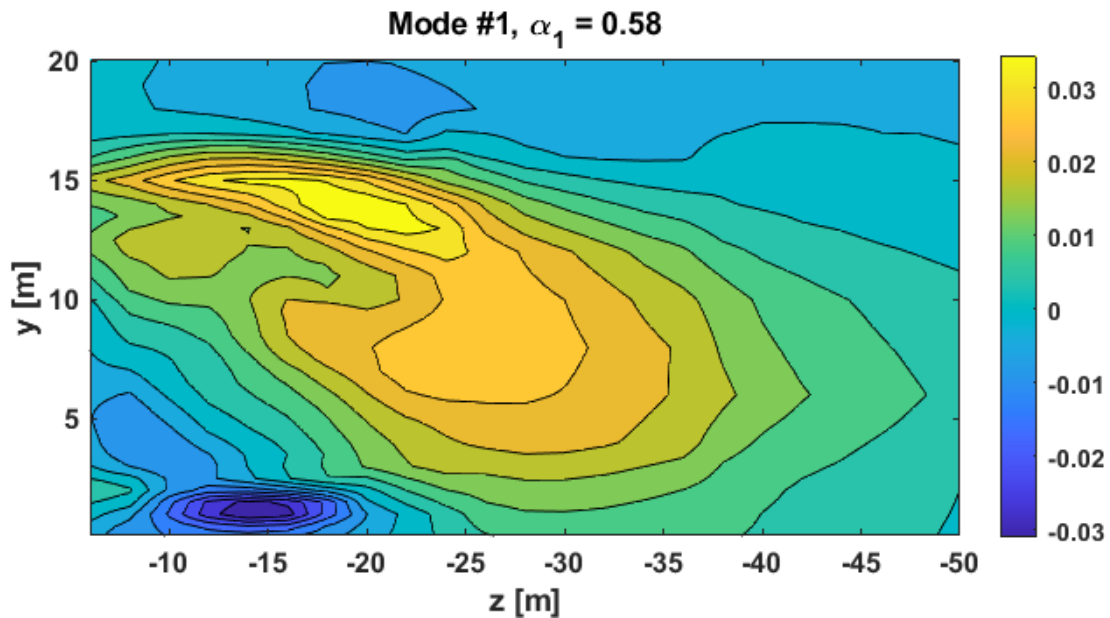


Figure 122: Mode #1 from POD analysis of longitudinal velocity data aft of the obstacle along $x=0m$ plane in $St \in (0,0.02]$ bin.

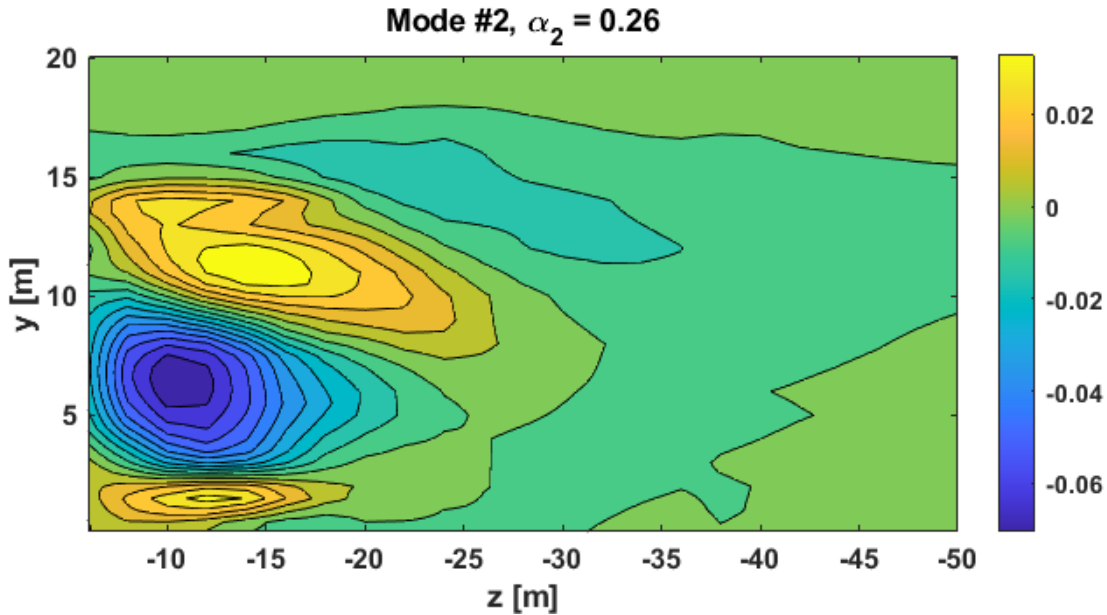


Figure 123: Mode #2 from POD analysis of longitudinal velocity data aft of the obstacle along $x=0\text{m}$ plane in $St \in (0,0.02]$ bin.

Dynamic Characterization Along $x=0\text{m}$ Plane: Vertical Velocity

The vertical velocity component is particularly important for rotorcraft shipboard operations. The normalized TKE values obtained from the cross-spectral density matrix are plotted in Figure 124. Two frequency bins are identified from the figure: $St \in (0,0.03]$ and $St \in (0.03,0.065]$.

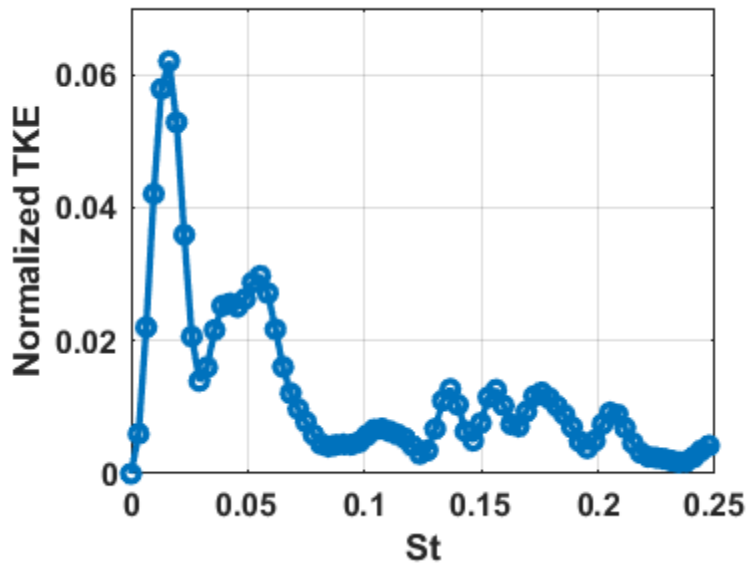


Figure 124: Normalized TKE for vertical velocity aft of the obstacle along $x=0\text{m}$ plane.

For the $St \in (0,0.03]$ bin, the first two modes account for 69% of the total TKE. These modes are plotted in Figure 125 and Figure 126. From Figure 125, it is not immediately clear which flow behavior is captured by mode #1. A time lapse of longitudinal velocity contour is shown in Figure 127. At $t = 5\text{s}$, there is a negative vertical velocity region immediately aft of the cube, evident by the purple oval region seen in the Figure. As we proceed slightly

downstream, we note a positive vertical velocity region, indicated by green shading. As time passes, we note a cyclic alternation in the spatial location of the negative (purple) and positive (green) velocity zones. The contour plot of mode #1 in Figure 125 indicates this flow behavior with the yellow/orange regions indicating part of the shear layer getting entrained by freestream and the blue region indicating the part getting sucked into the aft recirculation. This phenomenon contributes to 36% of the TKE in the $St \in (0,0.03]$ bin. Mode #2, plotted in Figure 126, seems to capture the interaction between the flow associated with the aft recirculation and the top shear layer. This interaction contributes to 30% of the TKE in the $St \in (0,0.03]$ bin.

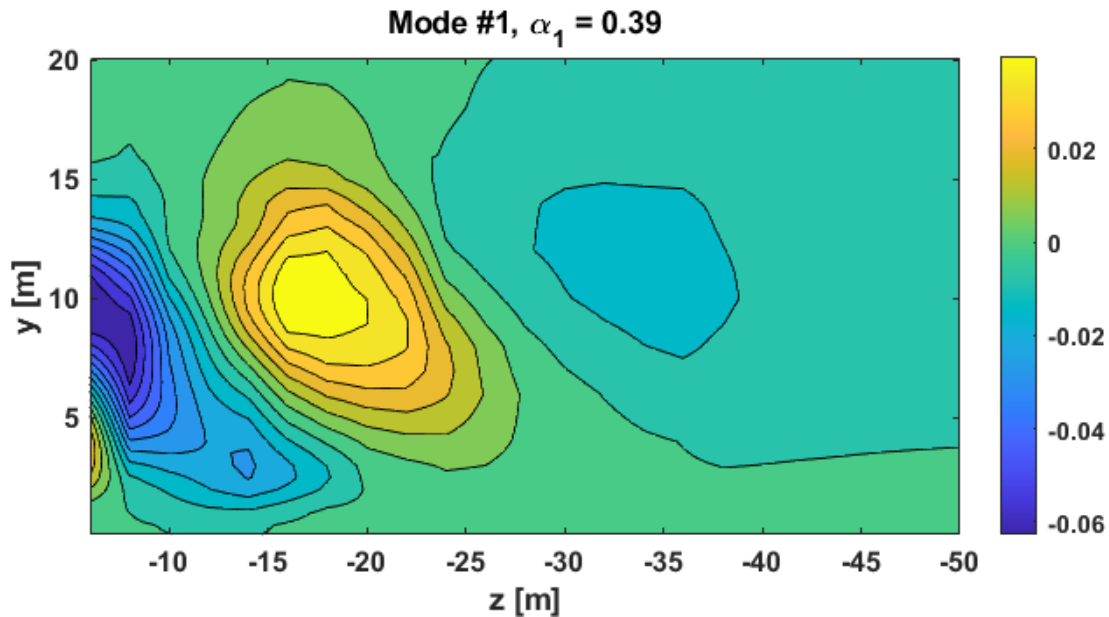


Figure 125: Mode #1 from POD analysis of vertical velocity data aft of the obstacle along $x=0m$ plane in $St \in (0,0.03]$ bin.

For the $St \in (0.03,0.065]$ bin, it was found mode #1 contributed 36% to the total TKE and exhibited a similar structure to the dominant mode from the previous bin shown in Figure 125.

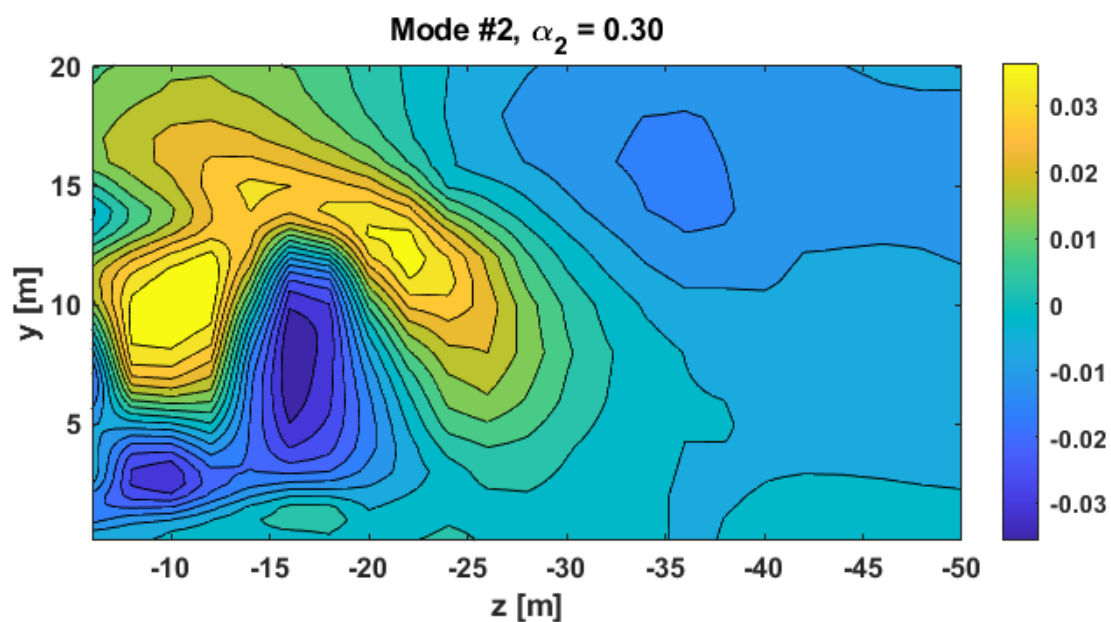
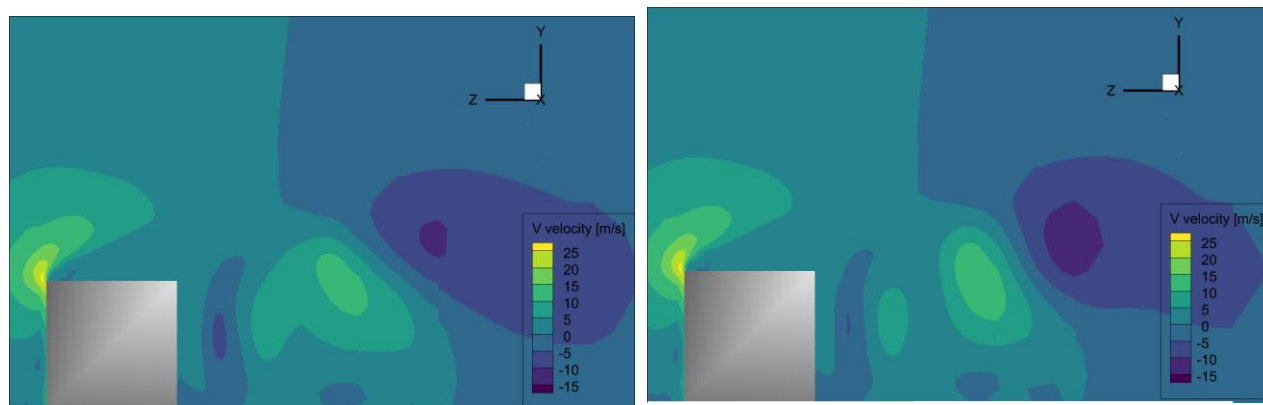


Figure 126: Mode #2 from POD analysis of vertical velocity data aft of the obstacle along $x=0m$ plane in $St \in (0,0.03]$ bin.



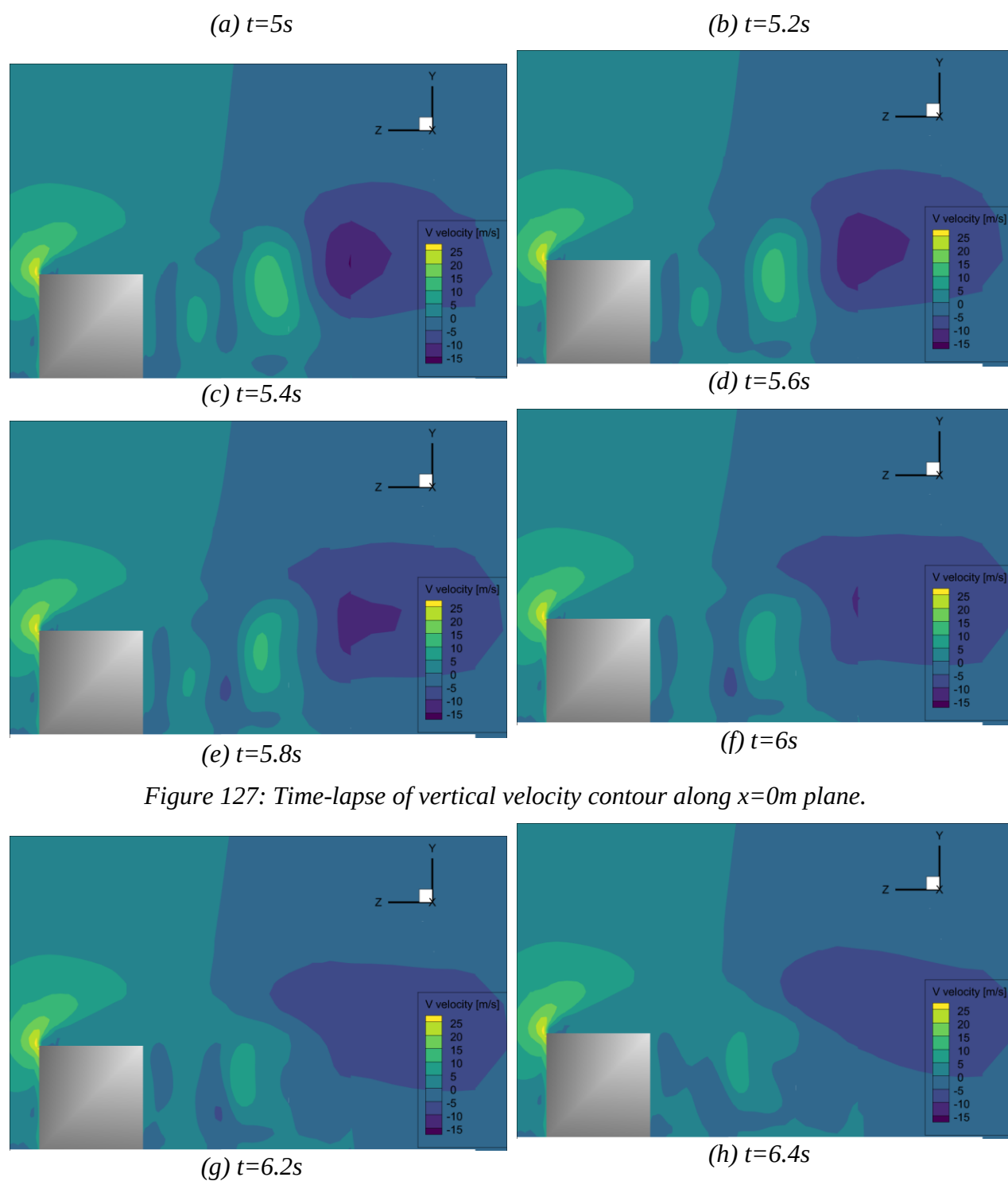


Figure 127: Time-lapse of vertical velocity contour along $x=0m$ plane.

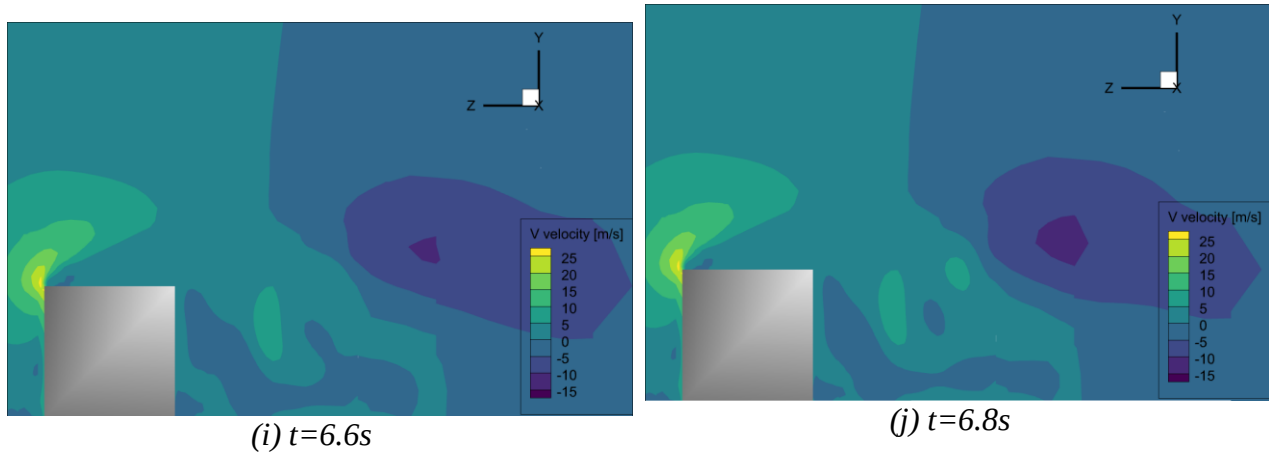


Figure 127: Time-lapse of vertical velocity contour along $x=0$ m plane.

Dynamic Characterization Along $y=5$ m Plane: Longitudinal Velocity

The normalized TKE values obtained using the cross-spectral density matrix from Eq. (25) are shown in Figure 128. The most energetic dynamic flow features occur in the $St \in (0.065, 0.1]$ range. A filtered data matrix isolating flow features in this bin was generated and examined using POD. The first two modes contribute the most to the overall TKE in that bin. Mode #1 is plotted in Figure 129. This mode captures the characteristic bistability of flow observed aft of back steps where flow switches intermittently between portside and starboard locations [6,12]. This flow bistability contributes to 38% of the TKE in the $St \in (0.065, 0.1]$ bin. Mode #2 displays the onset of the bistability immediately aft of the cube in Figure 130, where the larger alternating flow features seen in mode #1 are broken up into contributions from the shear layer on either side obstacle and associated recirculation regions. Mode #2 contributes to 27% of the TKE in the $St \in (0.065, 0.1]$ bin. Note that Zhu et al. [94] identify the bistable flow feature in the $St \in (0, 0.06]$ bin in their experimental study, which considered three headwinds (15.2, 45.7 and 61 m/s) over $1/90^{\text{th}}$ scale model of the SFS2 ship geometry.

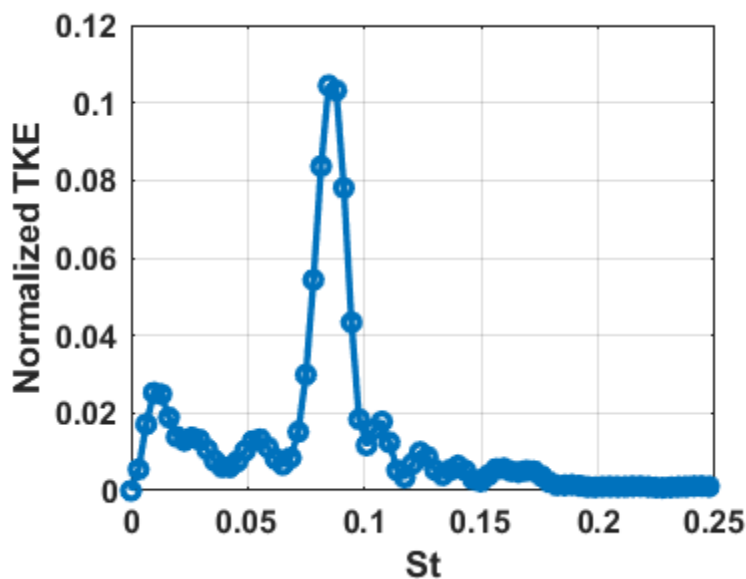


Figure 128: Normalized TKE for longitudinal velocity aft of the obstacle along $y=5$ m plane.

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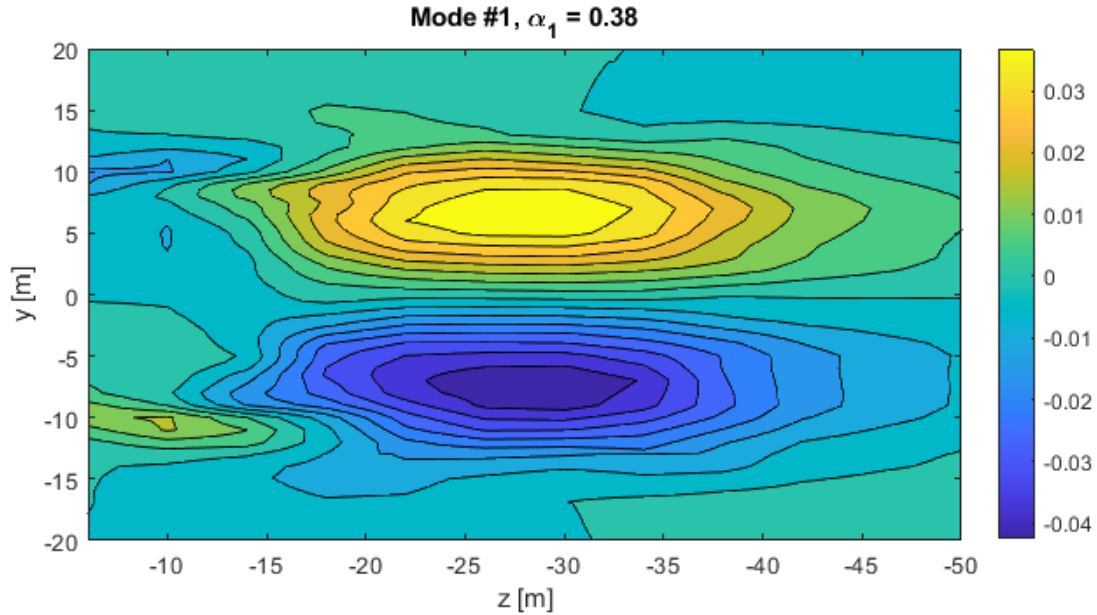


Figure 129: Mode #1 from POD analysis of longitudinal velocity data aft of the obstacle along $y=5\text{m}$ plane in $St \in (0,065, 0.1]$ bin.

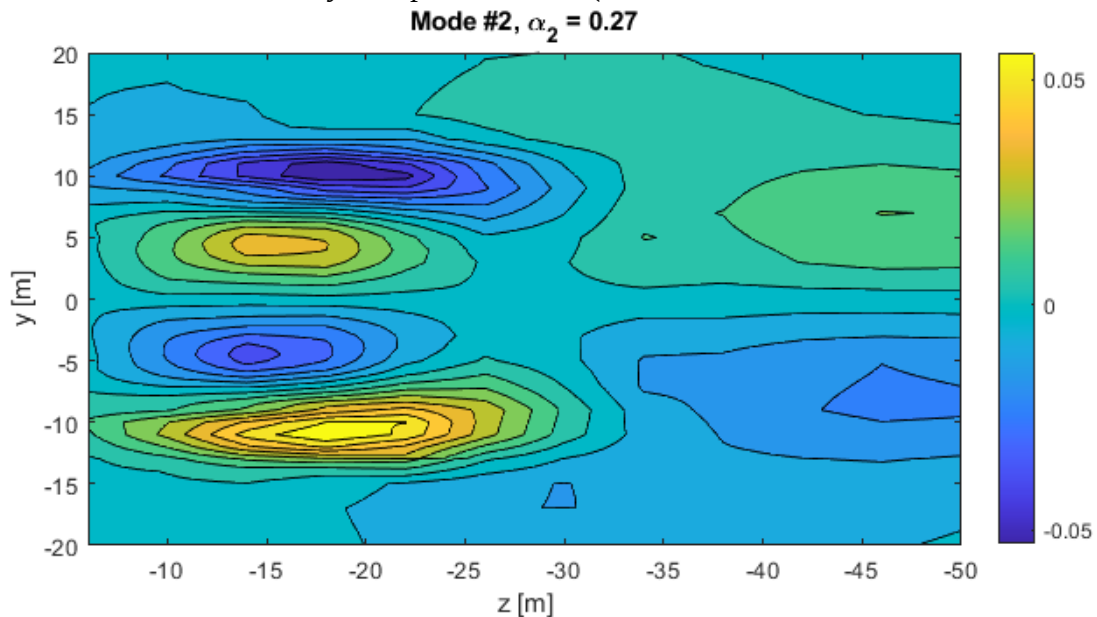


Figure 130: Mode #2 from POD analysis of longitudinal velocity data aft of the obstacle along $y=5\text{m}$ plane in $St \in (0,065, 0.1]$ bin.

Nondimensionalization and Parametrization

In this section, we examine the spanwise variation of longitudinal velocity model input parameters along the three planes we have been considering and provide simplified nondimensional relations that allow us to adequately capture the corresponding longitudinal velocity profiles, whilst simultaneously providing a basis for scalability to other rectangular configurations. These parameters were obtained using the automated parameter fitting scripts described above. Coordinates along the x , y , and z axes are expressed in nondimensional terms using obstacle width (W), height (H) and length (L), respectively.

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Midplane $x=0W$

Longitudinal velocity model input parameter variation along the $x=0W$ midplane is shown in Figure 131, where the overbar indicates nondimensional quantities. Distance parameters $r_c, h, R_{M_{upper}}, R_{M_{lower}}$ and r_0 were nondimensionalized using the obstacle height H while the velocity parameters $U_{M_{upper}}, U_{M_{lower}}$ and U_0 were nondimensionalized using freestream speed U_∞ . The trends exhibited in the parameter variations provide some insights into physical phenomena:

- the vertical location of vortex center $\bar{r}_c$ along the plane reduces as we proceed downstream in a manner similar to the shear layer shed from the top edge of the back face of the cube;
- the size of the vortex radius $\bar{h}$ increases almost linearly beyond $\bar{z} = 0.4$;
- the size of the vortex core region $\bar{c}$, where flow follows a rigid body rotation model increases linearly from $\bar{z} = 0.4$ to $\bar{z} = 1$, beyond which it starts decreasing;
- the vertical location of the upper spin point $\bar{R}_{M_{upper}}$, which demarks the start of potential flow region, remains approximately constant at $1.855H$ when the plotting range in Figure 131 is examined;
- the vertical location of the lower spin point $\bar{R}_{M_{lower}}$, where maximum velocity in the direction opposite of freestream is noted, increases almost linearly;
- flow acceleration due to shear layer shedding is apparent in the trendline for the upper spin velocity $\bar{U}_{M_{upper}}$, which initially has a magnitude of $1.2U_\infty$ immediately aft of the obstacle and progressively reduces to $1U_\infty$ as we proceed downstream;
- the magnitude of the lower spin velocity $\bar{U}_{M_{lower}}$ gives a direct measure of the magnitude of the recirculation velocity as it represents the maximum velocity in the direction *opposite* freestream velocity. In Figure 131, this magnitude can be seen increasing up to approximately $\bar{z} = 1.3L$, where peak velocity is close to $0.5U_\infty$, and beyond which it decreases. The location $\bar{z} = 1.3L$ corresponds to $z = -18m$ when dimensionalized, which is close to the $-18.9m$ location where the primary vortex core was established to be using Tecplot's vortex core extraction features in previous progress reports.

The parameters $\bar{r}_0$ and $\bar{U}$ are artificial artifacts added for better agreement with the CFD simulations that we employ as truth model.

Using the nondimensional trendlines in Figure 131 as basis, we derived the following simplified polynomial relations for the spanwise variation of model inputs:

$$\begin{aligned}
\bar{r}_c(\bar{z}) &= -0.14\bar{z}^2 + 0.17\bar{z} + 1.1, \\
\bar{h}(\bar{z}) &= 0.1\bar{z} + 0.35, \\
\bar{c}(\bar{z}) &= -0.16\bar{z}^2 + 0.17\bar{z} + 0.49, \\
\bar{R}_{M_{upper}}(\bar{z}) &= 1.9, \\
\bar{R}_{M_{lower}}(\bar{z}) &= 0.088\bar{z} + 0.15, \\
\bar{U}_{M_{upper}}(\bar{z}) &= 0.069\bar{z}^2 - 0.1\bar{z} - 1.15, \\
\bar{U}_{M_{lower}}(\bar{z}) &= -0.33\bar{z}^2 + 0.86\bar{z} - 0.11, \\
\bar{r}_0(\bar{z}) &= -0.012\bar{z} + 0.086, \\
\bar{U}_0 &= 0.
\end{aligned} \tag{28}$$

which, when used with the modified Banks and Meroney model, enables adequate representation of the longitudinal velocity profile in the $x = 0W$ plane, as shown in Figure 132.

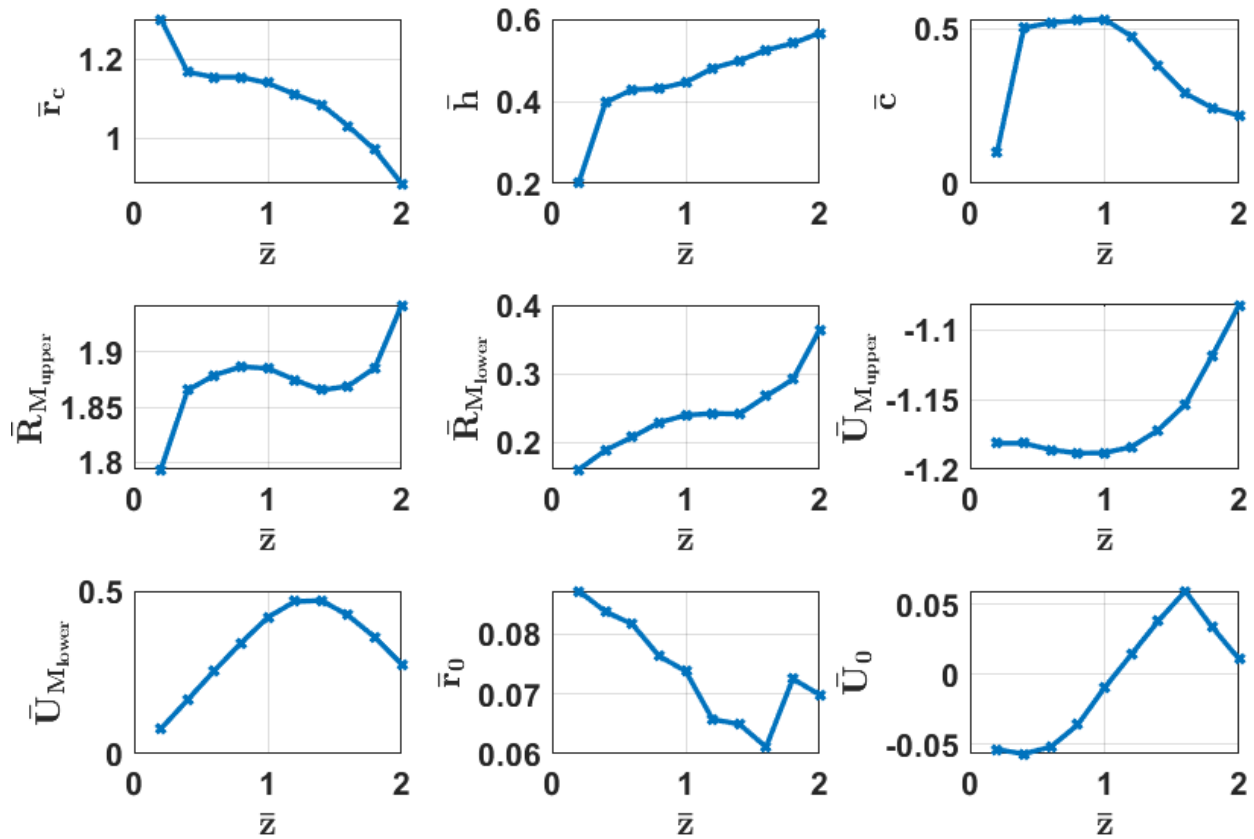


Figure 131: Nondimensional variation of modified Banks and Meroney model input parameters along $x = 0W$ plane; distance parameters nondimensionalized by obstacle height, and span location nondimensionalized using obstacle length such that back face corresponds to $\bar{z} = 0L$.

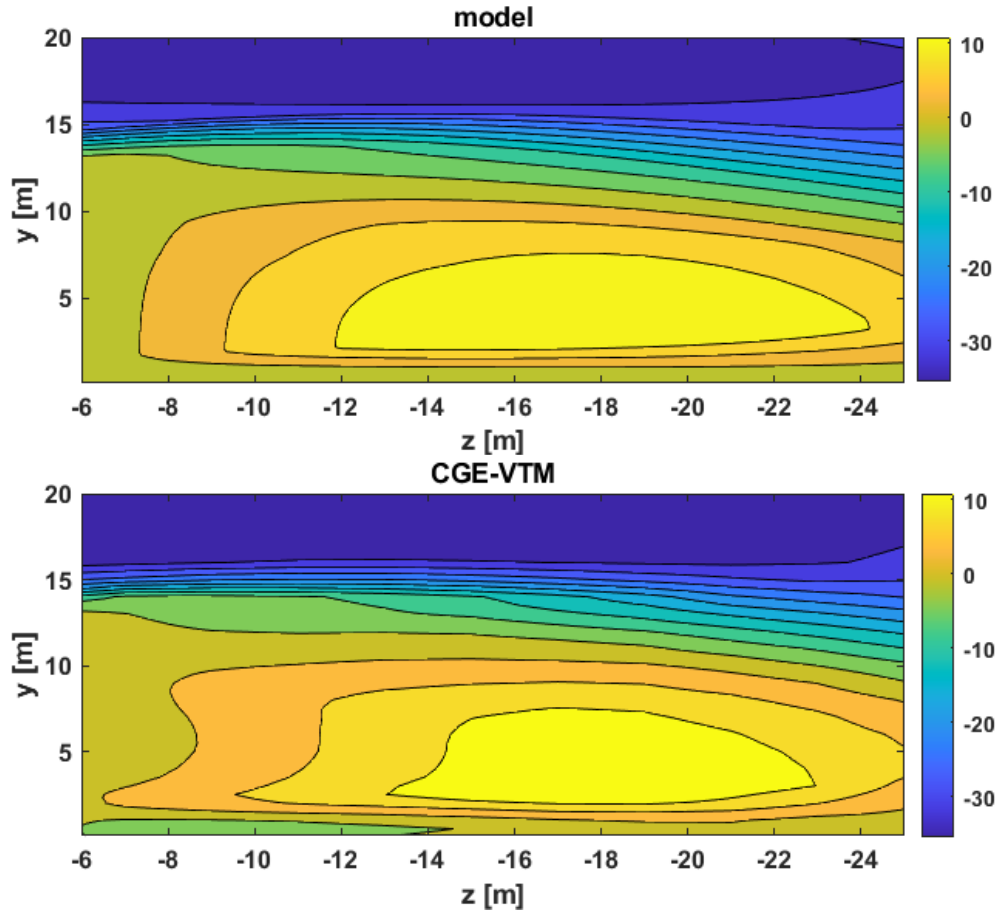


Figure 132: Contour of longitudinal velocity profile across $x = 0W$ plane obtained using model (top) and CGE/VorTran-M (bottom).

Midplane $y=0.5H$

A symmetric velocity distribution is noted in the $y = 0.5H$ plane. Given the resemblance of the profile to the that in $x = 0W$ plane, we conjectured that the simplified parameter relations given in Eq. (28) could be employed here as well. We introduce the $\hat{y}$ axis here to represent the vertical axis employed by the model. In the previous section, $\hat{y}$ coincided with the CGE/VorTran-M y axis and with $\hat{y} = 0H$ corresponding to $y = 0H$, which represents ground. In the current side plane, $\hat{y}$ corresponds to the CGE/VorTran-M x axis. No ground is present but a symmetry condition exists relative to the $x = 0W$ plane. We surmised that using the relations given in Eq. (28) together with the modified Banks and Meroney model with $\hat{y} = 0.5H$ as starting height would be adequate for representing the longitudinal velocity profile for regions $x > 0W$ and $x < 0W$ (symmetry assumed). From the contour plots shown in Figure 133, this hypothesis appears correct. Note that for this plane, the distance parameter relations given in Eq. (28) were dimensionalized using the obstacle width, which for a cube equals the height. These equations can also be potentially applied to rectangular configuration where the width does not equal the obstacle height.

The representation employed for the $y = 0.5H$ plane can also be thought of as a 90° counterclockwise rotation of the velocity profile from the $\hat{y} = 0.5H$ location upwards about the $x = 0W, y = 0.5H$ point, while appropriately accounting for obstacle width. The similarity in

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the velocity profiles also points towards a corresponding similarity that exists between the shear layer (and associated recirculation) from the top and side edges of the back face of the obstacle. Although these coherent features exist as 3D phenomena, they can be analyzed using 2D planar snapshots as we do here.

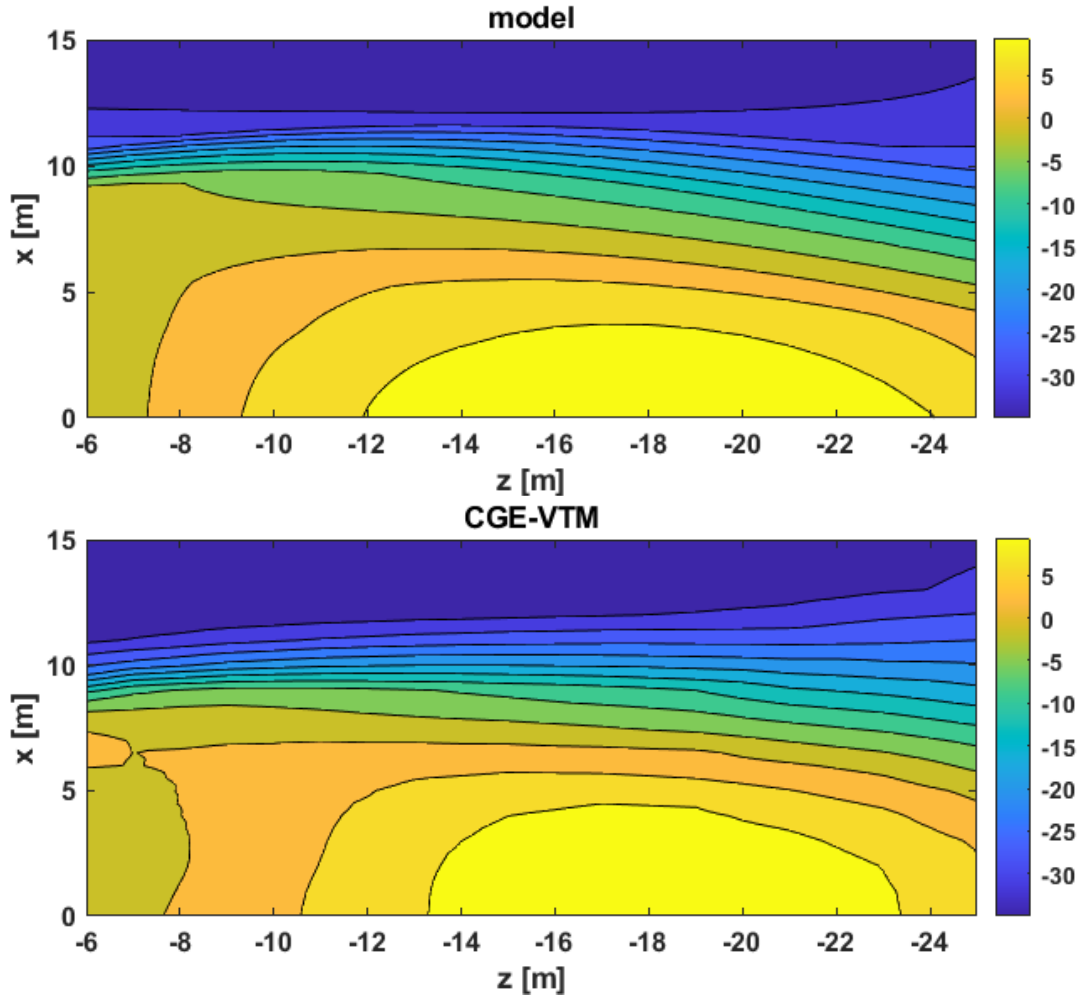


Figure 133: Contour of longitudinal velocity profile across $y = 0.5H$ plane obtained using model (top) and CGE/VorTran-M (bottom).

Diagonal Plane

We now consider the diagonal plane shown in Figure 80(c). As a first attempt, we applied the modified Banks and Meroney model with the relations given in Eq. (28) given the similarity of the velocity profile to that in Figure 80 (a). We dimensionalized distance parameters using the diagonal length of $L_c = 14.14\text{m}$. The resulting longitudinal velocity profiles are shown in Figure 134. There is a general agreement in the distribution with the CFD predictions, although it is apparent that the model predicts a wider range of positive velocities. Unlike the $x = 0W$ and $y = 0.5H$ planes considered previously, which intersect the middle of the obstacle, the diagonal plane intersects corner edges of the obstacle, which are associated with 3D flow effects. Shear layers shed from the top surfaces interact along that plane, together with deck-edge vortices that result due to pressure differences between the two surfaces.

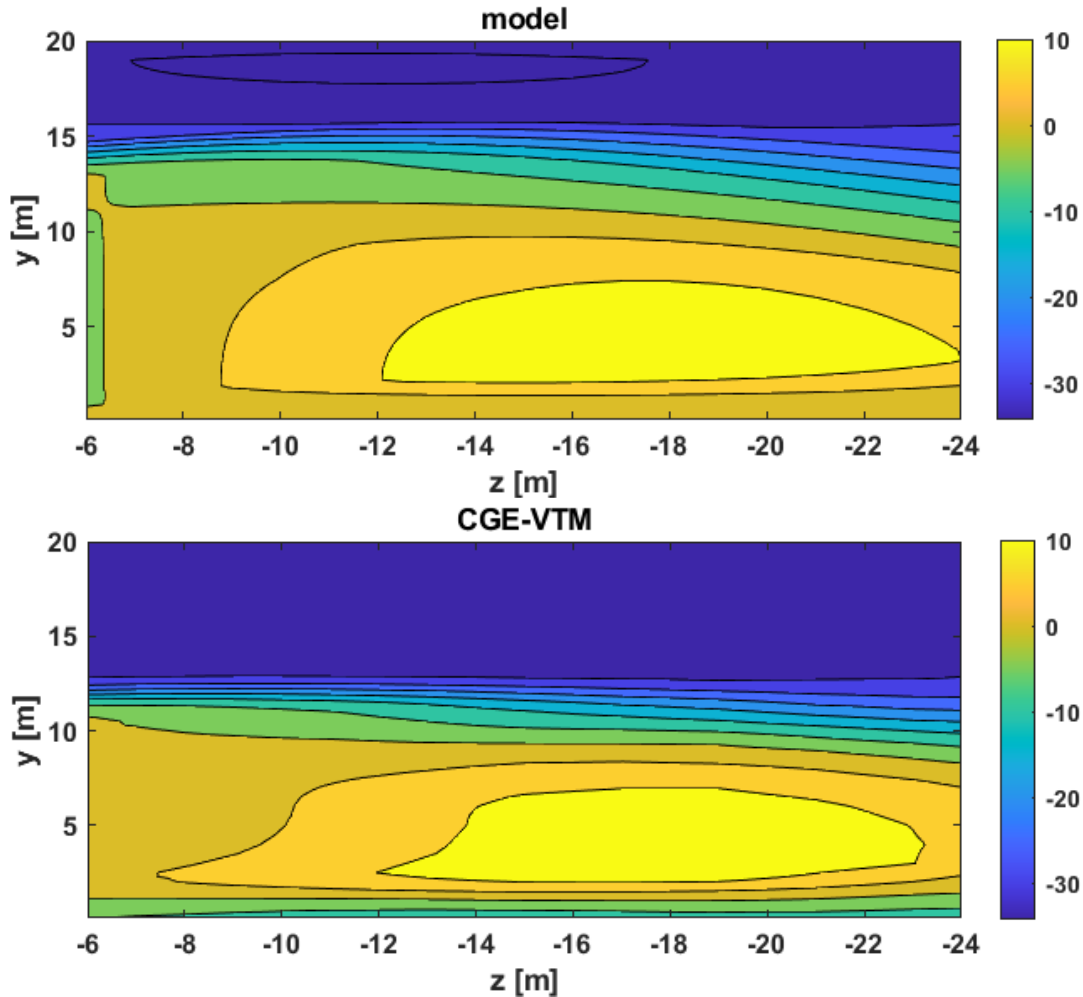


Figure 134: Contour of longitudinal velocity profile across diagonal plane obtained using model with input relations given by Eq. (28) (top) and CGE/VorTran-M (bottom).

Following a similar approach as in the $x = 0W$ plane, we employed the automated parameter fitting scripts to derive optimal parameter values at various span locations along the plane. These are plotted in Figure 135, where distance values have been nondimensionalized using the diagonal length and velocity values using freestream speed.

It is interesting to note the similarity in parameter trends in Figure 135 and Figure 131. With the exception of the vortex radius $\bar{h}$ and the two parameters added for consistency with the inviscid simulation $\bar{r}_0$ and $\bar{U}_0$, similar spanwise variations are noted for the remaining parameters. In fact, the magnitudes and values of the variations of $\bar{R}_{M_{lower}}$, $\bar{U}_{M_{upper}}$ and $\bar{U}_{M_{lower}}$ in Figure 135 closely match those in Figure 131. The differences seen in the $\bar{r}_c$ and $\bar{R}_{M_{upper}}$ parameter variations can potentially be attributed to the combined effect of the interacting top and side shear layers. The nondimensional range of $\bar{r}_c$ in Figure 131 of $[0.9, 1.3]H$ is wider than the $[0.8, 1]L_c$ range in the diagonal plane in Figure 135. Physically, this implies the viscous core center is lower in the diagonal plane than in the $x = 0W$ midplane. Similarly, the range of $\bar{R}_{M_{upper}}$ in the $x = 0W$ plane of $[1.8, 1.95]H$ is wider than the $[1.52, 1.6]L_c$ range across the diagonal plane.

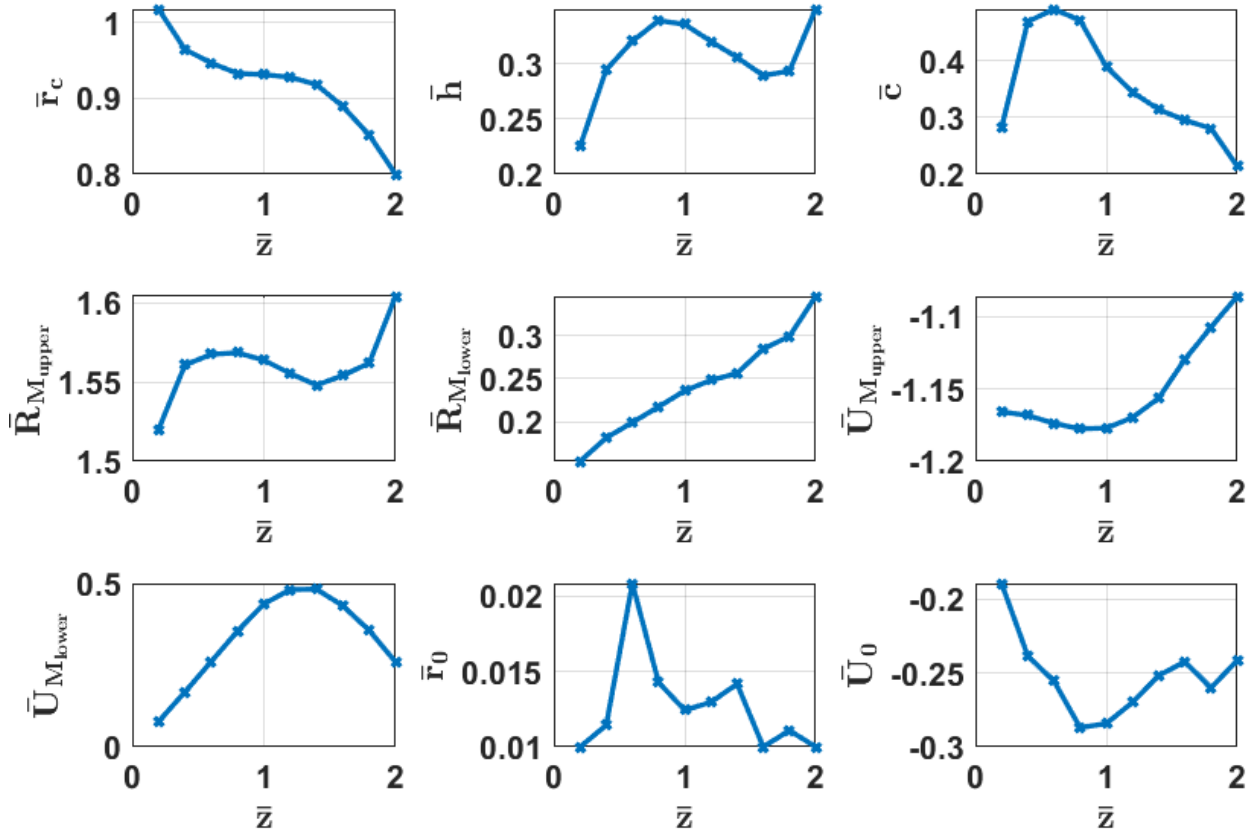


Figure 135: Nondimensional variation of modified Banks and Meroney model parameter inputs along diagonal plane; distance parameters nondimensionalized by diagonal length, and span location nondimensionalized using obstacle length such that back face corresponds to $\bar{z} = 0L$.

We derived the following simplified relations for the parameter variation in the diagonal plane:

$$\begin{aligned}
 \bar{r}_c(\bar{z}) &= -0.07\bar{z}^2 + 0.081\bar{z} + 0.93, \\
 \bar{h}(\bar{z}) &= 0.024\bar{z} + 0.28, \\
 \bar{c}(\bar{z}) &= -0.17\bar{z} + 0.57, \\
 \bar{R}_{M_{upper}}(\bar{z}) &= 1.56, \\
 \bar{R}_{M_{lower}}(\bar{z}) &= 0.094\bar{z} + 0.14, \\
 \bar{U}_{M_{upper}}(\bar{z}) &= 0.06\bar{z}^2 - 0.088\bar{z} - 1.15, \\
 \bar{U}_{M_{lower}}(\bar{z}) &= -0.36\bar{z}^2 + 0.92\bar{z} - 0.13, \\
 \bar{r}_0(\bar{z}) &= -0.0017\bar{z} + 0.015, \\
 \bar{U}_0 &= -0.25.
 \end{aligned} \tag{29}$$

Note how closely the equations for $\bar{c}$, $\bar{R}_{M_{lower}}$, $\bar{U}_{M_{upper}}$ and $\bar{U}_{M_{lower}}$ match those given in Eq. (28). The similarity in the parameter trends points towards the spatial correlation that exists in the ship airwake aft of the obstacle. Predictions using these updated parameter relations are shown in Figure 136, where good agreement with CGE/VorTran-M is noted.

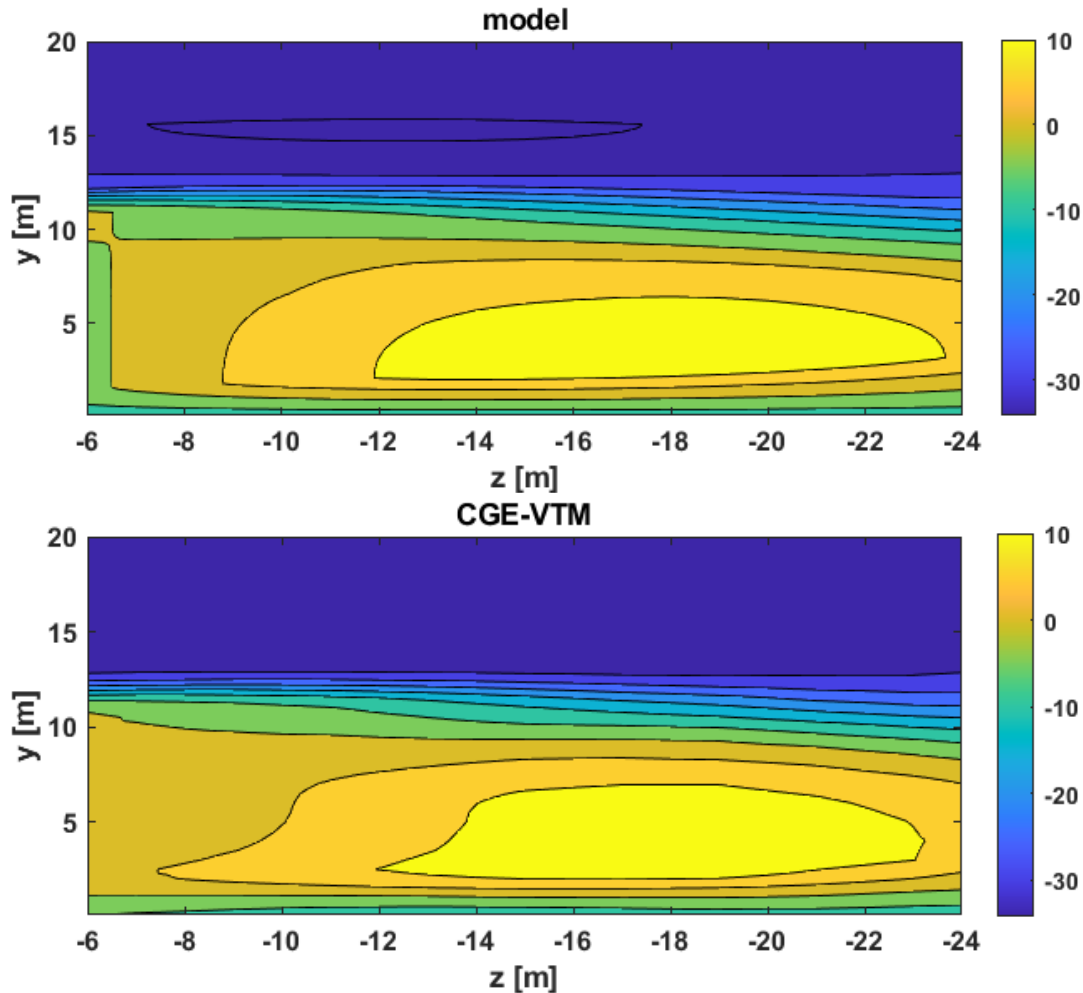


Figure 136: Contour of longitudinal velocity profile across diagonal plane obtained using model with input relations given by Eq. (29) (top) and CGE/VorTran-M (bottom).

Scaling Test for Time-Averaged Longitudinal Velocity Nondimensional Relations

Here we presented results demonstrating the scalability of the nondimensional relations derived previously by considering a 30 m/s headwind flow over a rectangular obstacle that is 30 m wide, 10 m long and 10 m high. For this wider configuration, the recirculation region extends further downstream when compared to the cube but still exhibits similar longitudinal velocity profiles. We introduced constant x , y and z stretch factors that when employed with the relations given earlier, allowed us to obtain adequate velocity profiles. For instance, if we employ a constant z -coordinate stretch factor of 0.5, the velocity profiles predicted cover twice the longitudinal distance aft of the obstacle. By default, all stretch factors are 1. The stretch factors for the cuboid were determined manually by comparison with CGE/VorTran-M data.

Model predictions for a transverse plane are shown in Figure 138 for the wider configuration. Despite the smaller size of the peak recirculation region predicted by the model, good agreement is noted with CGE/VorTran-M results. The larger size of the recirculation region is apparent in the contour plots. Similar nondimensional relations were derived and tested for time-averaged vertical velocity profiles over and aft of the obstacle.

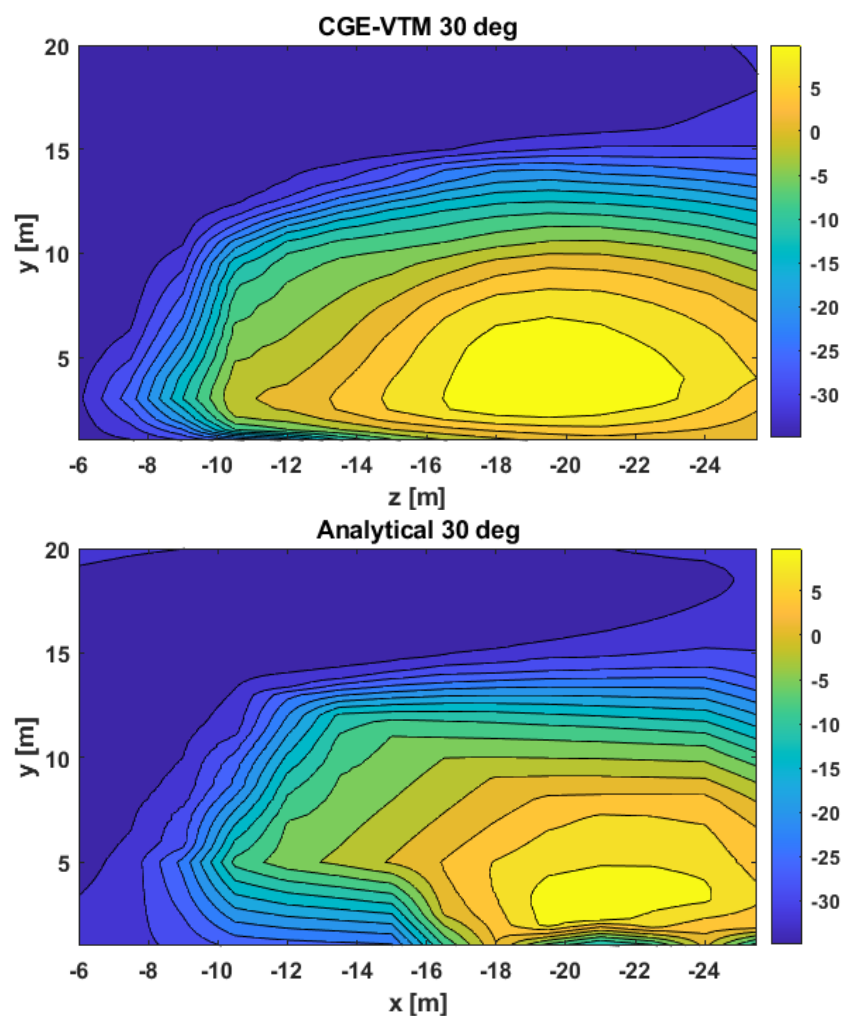


Figure 137: Mean longitudinal velocity contours along transverse plane aft of cube; truth (top) and parametric model (bottom).

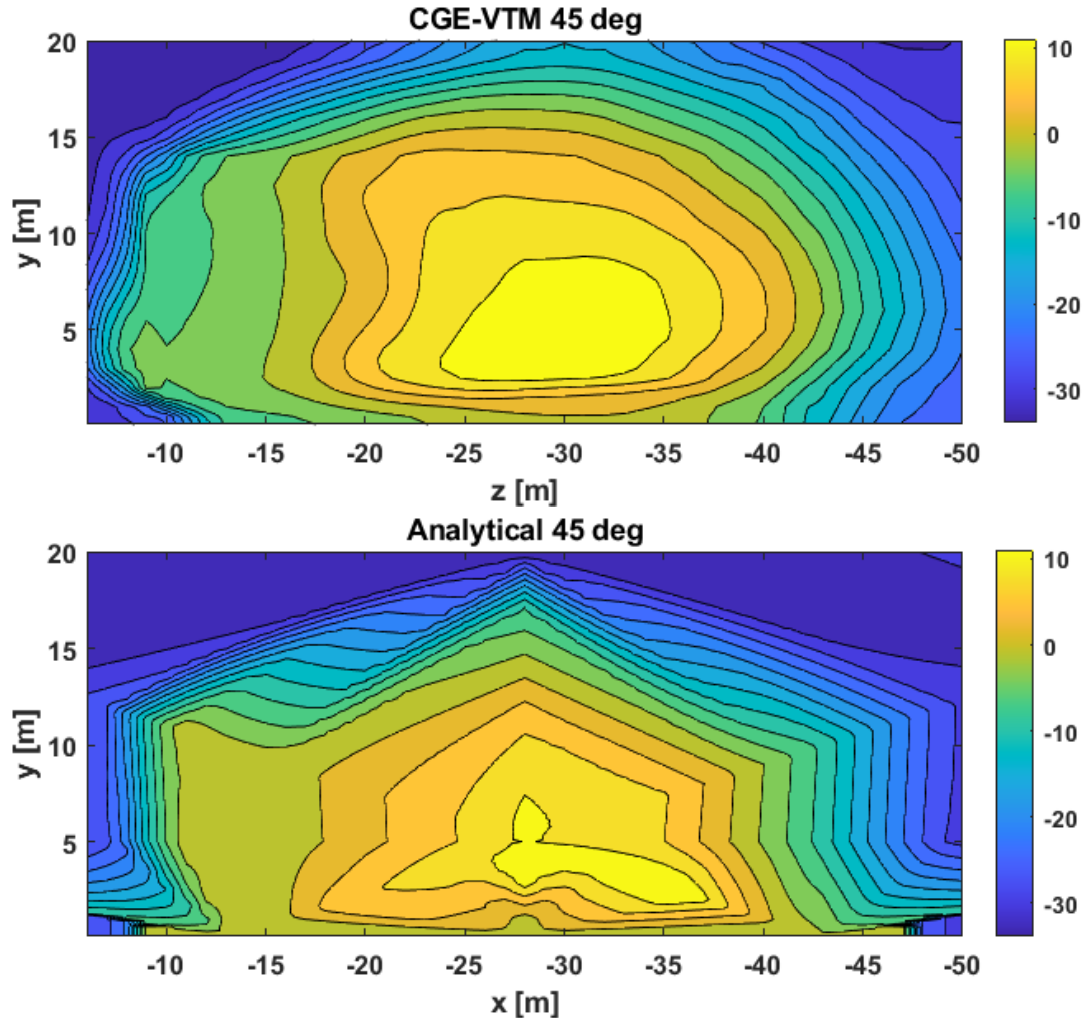


Figure 138: Mean longitudinal velocity contours along transverse plane aft of 30m wide cuboid; truth (top) and parametric model (bottom).

Disturbance Model Testing

Flow Feature Characterization – Arch Vortex

During the effort, we formulated and tested out a second approach for flow features characterization. In this methodology, we characterize 3D flow features directly as vortical structures in terms of geometry (position, size) and strength. Initial tests of this simplified disturbance field representation were carried for the arch vortex aft of the surface mounted cube (see Figure 78) with the goal of deriving a nondimensional function for the vertical velocity induced by the flow structure $v_{vortex}(r, \psi)$, which would be used to examine fundamental rotorcraft responses.

We employ a planar approach to the characterization, whereby we examine the time-averaged velocity, Q-criterion, and Δ -criterion distributions in various planes to identify (a) the center of the arch vortex, (b) the associated viscous core radius and (3) corresponding strength. Definitions for the Q- and Δ -criteria can be found in Ref. [99]. The planes we consider consist of the $x = 0$ m midplane shown in Figure 80 (a), and the $y = \{2.5, 3.5, 5, 7, 9\}$ m planes. Figure 139 shows quiver plots of the velocity field across various $y = \text{constant}$ planes. The figure follows

the general CGE/VorTran-M coordinate system described above. The aft recirculation zone is apparent with the blue “hole” near $z=-20\text{m}$. The arch vortex exists within the “white” region that bounds the hole on the Figure. The process we used in identifying the arch vortex center, size and strength consisted of:

1. Plotting the velocity field along multiple 2D planes as a quiver plot to locate vortex centers;
2. Ensuring any rotational behavior observed corresponded to a vortex by cross-checking with corresponding Q-criterion and Δ -criterion contour plots;
3. Picking out the vortex center position and the position where maximum tangential velocity is noted (the latter demarked the vortex core limit);
4. Computing the circulation associated with the region that encloses the vortex;
5. Nondimensionalizing the position coordinates of the vortex core center and core radius size along the different planes by an obstacle dimension (e.g., height) and the circulation by the product of an obstacle dimension and free stream speed; and
6. Fitting polynomial expressions on the nondimensional vortex center position and radius data.

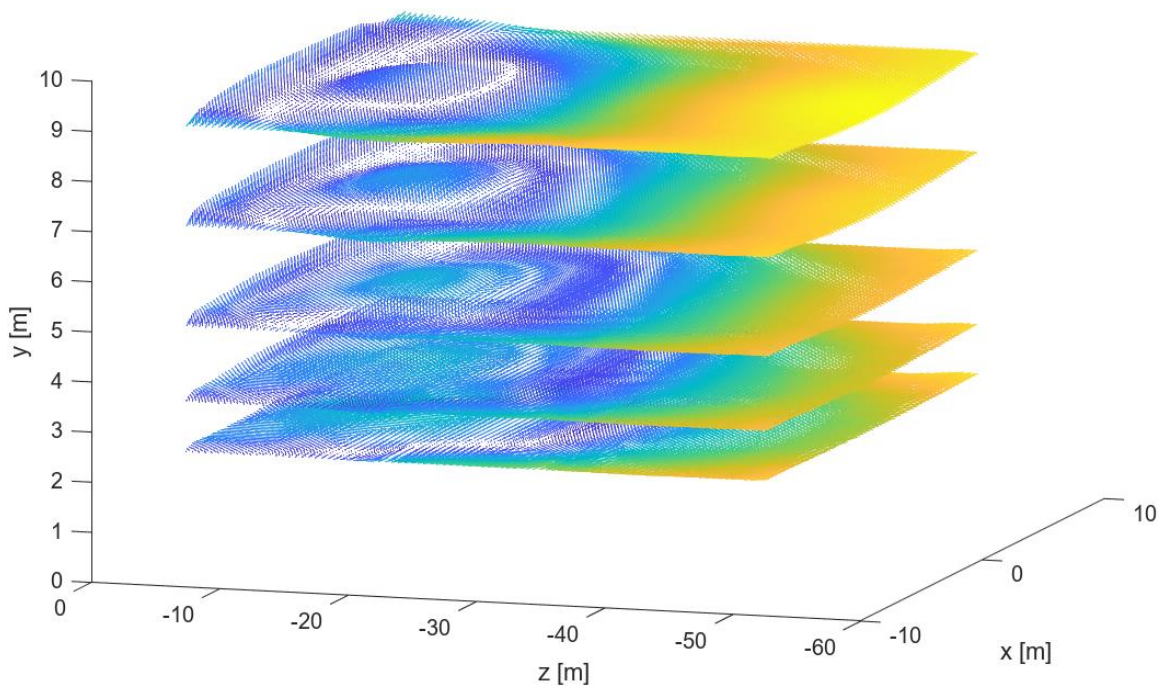


Figure 139: Quiver plot of in-plane velocity components at various heights; left to right along the z axis follows the flow downstream, down to up across the y axis goes upwards from ground.

We illustrate these steps using the $y=5\text{m}$ plane. The quiver plot for time-averaged in-plane velocities is shown in Figure 140. From the figure, two vortices with centers at $[z, x] = [-15, -6.5]\text{m}$ and $[z, x] = [-17, 6.5]\text{m}$ can be identified. Cross checking with the zoomed in Q- and Δ -criteria plots shown in Figure 141, we note that a vortex does indeed exist at $[z, x] = [-15, -6.5]\text{m}$. A stronger vortex exists near $x \approx -8.0\text{m}$, likely associated with the alternate vortices shed from the vertical edges of the cube. We assume symmetry exists and focus on this one confirmed vortex. It is important to crosscheck the centers identified from the quiver plots with those in Q- and Δ -criteria plots because, by themselves, quiver plots can fail to identify

vortex cores depending on the reference frame relative to which they are plotted [100]. From the quiver plot in Figure 140, peak tangential (x) velocity along the $z=-15\text{m}$ line (where the vortex center was identified) is noted at $x=-0.5\text{m}$, resulting in a vortex core radius size of

$$r_c = |-6.5 - (-0.5)| = 6\text{m}. \quad (30)$$

The final step consists of computing the vortex strength or circulation, which is defined as:

$$\Gamma = \oint_C \mathbf{v} \cdot d\mathbf{l} = \int_S (\nabla \times \mathbf{v}) \cdot d\mathbf{S} = \int_S \boldsymbol{\omega} \cdot d\mathbf{S}, \quad (31)$$

where $\mathbf{v}$ is the velocity field, $d\mathbf{l}$ is an infinitesimal length along a closed circuit C and $\boldsymbol{\omega}$ is the vorticity vector. Note that we apply Stokes theorem in Eq. (31) to convert the line integral to the surface integral. To compute the circulation, we first compute the out of plane vorticity by taking the curl of the planar velocities and then carry out a surface integral over the region bounded within $z \in [-13, -17]\text{m}$ and $x \in [-8.5, -3.5]\text{m}$, a region selected from examining the Q- and Δ -criteria plots in Figure 141. This results in a circulation value of $\Gamma = 33.7 \text{ m}^2/\text{s}$.

We repeated the characterization procedure using various planes in the 3D space. Results are summarized in Table 8. Note that for the $x=0\text{m}$ midplane shown in Figure 80(a), a secondary and more potent vortex exists further downstream at $z=-21\text{m}$, where part of the shear layer flows back into the recirculation zone while the other gets entrained downstream by freestream velocity.

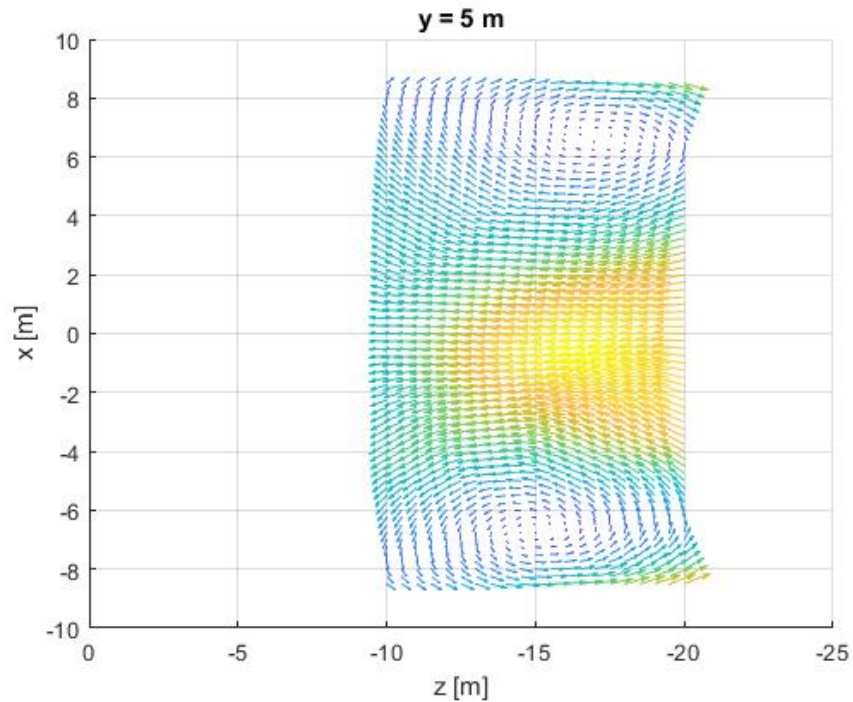


Figure 140: Quiver plot for $y=5\text{m}$ plane.

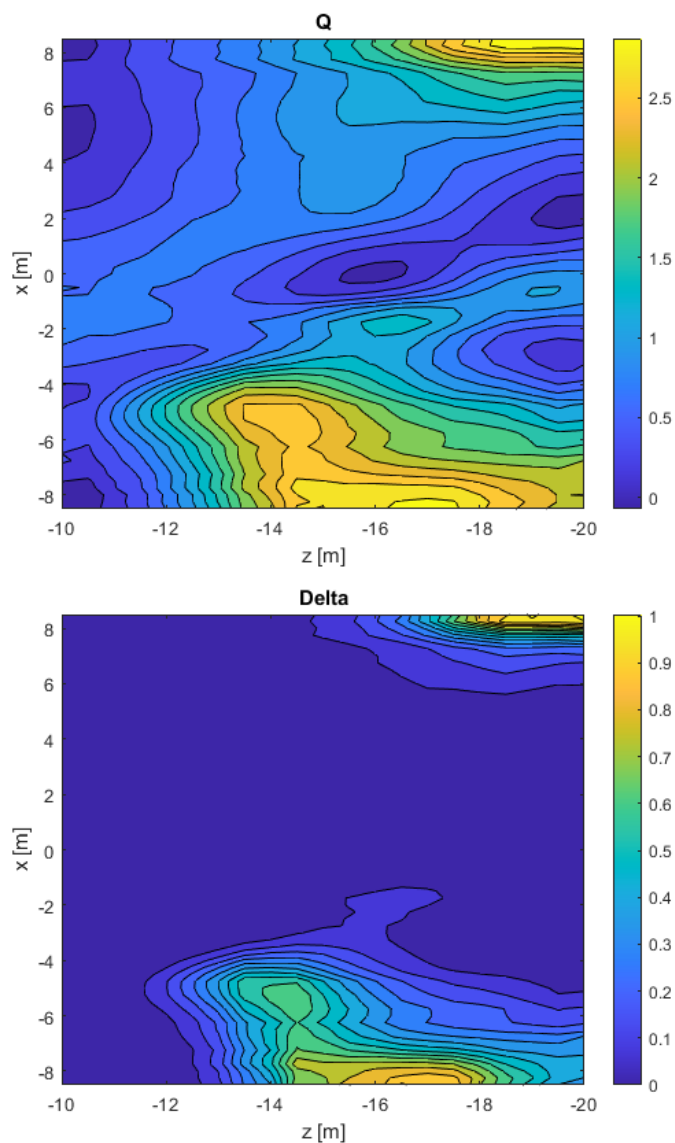


Figure 141: Q-criterion (top) and Δ -criterion (bottom) distributions for $y=5\text{m}$ plane.

Table 8: Arch vortex characterization along different planes.

Plane	Vortex #1 loc [m]	Radius [m]	Circ strength [m ² /s]
x=0m	$[z, y] = [-18, 12]$	1	26.5
y=9m	$[z, x] = [-16, -4.25]$	3.75	33.6
y=7.5m	$[z, x] = [-15, -5.5]$	4.5	33.1
y=5m	$[z, x] = [-15, -6.5]$	6	33.7
y=3.5m	$[z, x] = [-15, -7.5]$	7	31.5
y=2.5m	$[z, x] = [-16.5, -7.75]$	7.25	31.9

The arch vortex characterization information provided in Table 8 is specific to the headwind flow simulation over the 10m cube. We derived nondimensional relations using this information. The nondimensional position of the arch vortex center line is given by

$$\begin{aligned}\bar{y} &= 1.55\bar{x}^2 + 1.2, \\ \bar{z} &= -1.5H,\end{aligned}\tag{32}$$

where the coordinates $\bar{x}$, $\bar{y}$, and $\bar{z}$ have been nondimensionalized by obstacle width W , height H and length L , respectively. From Eq. (32), the arch vortex center is assumed to be a 2D parabola that exists in the $\bar{z} = -1.5H$ plane, see Figure 142.

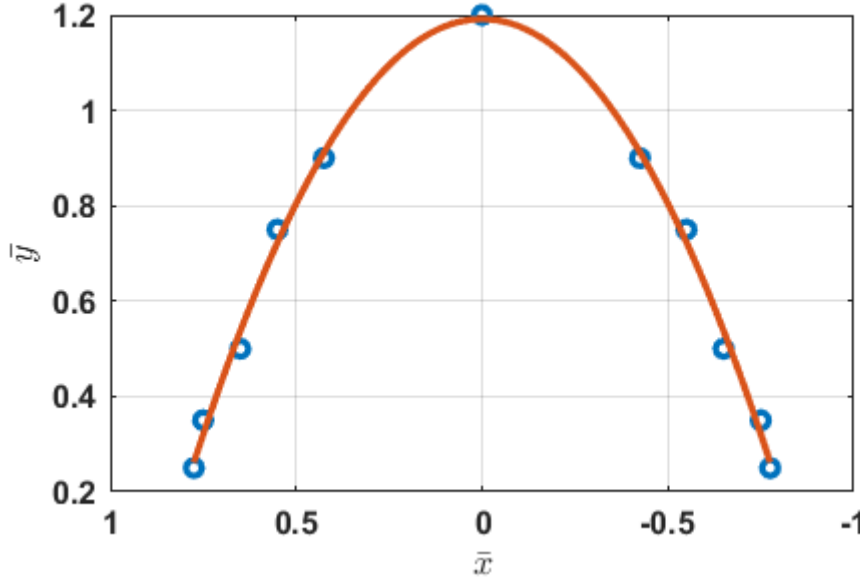


Figure 142: Parabolic representation of arch vortex in the $\bar{z} = -1.5H$ plane; blue points represent points from Table 1 and the red line represents the quadratic fit.

An expression for the nondimensional vortex core radius was similarly derived as a function of the nondimensional vertical coordinate $\bar{y}$:

$$\bar{r}_c = -0.90\bar{y} + 1.27.\tag{33}$$

From Table 8, the strength of the vortex in the different planes are close to each other, indicating viability of the vortex modeling approach. We averaged the corresponding values and nondimensionalized them using the freestream velocity magnitude of 30m/s and obstacle height $H = 10\text{m}$ to obtain a nondimensional arch vortex strength of

$$\bar{\Gamma} = \frac{\Gamma}{U_\infty H} = \frac{31.7 \text{ m}^2/\text{s}}{(30 \text{ m/s})(10 \text{ m})} = 0.106.\tag{34}$$

We represented the arch vortex as a parabolic vortex filament with a ground mirror counterpart and implemented a numerical vortex model that combined the Biot-Savart law with the Vatisas viscous core model [101] to obtain associated induced velocity predictions. The ground mirror filament helped satisfy Helmholtz theorem which states that a vortex filament must form a closed loop. The velocity induced by this arch vortex at a point located at position $\mathbf{r}$ relative to discretized vortex filament element $d\mathbf{l}$ was given by

$$\mathbf{V}_{ind} = \frac{\Gamma h}{4\pi(r_c^{2n} + h^{2n})^{1/n}} \int \frac{d\mathbf{l} \times \mathbf{r}}{|\mathbf{r}|^3} \times fac,\tag{35}$$

where h is the perpendicular distance of the evaluation point from the vortex filament element, n is an integer that controls the smoothness of the transition of the velocity predictions from the viscous core to the external inviscid region (we used $n = 1$), and fac is a scaling factor that varies with the nondimensional vertical coordinate $\bar{y}$ that we found necessary to include for accurate velocity magnitude predictions:

$$fac = -25\bar{y} + 25. \quad (36)$$

The induced velocities determined using Eq. (35), together with those from CGE/VorTran-M for the $y = \{2,4,6,8,10\}$ m and $x = \{1,3,5,7,9\}$ m planes for the 30m/s headwind flow over the 10m cube are plotted in Figure 143 and Figure 144, respectively. The CGE/VorTran-M predictions include influence from all flow features whereas those of the vortex model only represent those from the arch vortex.

We make the following observations from the Figures:

- a. For the $y=\text{constant}$ planes, the lateral velocity U component predictions from the model exhibit similar magnitudes and spatial distributions as the CGE/VorTran-M predictions, while for the $x=\text{constant}$ planes, the model prediction for U are similar to those from CGE/VorTran-M only in the $x=5\text{m}$ and $x=7\text{m}$ planes, with some discrepancies noted downstream in the $z<-20\text{m}$ and $y>10\text{m}$ regions.
- b. For the $y=\text{constant}$ planes, the model predicts an antisymmetric vertical velocity V distribution about $z=-15\text{m}$, which bears some resemblance to the predictions from CGE/VorTran-M but does not fully capture the expected velocities and magnitude, a mismatch which could be potentially improved by accounting for additional flow feature models. This mismatch is noted in the $x=\text{constant}$ planes as well, although to a lesser extent, indicating the potential viability of the arch vortex model.
- c. For both the $y=\text{constant}$ and $x=\text{constant}$ planes, peak longitudinal velocities W are adequately captured by the model in all planes except the $y=10\text{m}$ plane. Additionally, the peak longitudinal velocity regions predicted by the model covers a smaller spatial domain than those from CGE/VorTran-M.
- d. Predictions from the arch vortex model show reasonable agreement with expected velocities in the domain bounded approximately by $x \in [-5,5]\text{m}$, $y \in [2,9]\text{m}$ and $z \in [-25,-10]\text{m}$, which corresponds to $\bar{y} \in [-W/2, W/2]$, $y \in [0.2,0.9]H$ and $z \in [0.5,2]H$ aft of the obstacle.

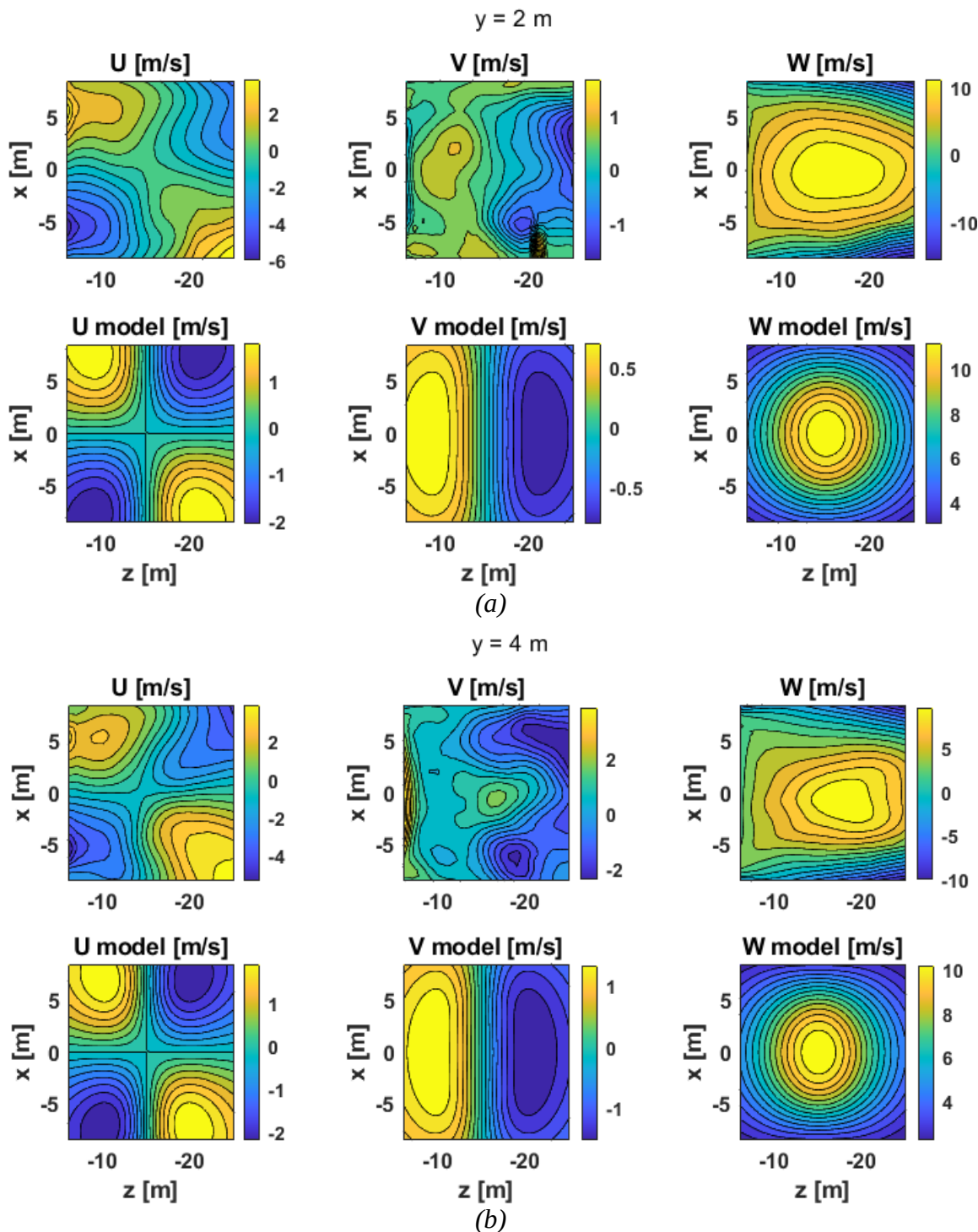


Figure 143: Contours of airwake velocity components aft of the 10m obstacle for various $y=\text{constant}$ planes obtained using CGE/VorTran-M and arch vortex model.

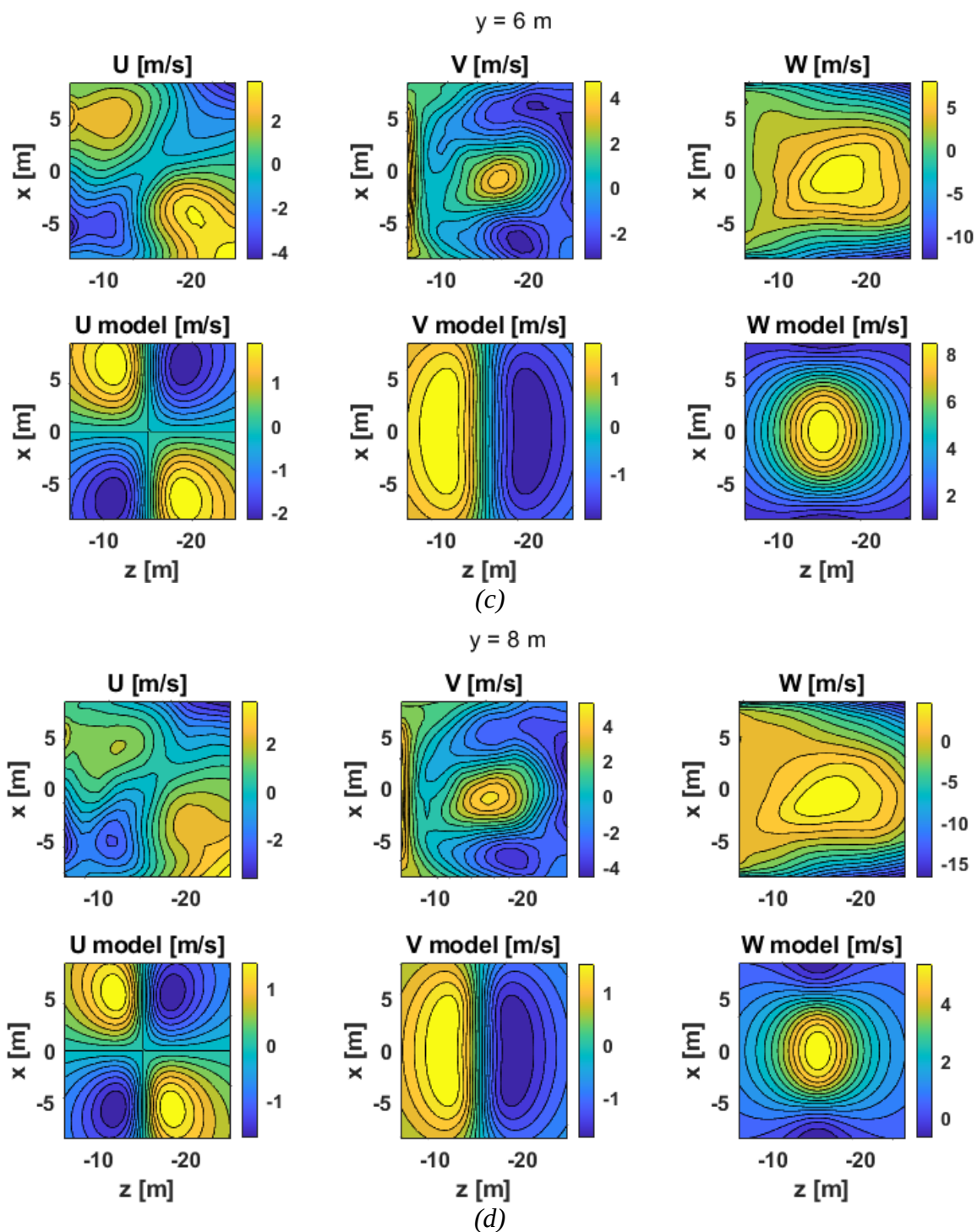


Figure 143: Contours of airwake velocity components aft of the 10m obstacle for various $y=\text{constant}$ planes obtained using CGE/VorTran-M and arch vortex model.

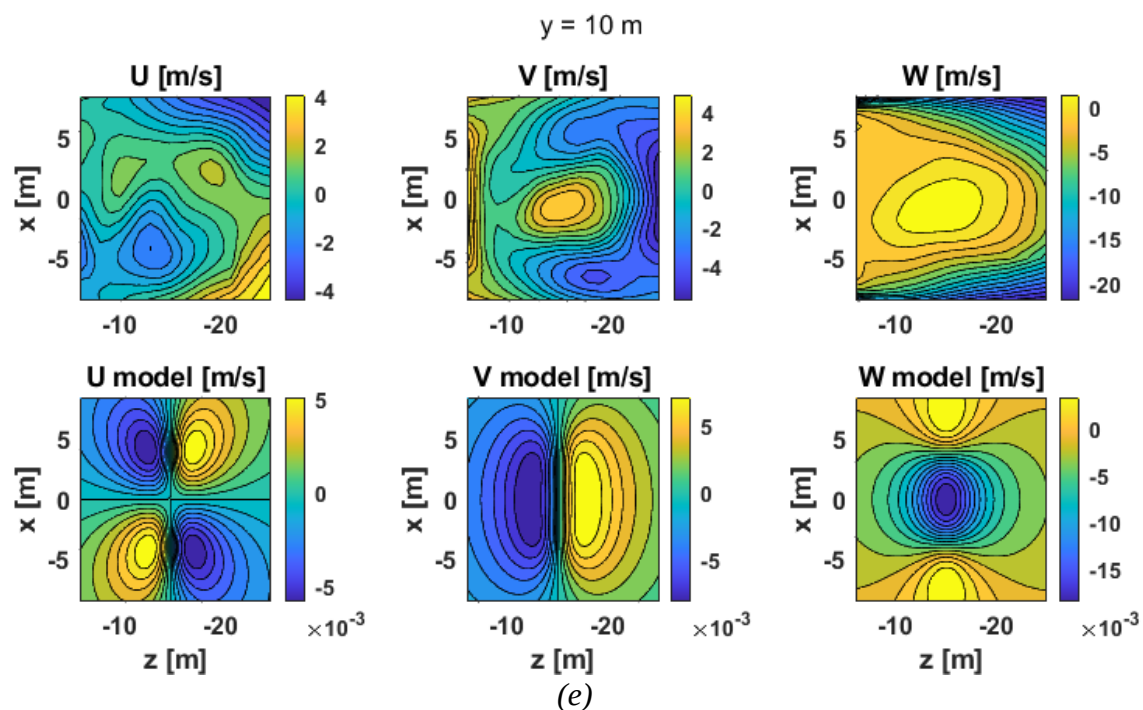


Figure 143: Contours of airwake velocity components aft of the 10m obstacle for various $y=\text{constant}$ planes obtained using CGE/VorTran-M and arch vortex model.

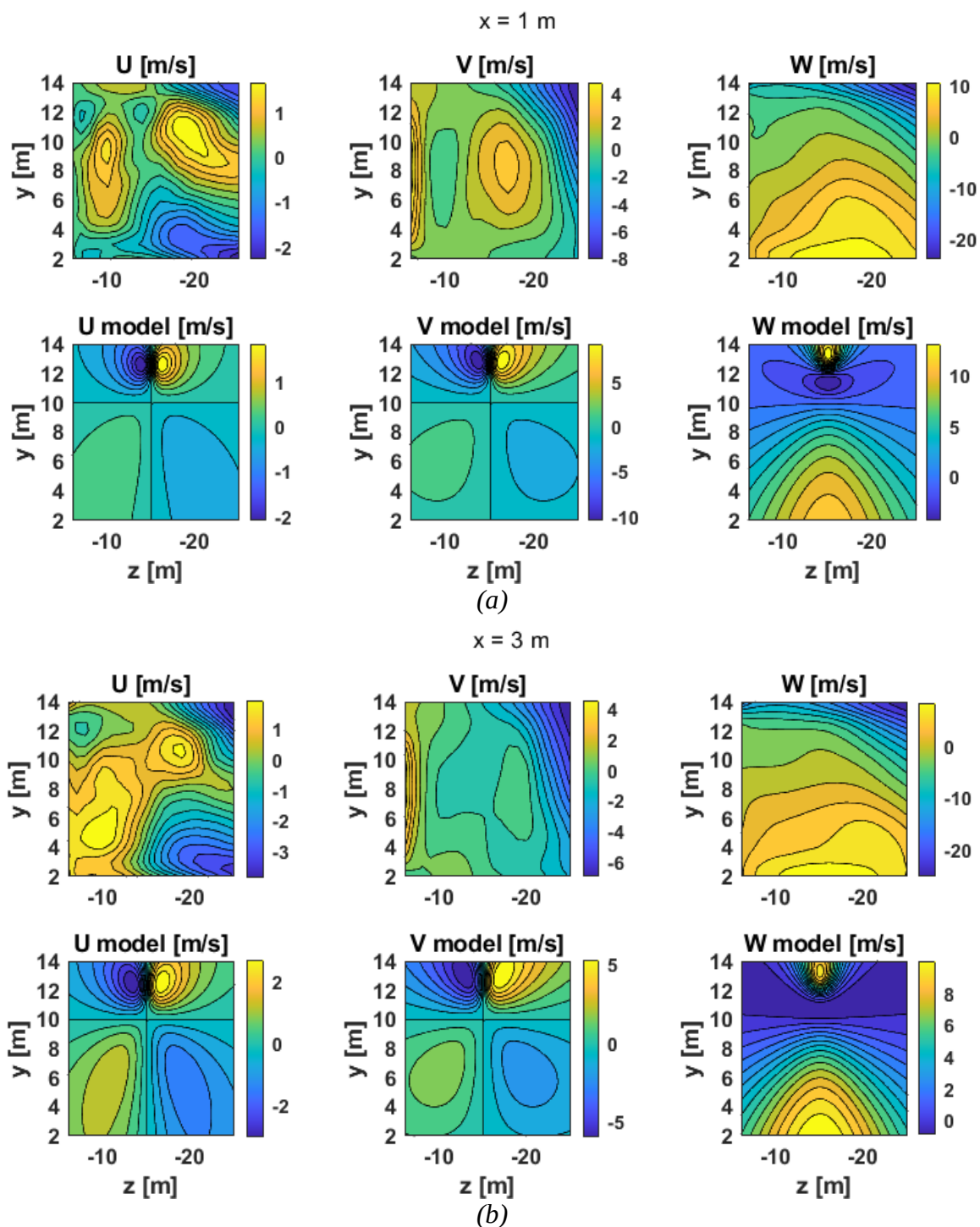


Figure 144: Contours of airwake velocity components aft of the 10m obstacle for various $x=\text{constant}$ planes obtained using CGE/VorTran-M and arch vortex model.

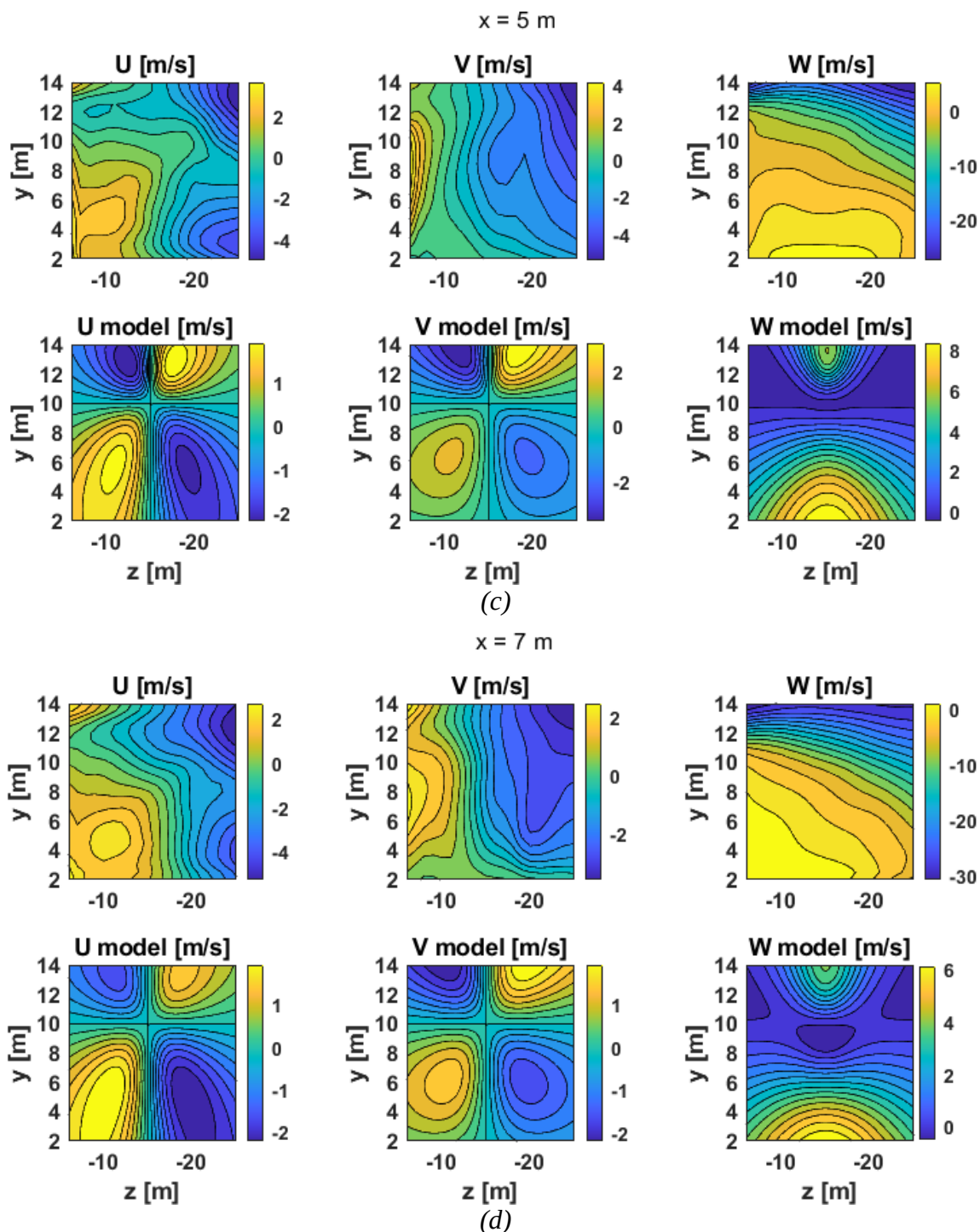


Figure 144: Contours of airwake velocity components aft of the 10m obstacle for various $x=\text{constant}$ planes obtained using CGE/VorTran-M and arch vortex model.

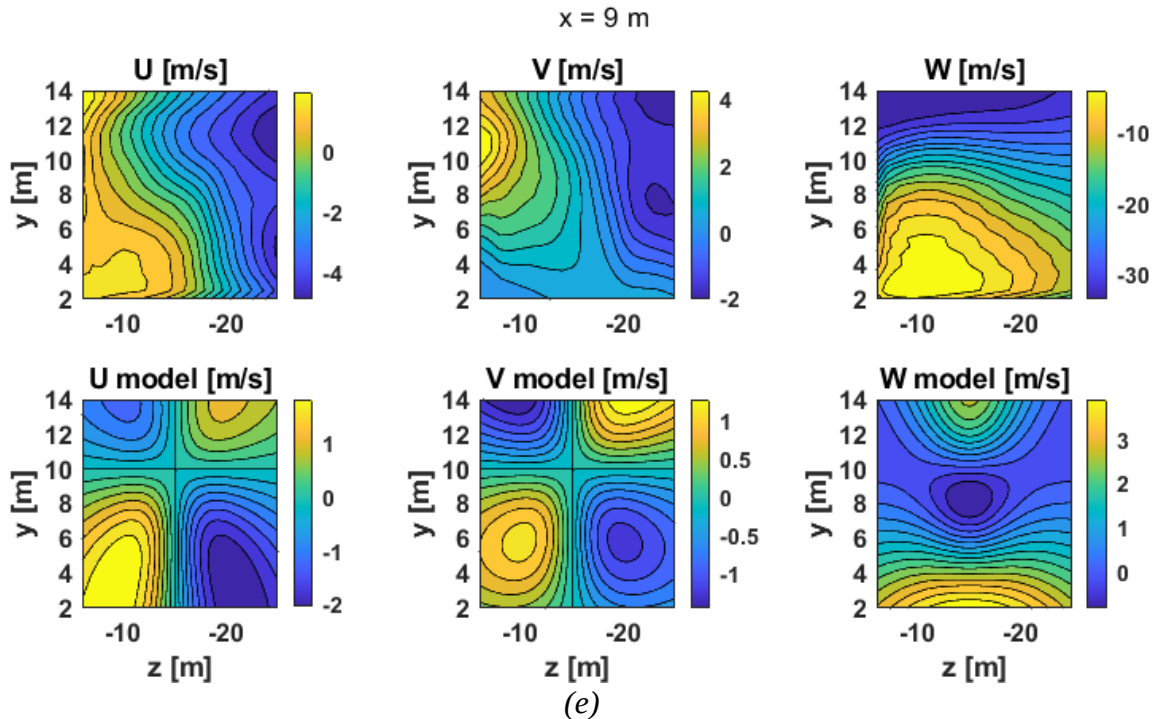


Figure 144: Contours of airwake velocity components aft of the 10m obstacle for various $x=\text{constant}$ planes obtained using CGE/VorTran-M and arch vortex model.

Flow Feature Characterization – Aft Horseshoe Vortex

Aft of the obstacle, carrying out the steps vortex characterization steps listed above for $z=\text{constant}$ planes revealed the existence of two symmetrical vortical tubes downstream of the obstacle. These vortical structures are likely associated with the interaction of the shear layers shed from the side, top and corner edges of the cube. We modeled these flow structures as a horseshoe vortex with the following limits: $x \in [-0.5, 0.5]W$, $y = 0.9H$ and $z \in [1.5, \infty]H$ downstream, where W is the obstacle width and H the obstacle height. The nondimensional circulation was found to be 0.01. To test out the characterization, we performed velocity reconstruction using Eq. (35) with $fac = 15$, determined manually. Resulting velocity contours, together with from CFD simulations are plotted in Figure 145 for various $z=\text{constant}$ planes. Note that the CGE/VorTran-M (CFD) predictions include influence from all flow features and that the horseshoe filament vortex has its horizontal part at $z=-20\text{m}$. Good agreement in the vertical v velocity profiles is noted downstream from approximately $z = -24\text{m}$ onwards. Closer to the obstacle at $z=-18\text{m}$, which is in the fore region of the horseshoe vortex, the model underpredicts the vertical velocity. The trends exhibited by the lateral u velocity contours bear some resemblance to those from CFD simulations in all the plots, while a mismatch is noted in the longitudinal w velocity ones. The results here are promising given that the vertical velocity of the airwake is the component that directly influences rotor inflow and is reasonably captured using an isolated horseshoe vortex. Note that we later on revisited the horseshoe vortex characterization and derived a new vortex strength value of $\bar{\Gamma} = 1.07$. The rederivation eliminated the need for the scaling factor fac , which varied depending on the geometry considered.

The combined (superimposed) influence of the arch vortex from the previous progress report and the horseshoe vortex is considered in Figure 146, where associated velocities at various $z=\text{constant}$ planes are plotted. From the Figure, the inclusion of the arch vortex model improves longitudinal w velocity predictions below $y=10\text{m}$ up to approximately $z=-27\text{m}$ downstream. Lateral u velocity predictions, however, are seen to have a lower magnitude than the counterparts in Figure 145. The vertical velocity profiles in Figure 146 are similar to those from Figure 145, indicating that contributions from the horseshoe vortex are dominant.

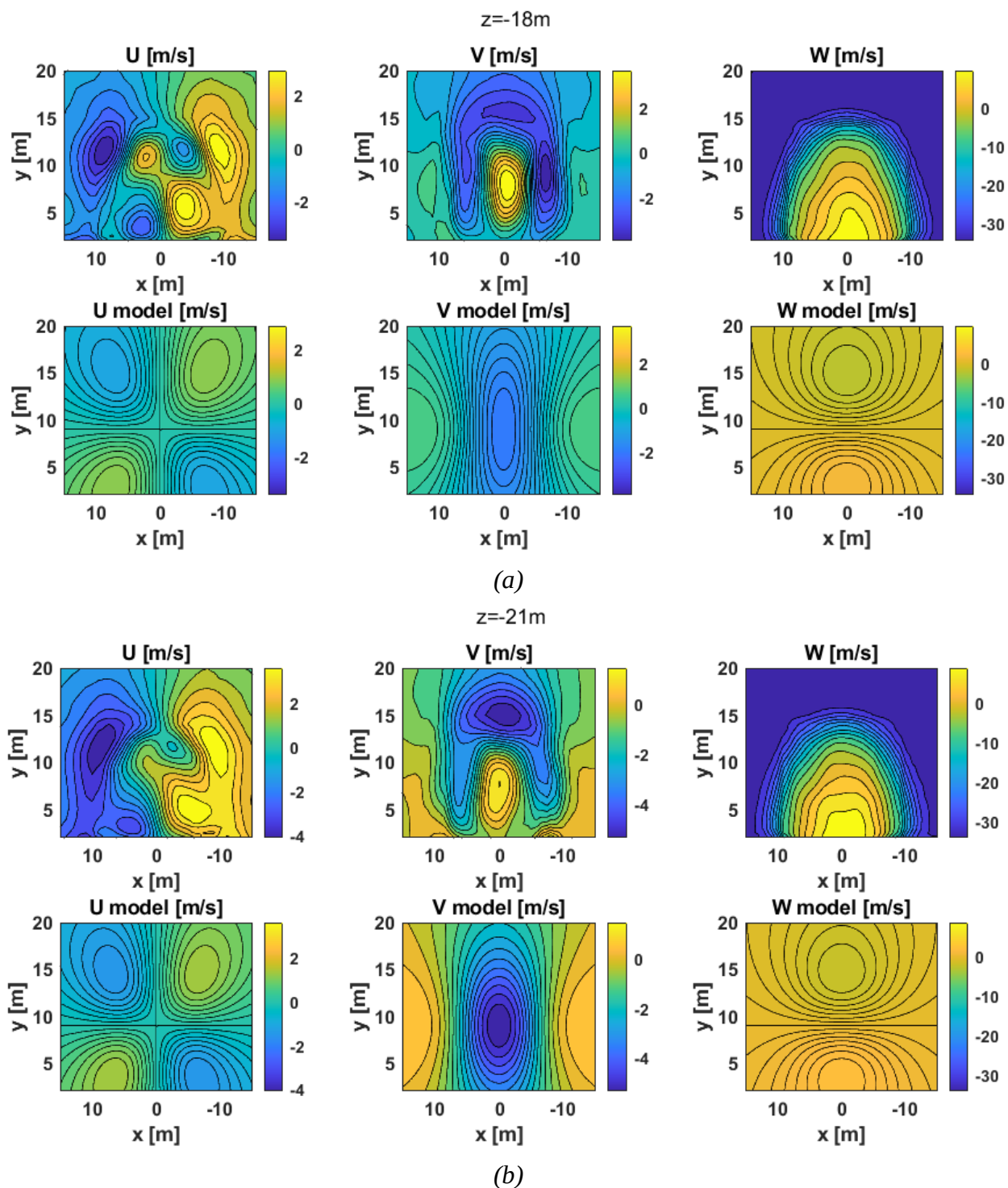


Figure 145: Contours of airwake velocity components aft of the 10m cube for various $z=\text{constant}$ planes obtained using CGE/VorTran-M and aft horseshoe vortex model.

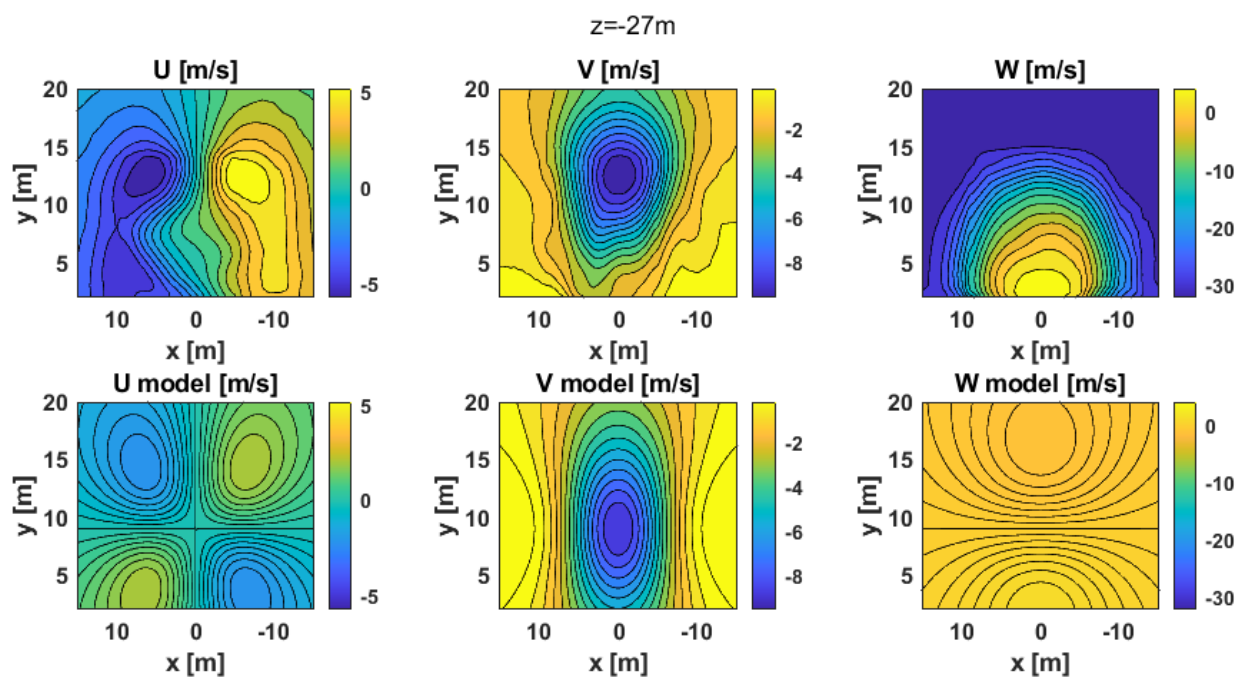
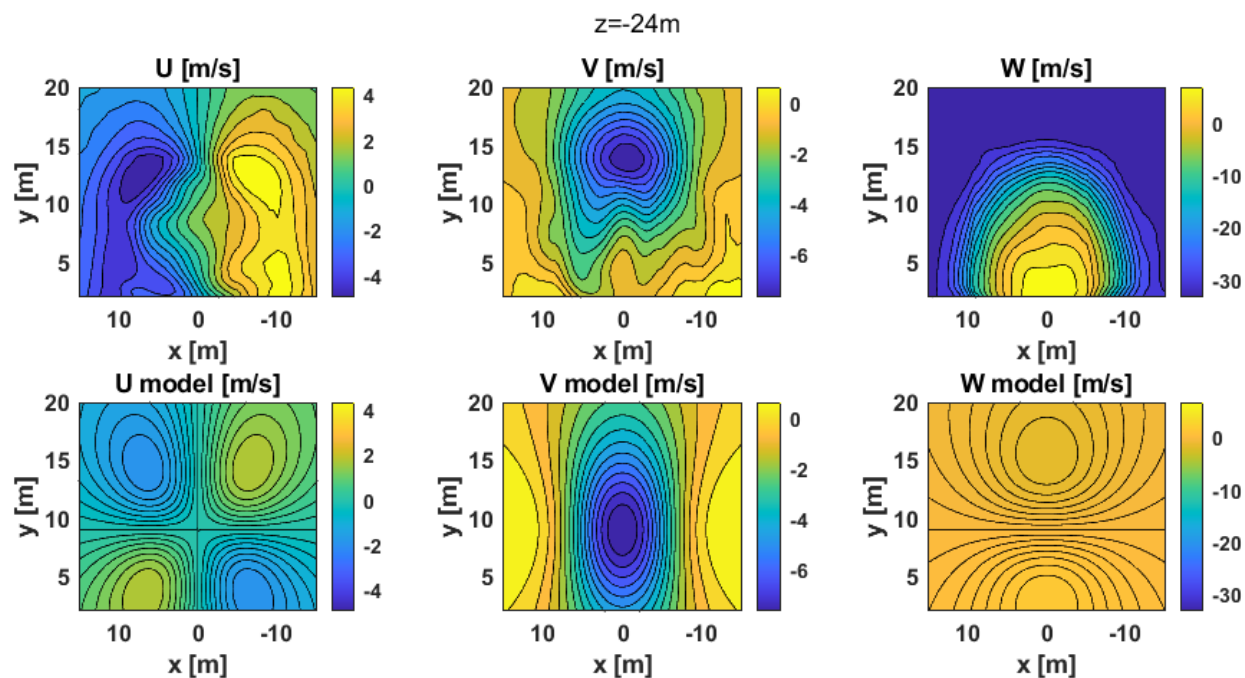
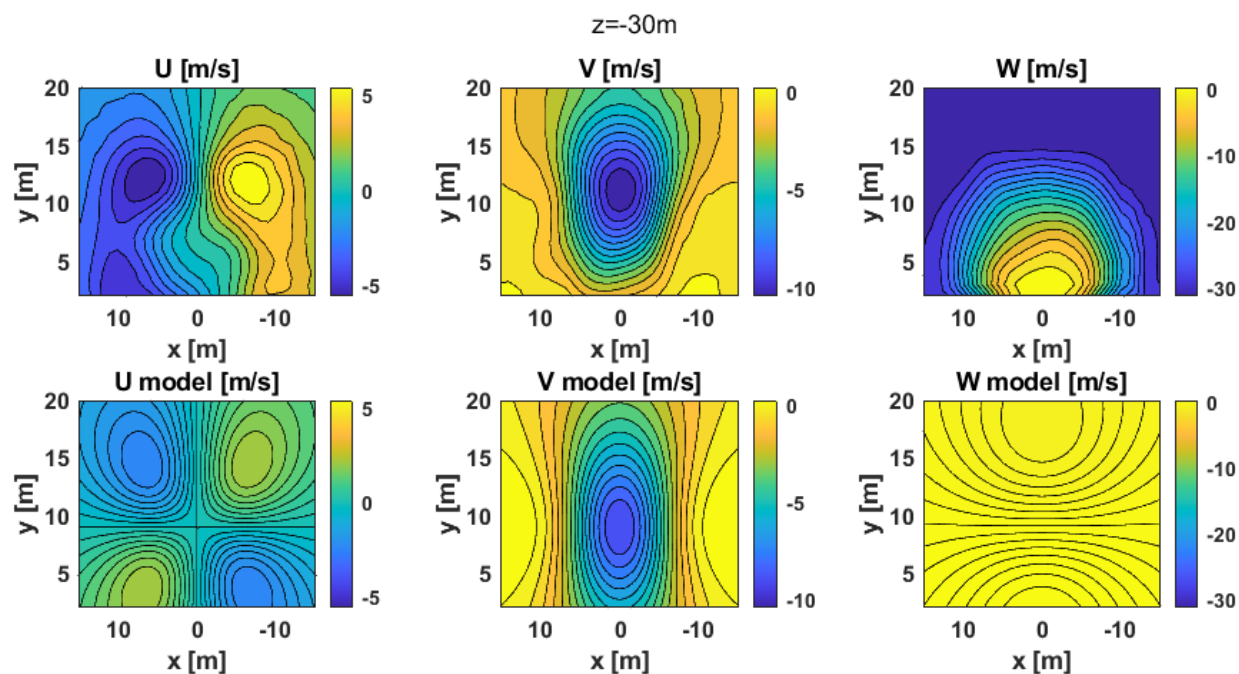
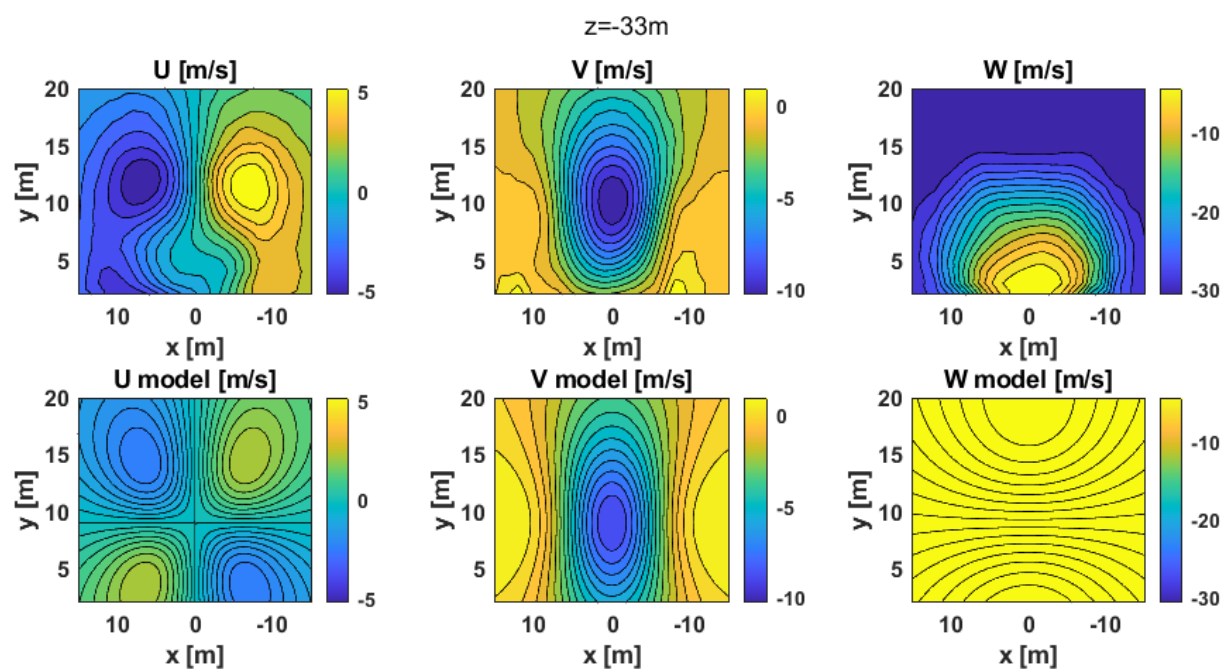


Figure 145: Contours of airwake velocity components aft of the 10m cube for various $z = \text{constant}$ planes obtained using CGE/VorTran-M and aft horseshoe vortex model.



(e)



(f)

Figure 145: Contours of airwake velocity components aft of the 10m cube for various $z=\text{constant}$ planes obtained using CGE/VorTran-M and aft horseshoe vortex model.

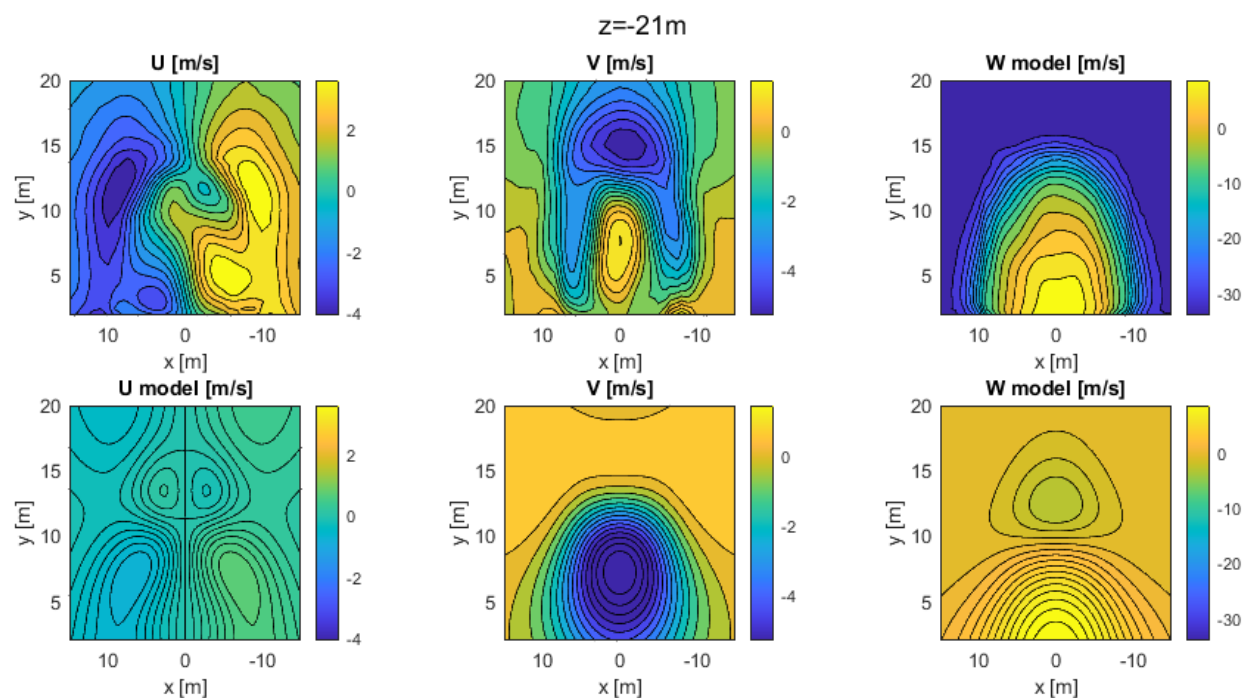
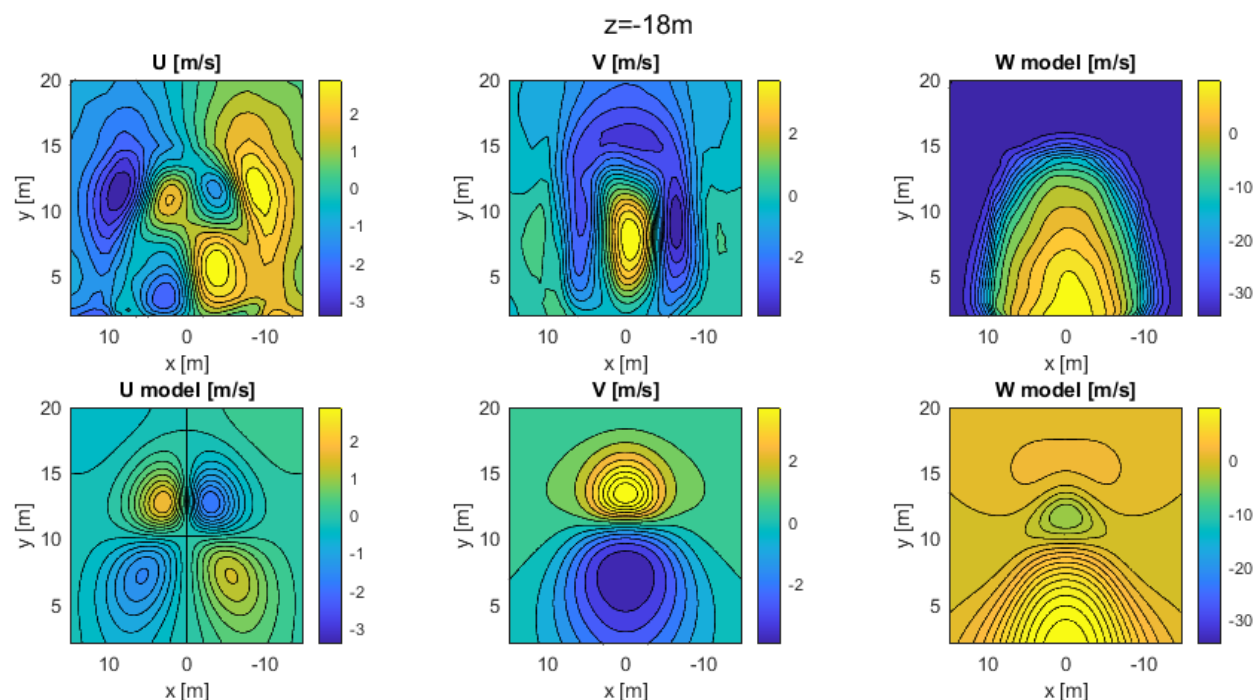
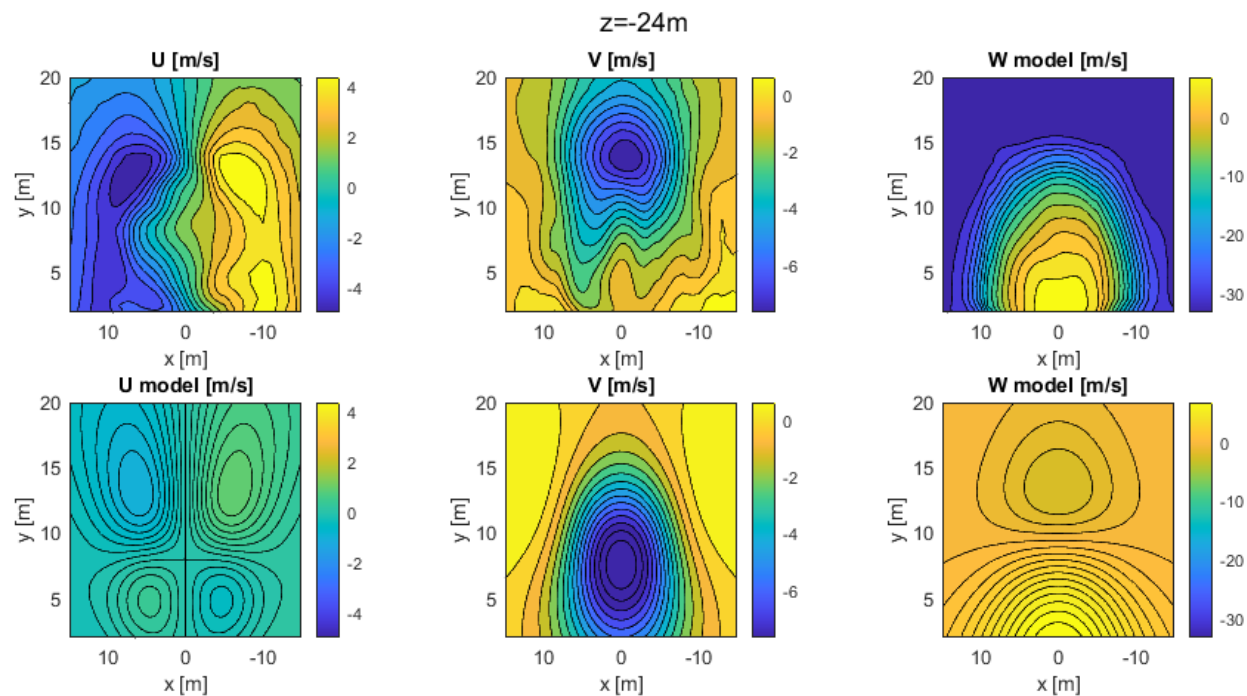
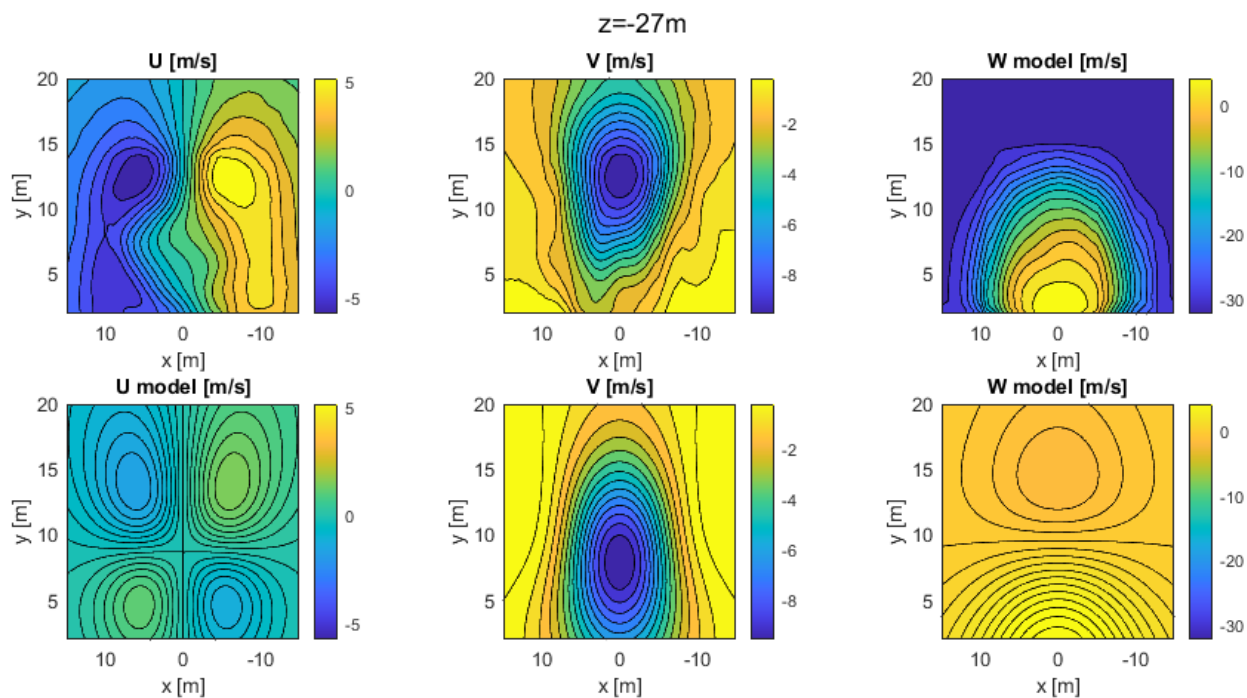


Figure 146: Contours of airwake velocity components aft of the 10m cube for various $z=\text{constant}$ planes obtained using CGE/VorTran-M and superimposed aft arch vortex and horseshoe vortex models.



(c)



(d)

Figure 146: Contours of airwake velocity components aft of the 10m cube for various $z=\text{constant}$ planes obtained using CGE/VorTran-M and superimposed aft arch vortex and horseshoe vortex models.

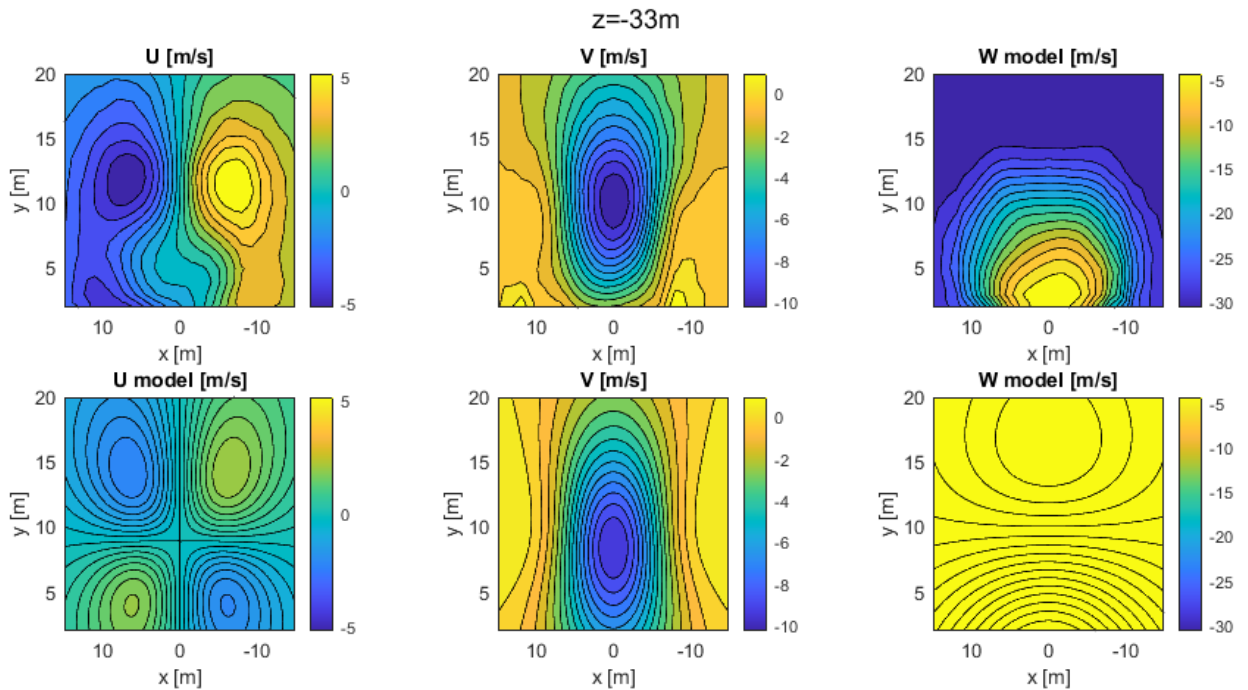
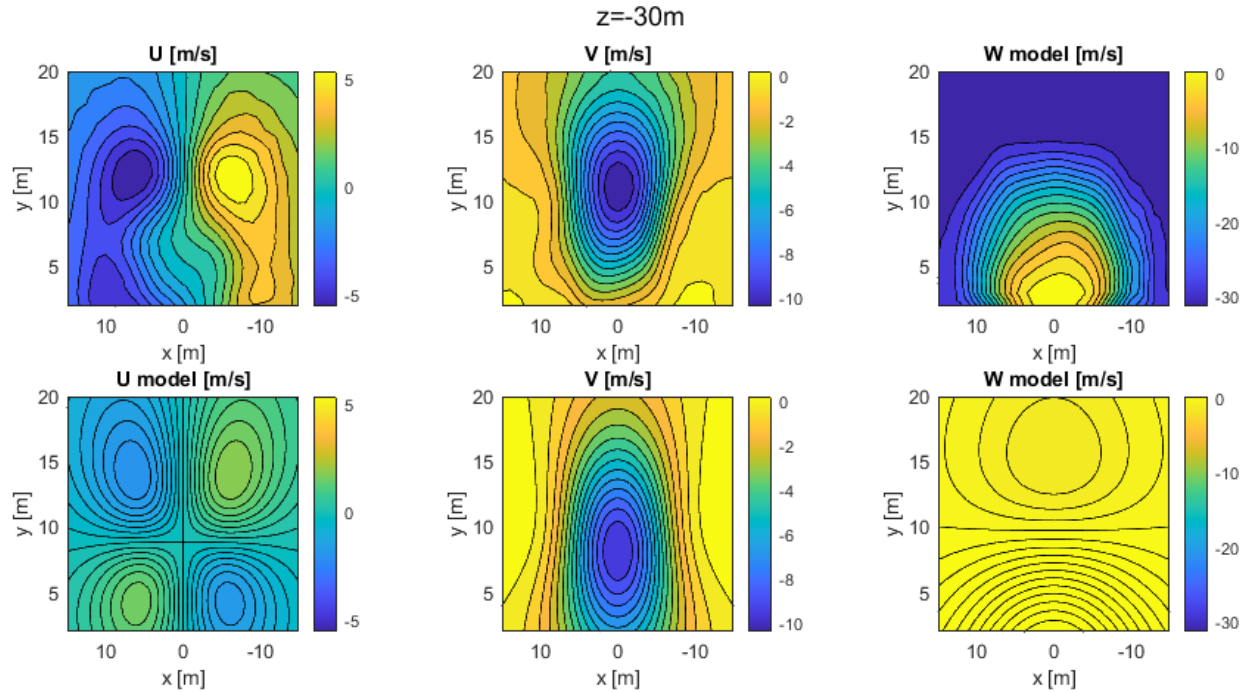


Figure 146: Contours of airwake velocity components aft of the 10m cube for various $z = \text{constant}$ planes obtained using CGE/VorTran-M and superimposed aft arch vortex and horseshoe vortex models.

Only flow features aft of the obstacle have been considered thus far. Over the obstacle, conical vortices were noted to develop along the streamwise corner edges of the obstacle,

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illustrated in Figure 147. However, upon carrying out the characterization steps listed above, these vortices were not found to be significantly potent, displaying dimensional circulation values $<0.5 \text{ m}^2/\text{s}$. The other primary flow features over the obstacle are the separation regions that result from flow hitting the obstacle. As a simple initial attempt to account for these without having to implement a full panel code, we represented the obstacle using a vortex ring consisting of 3 straight filaments that follow the outline of the obstacle in the $z=\text{constant}$ plane, with a corresponding ground mirror filament. We formulated and implemented an optimization problem to determine the longitudinal z -location of this vortex ring along the obstacle length, the size of the vortex core radius and the strength of the filament (the three design variables of the formulation). The objective function entailed minimizing the error in vertical velocity predictions obtained using the model and the truth data along the semi arc shown in Figure 148. The constraints consisted of lower and upper bounds on the design variables. Vertical velocity reconstruction was then carried out using Eq. (35) with $fac = 1$ to evaluate the characterization. Vertical velocity predictions along the $x=0\text{m}$ plane are shown in Figure 149. Reasonable agreement is noted with this first cut attempt. Further refinement of the modeling is underway.

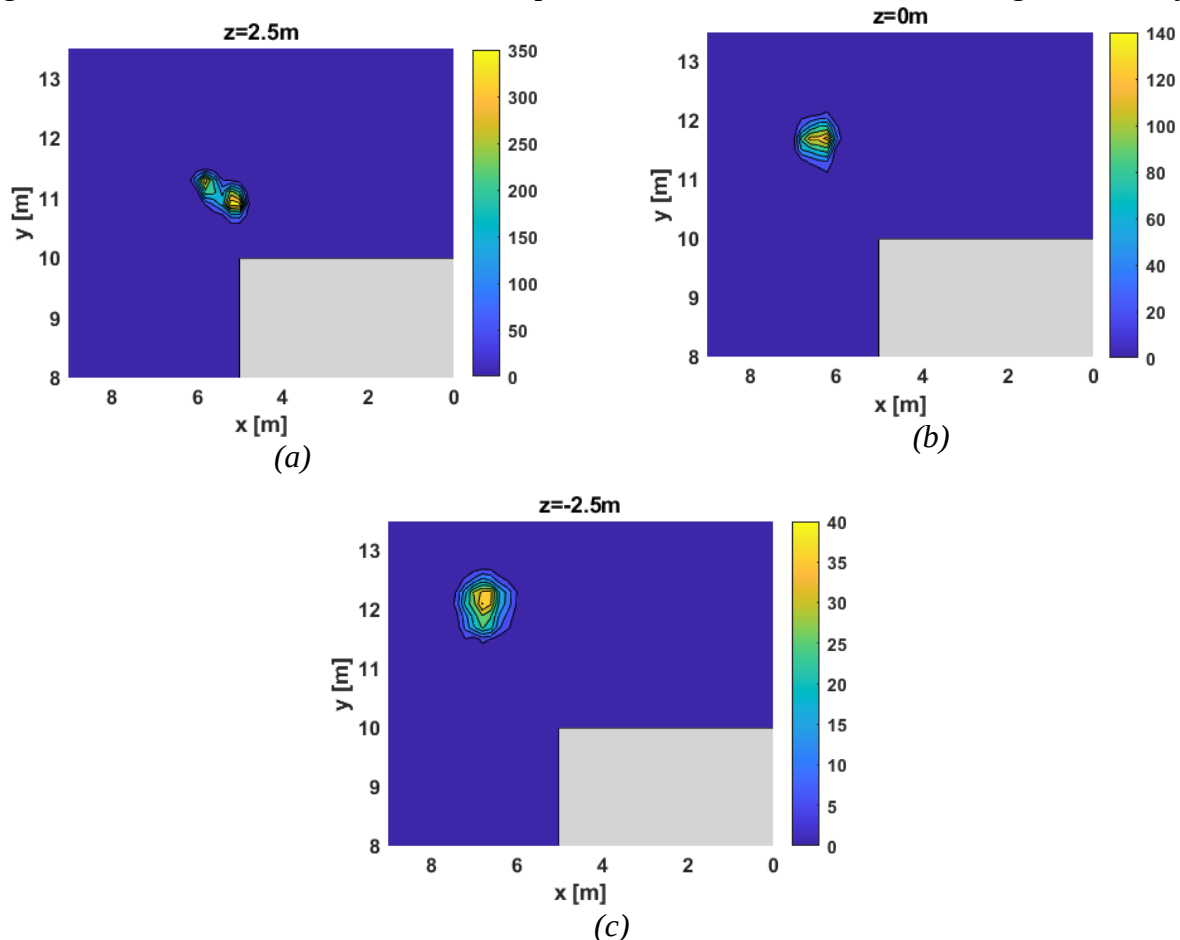


Figure 147: Contour plots of λ_2 vortex identification criterion at various $z=\text{constant}$ planes indicated presented of moving corner vortex.

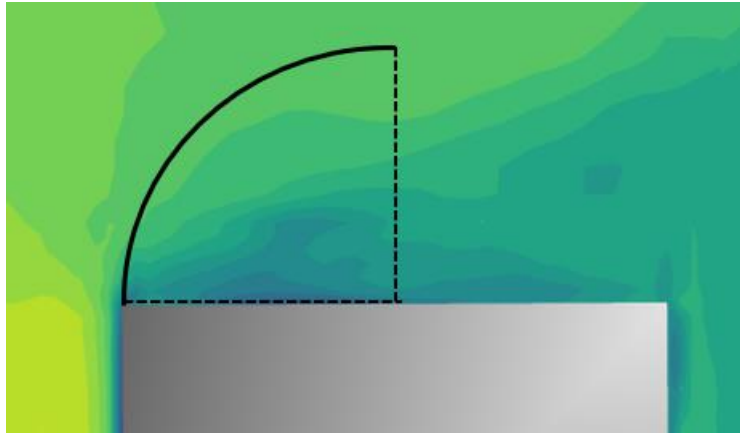


Figure 148: Illustration of arc (black solid line) along which predictions were made for optimization.

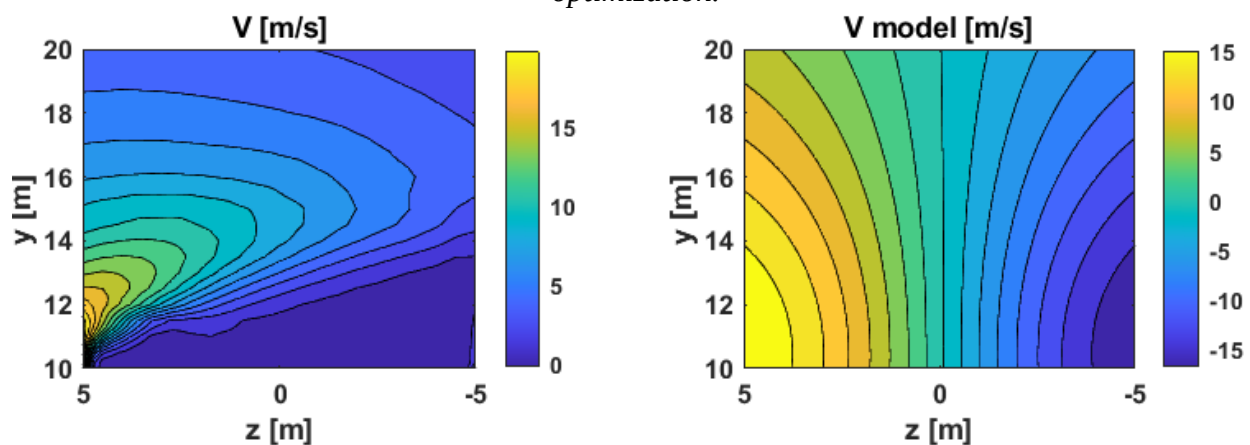


Figure 149: Vertical velocity predictions for region along $x=0m$ plane over the top of the cube using the simplified ring vortex model for the obstacle.

In the analysis presented above, we demonstrated that the steady vertical velocity profile aft of the cube could be adequately captured using a horseshoe vortex with the following limits: $x \in [-0.5, 0.5]W$, $y = 0.9H$ and $z \in [1.5, \infty]H$, where x, y and z follow the coordinate system shown in Figure 150 that has its origin in the center of the bottom face of the obstacle, W is the obstacle width and H the obstacle height. The associated strength of the vortex non-dimensionalized by the product of free stream speed and obstacle height was 1.07 (after the rederivation).

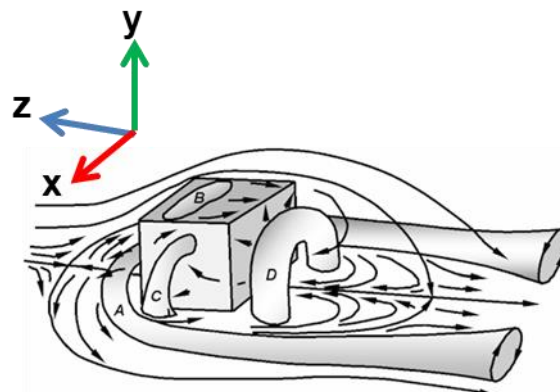


Figure 150: Reference frame and principal flow features around surface-mounted obstacle, adapted from [50]

The horseshoe model was enhanced with a simplified unsteadiness model that allows for reconstruction of time-varying velocities for different geometries. A velocity component u at any time instant t is obtained as a summation of a mean component $\bar{u}$ and a fluctuating component u' . The mean component is obtained using the horseshoe vortex parameters and Eq. (35), while the fluctuating component is computed as:

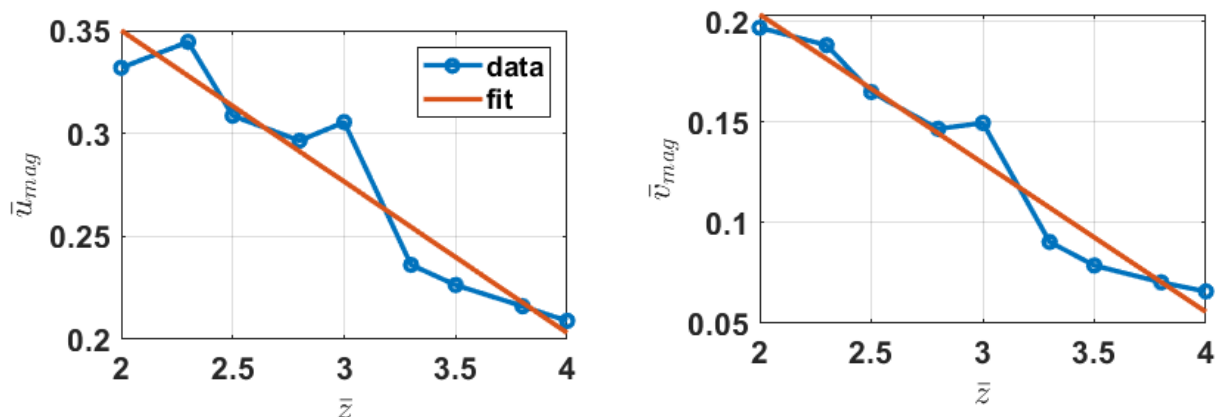
$$u' = U_{mag} \sin 2\pi ft, \quad (37)$$

where U_{mag} is the fluctuation amplitude and f is the vortex shedding frequency obtained from the Strouhal number defined as

$$St = \frac{fb}{U} \quad (38)$$

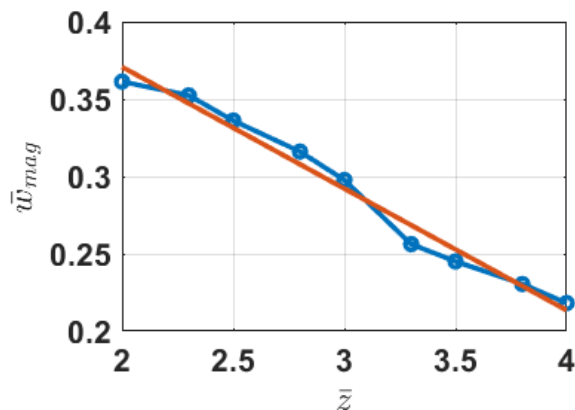
where b is a characteristic length, and U is a characteristic speed. Aft of a cube, results from literature indicate periodic shedding at a characteristic frequency of $St = 0.104$ [52, 88]. We could thus obtain f using this Strouhal value, the cube length and free stream speed using Eq. (38). The fluctuation amplitude U_{mag} at any z location aft of the cube was obtained by scaling the output of a nondimensional function $\bar{U}_{mag}(\bar{z})$ by freestream speed, where $\bar{z}$ is the downstream distance from the trailing face of the cube non-dimensionalized by height H . The $\bar{U}_{mag}(\bar{z})$ function was obtained by fitting a line across the non-dimensional maximum integral time-averaged fluctuation value for each velocity component at different planes downstream of the cube. The data and fits for each velocity component are shown in Figure 151.

To account for the alternating vortex shedding behavior aft of the cube (similar to the Von Karman vortex street), a 90° phase offset was introduced between predictions in the $x < 0$ and $x > 0$ regions of the grid. Contour plots of reconstructed vertical velocity profiles at various time instances are shown in Figure 152 for $\bar{z} = 1.9$, and can be compared to those from CFD in Figure 153. The alternating vortex shedding behavior is apparent in Figure 152, especially for regions away from $x = 0$. At $x = 0$, the mean downwash contribution from the horseshoe vortex tends to dominate. This reconstruction, when compared to those from CFD in Figure 153, is clearly simplistic, but could prove being adequate for gaining fundamental insights into rotorcraft response.



(a) lateral velocity

(b) vertical velocity



(c) longitudinal velocity

Figure 151: Maximum integral time-averaged fluctuation velocity values at different $\bar{z}$ planes; velocities have been nondimensionalized by freestream speed

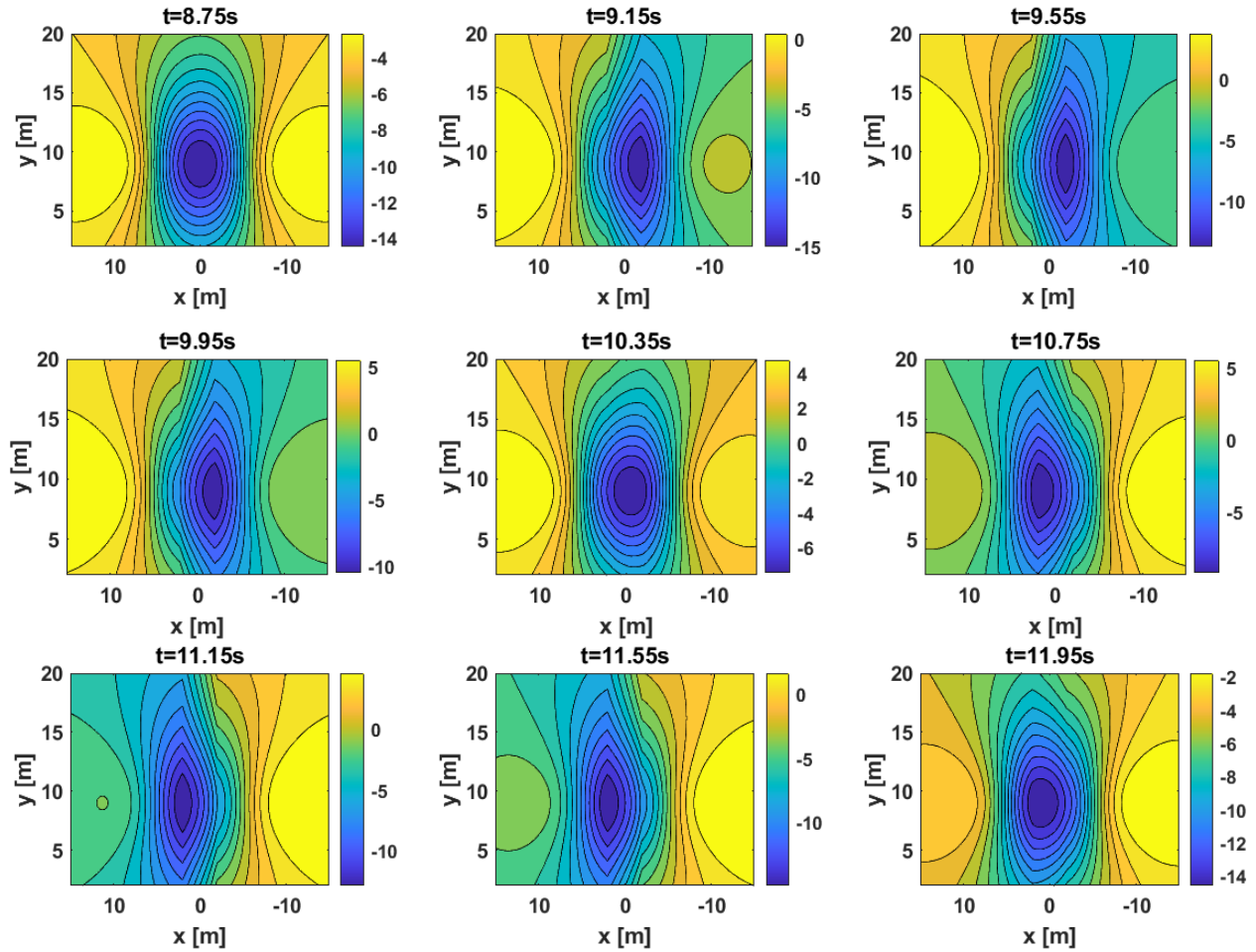


Figure 152: Contours of vertical velocity at various time stamps for $\bar{z} = 1.9$ obtained by summing mean contributions from horseshoe vortex representation and fluctuating contributions from simplified methodology presented in text.

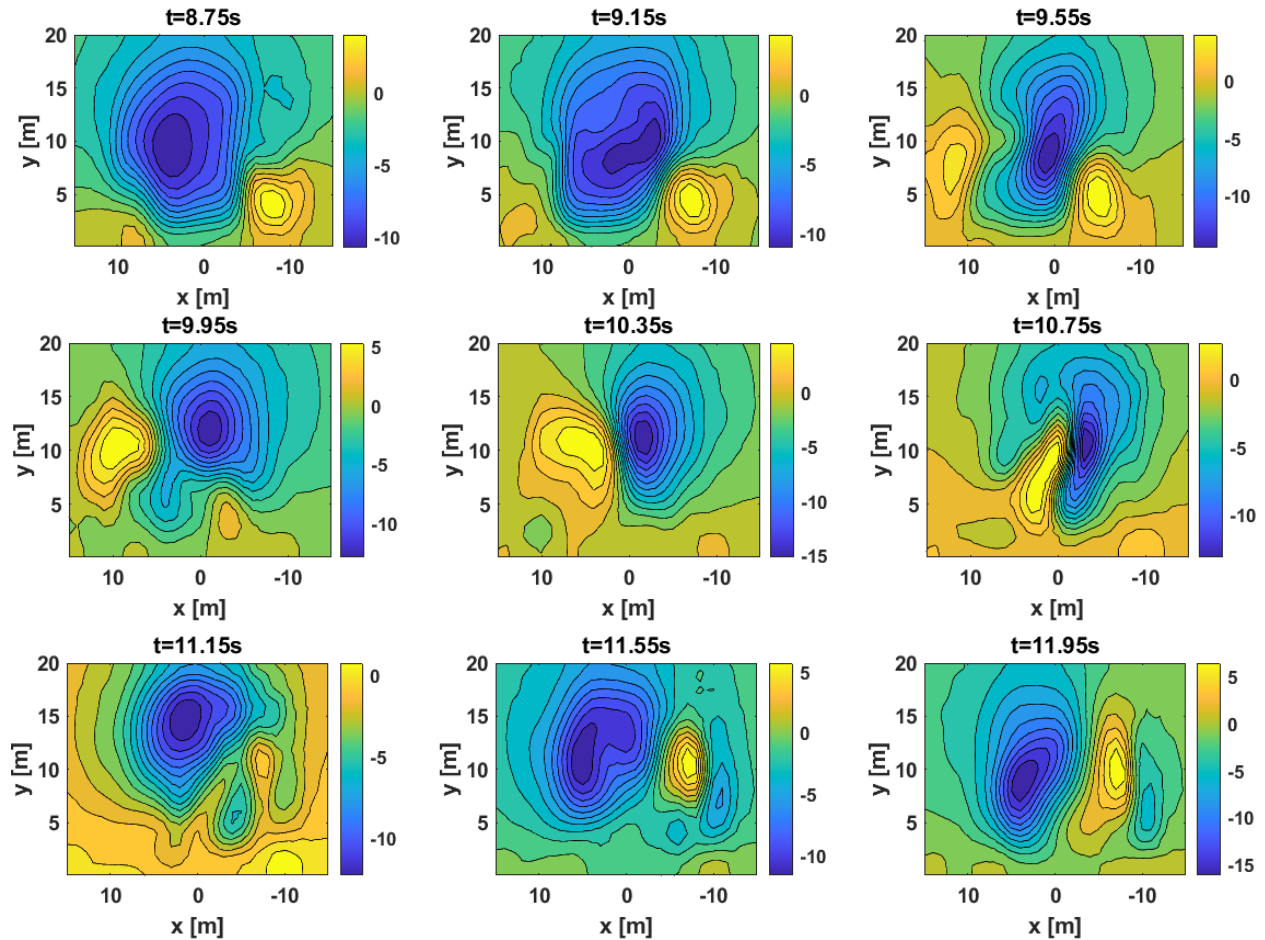


Figure 153: Contours of vertical velocity at various time stamps for $\bar{z} = 1.9$ obtained using CFD.

Generic Ship Geometry Parametrization

As a step up from analyses of flow over a cube, we consider flow over representative ship geometries. We implemented a generic geometry parametrization for ship structures shown in Figure 154. Corresponding parameters for the 5 ships shown in Figure 155 were then derived using information available in online databases. These are summarized in Table 9, where the two categories of ships are evident: flat topped ship geometries (category #1), where the superstructure does not cover the ship beam ($W \neq L_W$) and (category #2) ships where the superstructure width matches the ship beam ($W = L_W$). Nondimensional geometrical relations for the various ships are shown in Table 10, where the nondimensional parameters for the second category are relatively close to each other. In this report, we consider primarily a generic category #2 ship geometry whose dimensions were obtained using the average of the nondimensional relations shown in Table 10, together with a scaling length of $L = 40\text{m}$. The resulting geometry is depicted in Figure 156. We performed a 30 m/s headwind simulation of flow over the resulting generic configuration and recorded transient time history data in 0.1s increments for a total of 25s for a rectangular domain of interest that is aft of the hangar and extends up to $1.4L_a$ aft of the ship in the z direction, $2.4H$ above the hangar in the y direction and $1.25W$ beyond the beam on either side in the x direction.

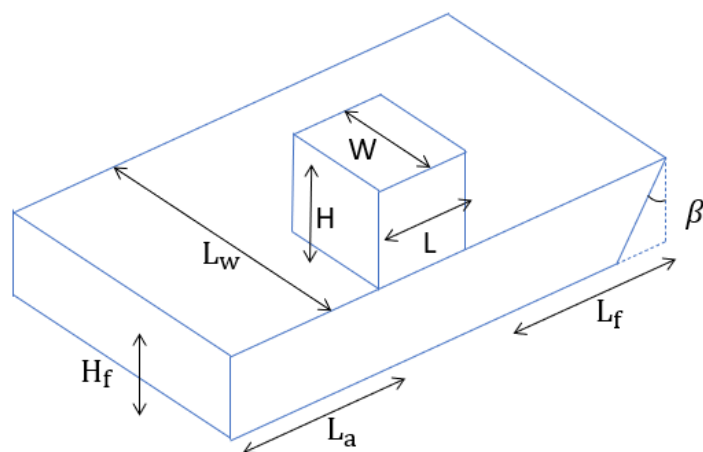


Figure 154: Generic ship geometry parameterization.



(a) CVN class ship



(b) LHD class ship



(c) LPD class ship



(d) DDG class ship



(e) LSD class ship

Figure 155: Different ship classes considered in parameterization.

Table 9: Parametrization of different ship geometries.

Geometry	L [m]	W [m]	H [m]	L_w [m]	L_f [m]	L_a [m]	H_f [m]	L_{tot} [m]	β [deg]
cube	10	10	10						
CVN	27.8	25	27.8	77	191.5	113.8	19.4	333.0	30
LHD	85.7	15	19.3	32	85.7	85.7	19.5	257.1	30
LPD	116	32	10.4	32	29.5	62.6	12.2	208.5	
DDG	64.6	20	14.2	20	43.9	46.5	7.8	155	
LSD	93	26	12.4	26	27.9	65.1	12.4	186	

Table 10: Nondimensionalization of ship geometry dimensions.

	L/W	L/H	W/ L_w	H/ H_f	L/ L_f	L/ L_a	L/ L_{tot}
cube	1.00	1.00					
CVN	1.11	1.00	0.32	1.43	0.14	0.24	0.08
LHD	5.71	4.44	0.47	0.99	1.00	1.00	0.33
LPD	3.64	11.19	1.00	0.85	3.95	1.86	0.56
DDG	3.23	4.55	1.00	1.82	1.47	1.39	0.42
LSD	3.58	7.50	1.00	1.00	3.33	1.43	0.50

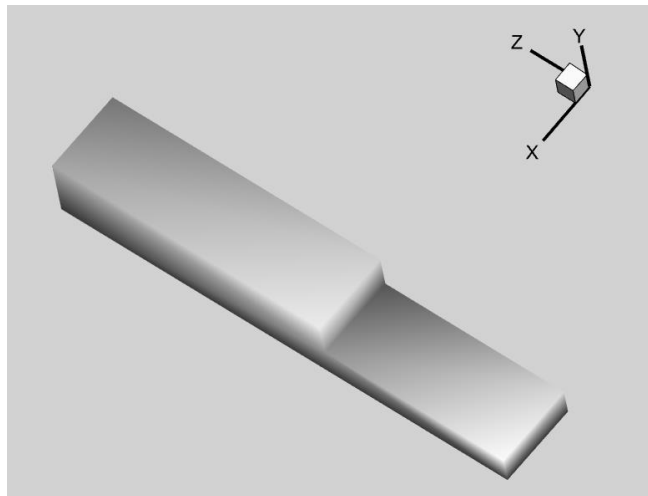


Figure 156: Rendering of generic shape geometry.

Headwind Over Generic Ship

Since the domain of interest is aft of the superstructure, we hypothesized that the arch vortex and horseshoe vortex described previously for flow aft of the cube would be applicable here as well with some corrections. In this report, we test out the scalability of the horseshoe filament since this flow feature was shown to adequately capture the vertical velocity component of ship airwake, which directly influences rotor inflow. The nondimensional spatial limits of the horseshoe vortex were dimensionalized using the ship beam ($W = 11.4\text{m}$) for x coordinates and hangar height ($H = 5.7\text{m}$) for y and z coordinates. The nondimensional circulation was dimensionalized using the freestream speed and hangar height. Figure 157 shows contours of the

velocity predictions for the horseshoe vortex obtained using Eq. (35) **Error! Reference source not found.** together with those from CGE/VorTran-M. Note that the deck position coordinates are bounded dimensionally within $z \in [-33.33, -6.67]\text{m}$ and the horizontal part of the horseshoe is at $z=-14.4\text{m}$. Apart from the regions near the horizontal part of the horseshoe at $z=-11\text{m}$ and $z=-15\text{m}$, good agreement in lateral u and vertical v velocity predictions are noted, showcasing the applicability of this horseshoe vortex representation to simplified ship superstructures. Note that these results were obtained with the nondimensional vortex core radius scaled by ship beam. We also investigated the applicability of the characterization by scaling the nondimensional viscous core radius by hangar height H . This scaling also generates reasonable vertical velocity predictions as indicated in Figure 158.

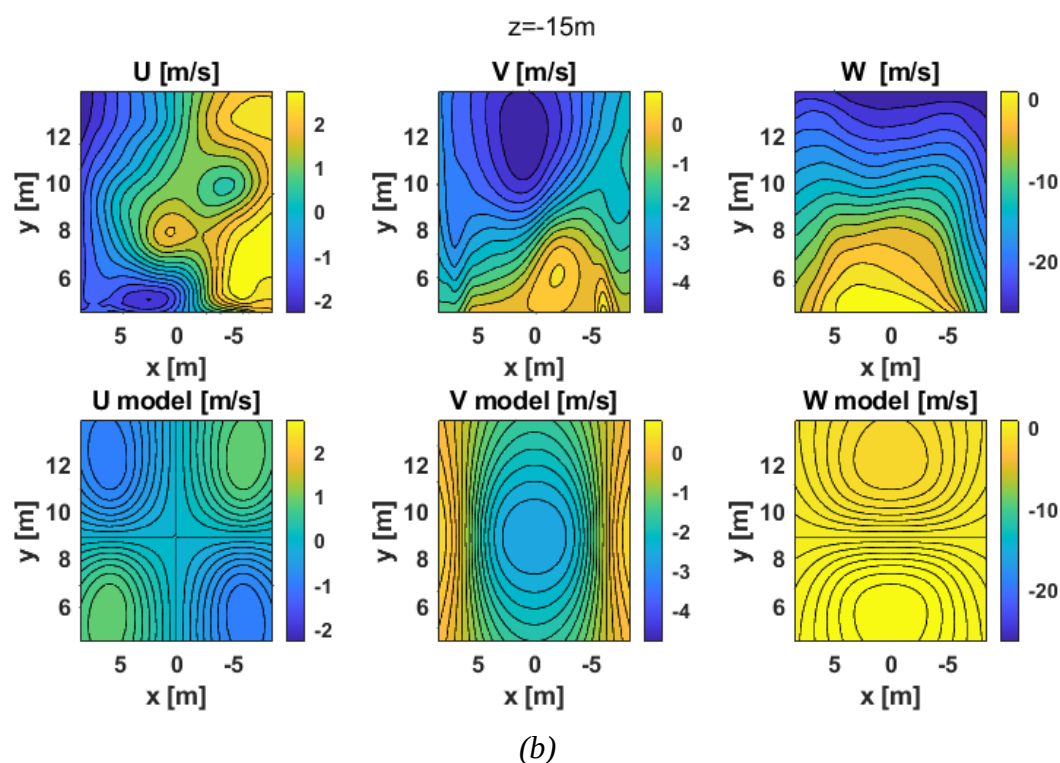
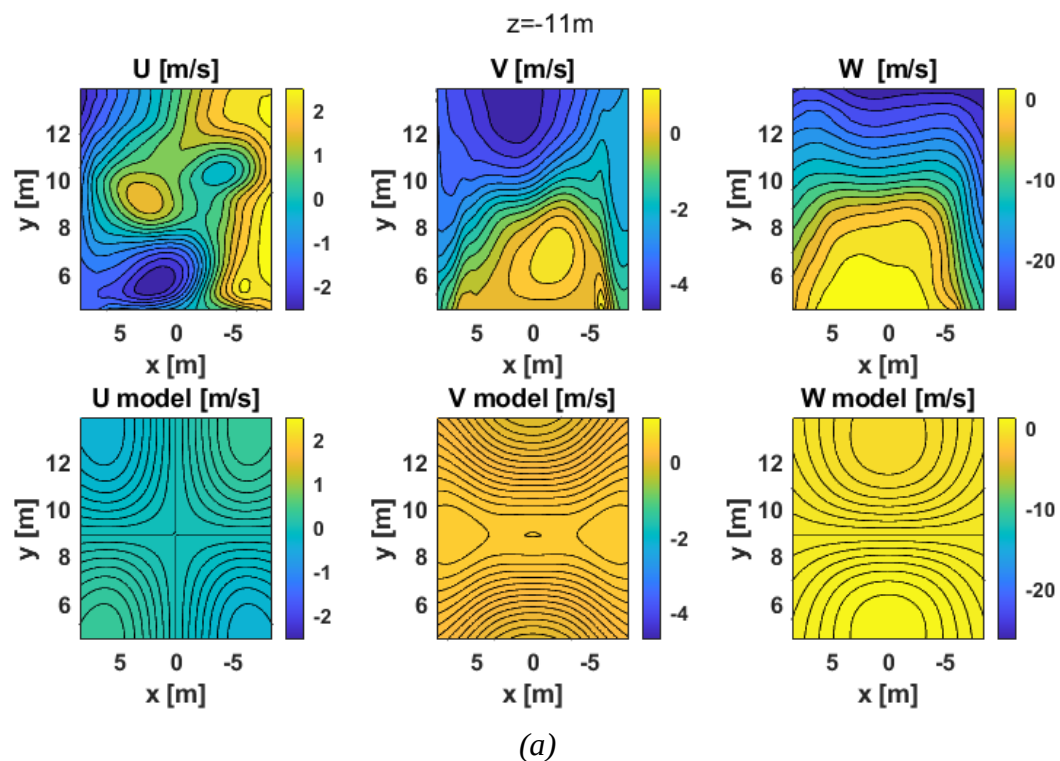


Figure 157: Contours of airwake velocity components aft of the generic ship geometry for various $z=\text{constant}$ planes obtained using CGE/VorTran-M and aft horseshoe vortex model.

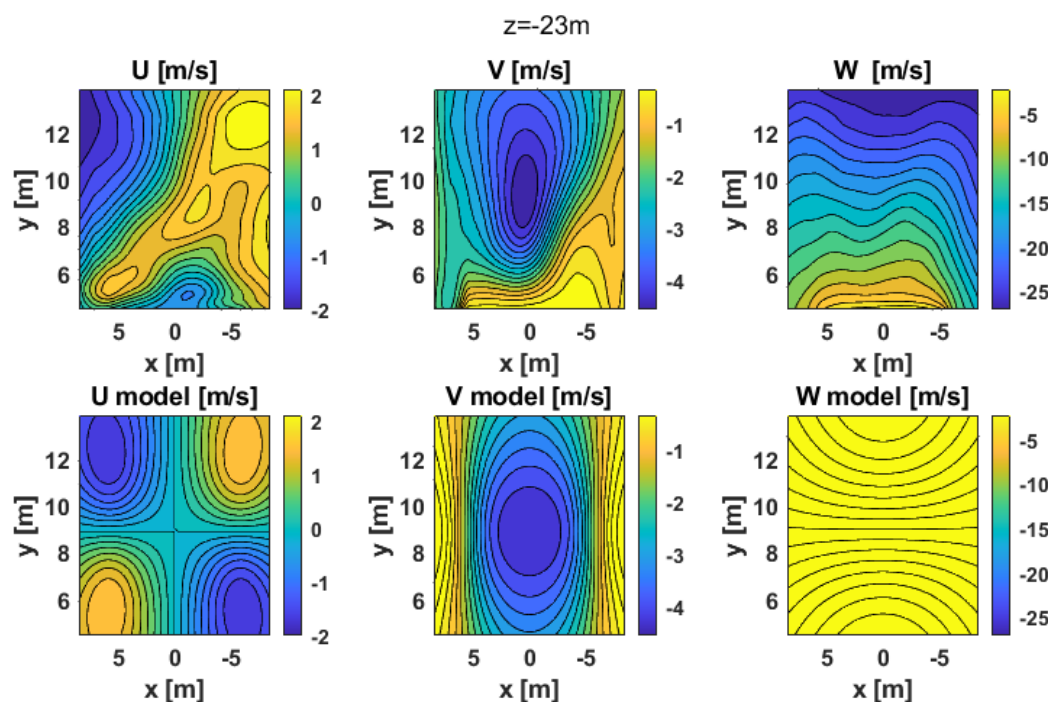
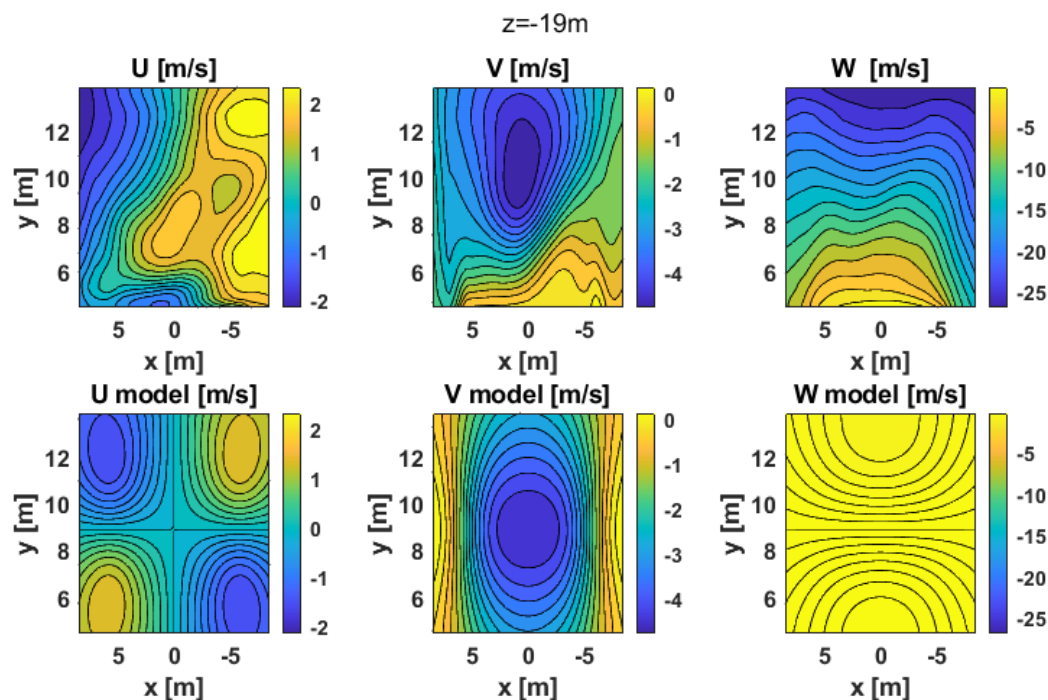
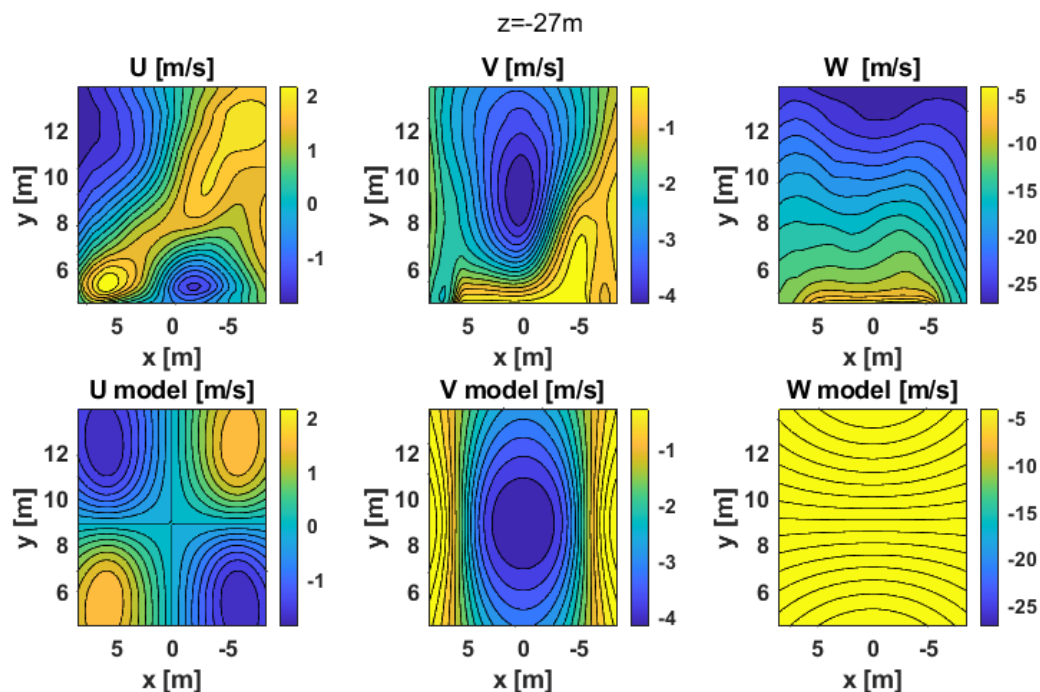
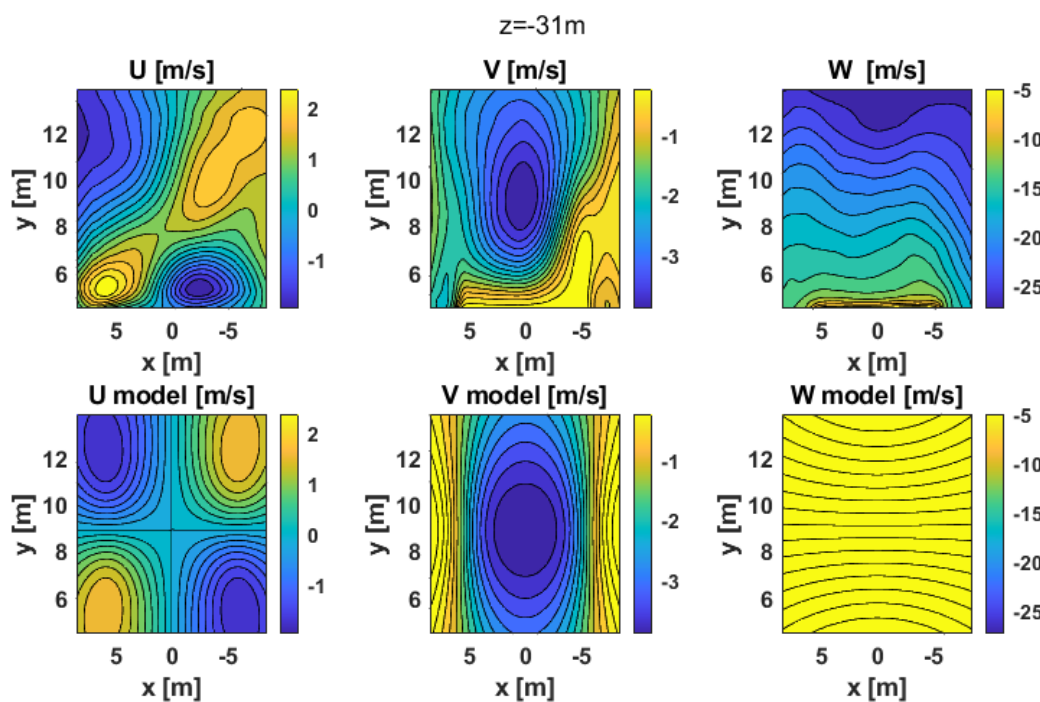


Figure 157: Contours of airwake velocity components aft of the generic ship geometry for various $z=\text{constant}$ planes obtained using CGE/VorTran-M and aft horseshoe vortex model.

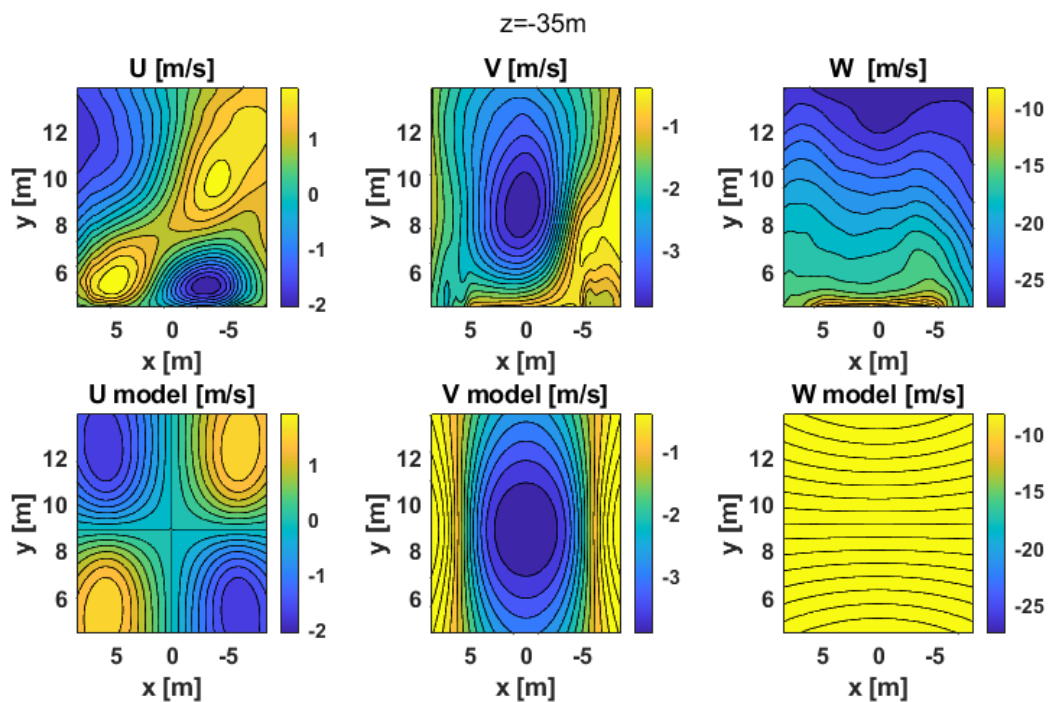


(e)

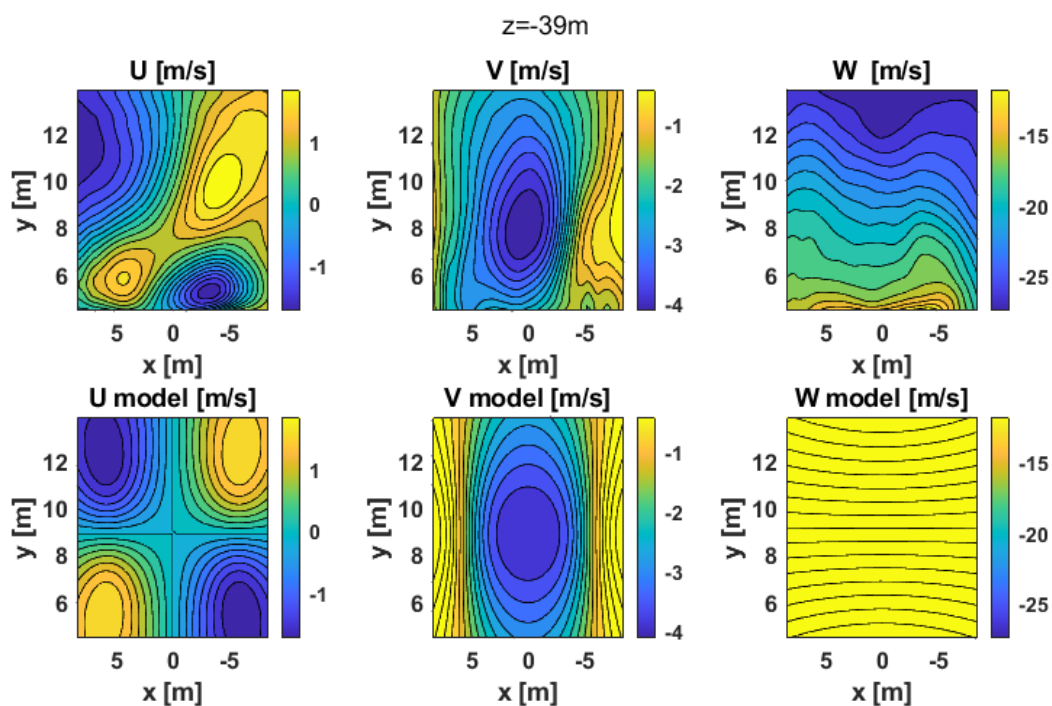


(f)

Figure 157: Contours of airwake velocity components aft of the generic ship geometry for various $z = \text{constant}$ planes obtained using CGE/VorTran-M and aft horseshoe vortex model.



(g)



(h)

Figure 157: Contours of airwake velocity components aft of the generic ship geometry for various $z=\text{constant}$ planes obtained using CGE/VorTran-M and aft horseshoe vortex model.

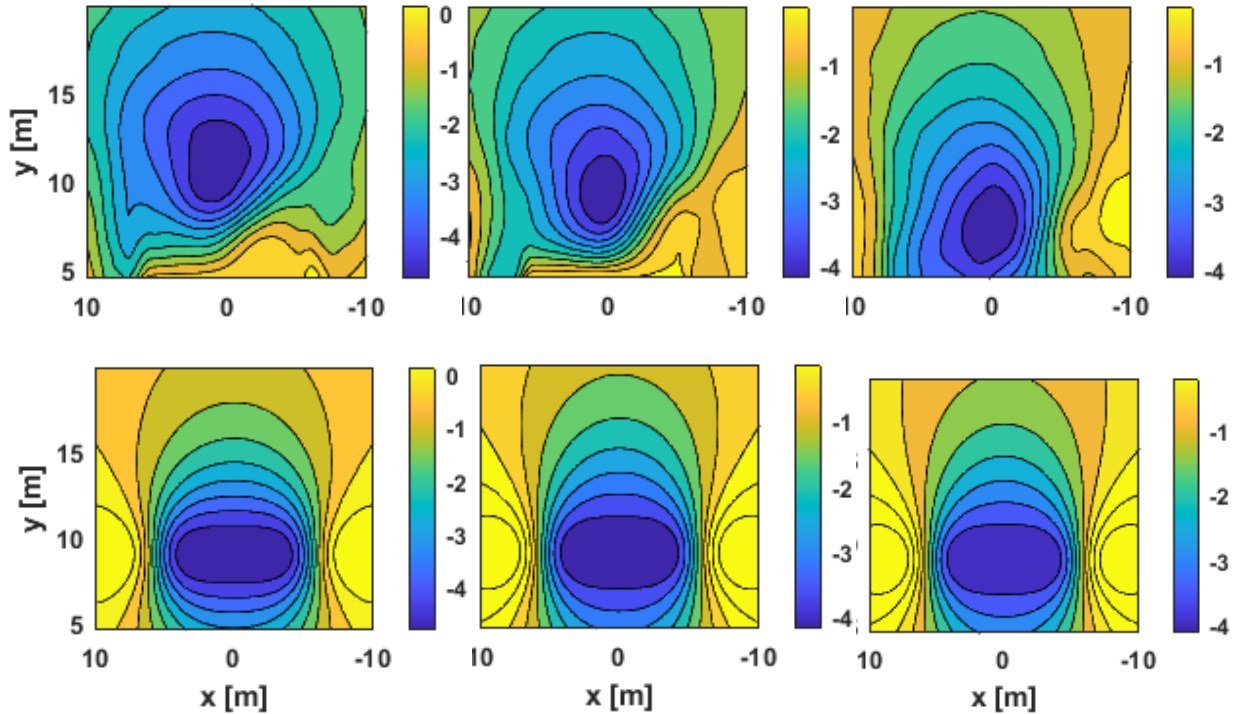


Figure 158: Contours of vertical velocity aft of generic ship at $z = [-18, -27, -45]$ m (from left to right, respectively) in presence of a headwind; time-averaged CFD (top) and horseshoe vortex model (bottom); $+x$ is portside, $-x$ is starboard and y represents height above sea level (deck is at $y = 4.3$ m)

Quantifying Fundamental Influence of Wake Structures on Rotorcraft

The influence of an arbitrary vortex wake structure on the fundamental response of a rotor can be derived using Blade Element Theory (BET) analysis [5, 7]. Detailed derivation of these equations is provided in Ref. [7]. We focus here on the assumptions made and resulting expressions.

Consider an “idealized” rotor with the following assumptions:

- No hub accelerations;
- Controls remain fixed;
- No hinge offset;
- Quasistatic response whereby the flap response of the rotor is much faster than the speed at which the vortex structure traverses the rotor; and
- Only the vertical component of the velocity associated with the arbitrary vortex is considered since it directly influences rotor inflow.

The influence of an arbitrary vortex with nondimensional vertical velocity $v_{vortex}(r, \psi)$, where r is the radial coordinate and ψ is the rotor azimuth, on rotor thrust coefficient C_T , coning β_0 , lateral disc tilt β_{1s} and longitudinal disc tilt β_{1c} is given by the following expressions [7]:

$$C_T = C_T^* - \frac{Nca}{4\pi^2} \int_0^{2\pi} \int_0^{R1} v_{vortex}(r, \psi) (r + \mu \sin \psi) dr d\psi, \quad (39)$$

$$\begin{aligned}\beta_0 &= \beta_0^* - \frac{ca}{4\pi^2\omega^2 I_\beta} \int_0^{2\pi} \int_0^1 v_{vortex}(r, \psi)(r^2 + \mu r \sin \psi) dr d\psi, \\ \beta_{1s} &= \beta_{1s}^* + \frac{ca}{2\pi^2(\omega^2 - 1)I_\beta} \int_0^{2\pi} \int_0^1 v_{vortex}(r, \psi)(r^2 \sin \psi + \mu r \sin^2 \psi) dr d\psi, \\ \beta_{1c} &= \beta_{1c}^* + \frac{ca}{2\pi^2(\omega^2 - 1)I_\beta} \int_0^{2\pi} \int_0^1 v_{vortex}(r, \psi)(r^2 \cos \psi + \mu r \cos \psi \sin \psi) dr d\psi,\end{aligned}$$

where $()^*$ terms represent the responses without the vortex, N is the number of rotor blades, a is the airfoil lift-curve slope, c is the blade chord length nondimensionalized by rotor radius R , ω is the flap natural frequency nondimensionalized by rotor rotational speed Ω ($\omega = 1$ for zero hinge offset), and I_β is the blade flapping inertia nondimensionalized by ρAR^3 , where A is the rotor disc area. For a specified vortex velocity function $v_{vortex}(r, \psi)$, the corresponding influence on rotor fundamental response can therefore be quantified using the expressions in Eq. (39).

Rotor Fundamental Response to an Isolated Vortex

As described at the beginning of the document, research on the impact of fixed-wing wake vortices on rotorcraft response motivated prior work [7], which investigated the effect of disturbance modeling fidelity on rotor fundamental response using idealized vortex representations. Fidelity levels for the interaction ranged from analytical idealizations and frozen field models to fully distorting CFD models solving the Eulerian form of the incompressible Navier-Stokes equations. In this section, we model a similar idealized vortex to examine the analytical responses given by Eq. (39) for the four helicopter rotors presented in Table 11. The idealized vortex is depicted in Figure 159 and represented as [7]

$$\begin{aligned}v_{vortex}/v_c &= 1.359 \frac{r}{r_c} e^{-0.0001\left(\frac{r}{r_c}\right)^4} \text{ if } 0 \leq \frac{r}{r_c} \leq 0.60653, \\ v_{vortex}/v_c &= \frac{1 + \ln(r/r_c)}{r/r_c} e^{-0.0001\left(\frac{r}{r_c}\right)^4} \text{ if } \frac{r}{r_c} > 0.60653,\end{aligned}\tag{40}$$

where r represents the radial distance from the vortex center nondimensionalized by rotor radius R , r_c is the nondimensional viscous core radius and v_c is the peak tangential velocity of the core nondimensionalized by the product of rotor rotation speed Ω and rotor radius R . The associated velocity distribution is shown in Figure 160. Note that vortex direction influences the results; a positive vortex, which produces an upflow on the advancing side of the main rotor, increases rotor thrust while a negative vortex, which induces an upflow on the retreating side of the blade, decreases rotor thrust. We consider a positive vortex here. Rotor responses following Eq. (39) for the rotors aligned with the vortex as shown in Figure 159 are shown in Figure 161 and Figure 162 for various values of advance ratio μ (forward speed nondimensionalized by ΩR). Similar to results from [7], the BET based analytical results predict a linear increase in response with increasing forward speed. It is also worth noting that the nondimensional blade loading parameter $\Delta C_T/\sigma$, where $\Delta C_T = C_T - C_T^*$ and σ is the blade solidity, “collapses” all the rotor analytical responses onto each other in Figure 162, highlighting its potential as a useful scaling parameter that can provide insights into fundamental rotor response to disturbances. In Figure 161, the MQ-8C and UH-60 rotor responses are close to each other, due to similar disk loading

and solidity values (see Table 11). The validity of the trends seen in Figure 161 and Figure 162 will be tested using dynamic coupled airframe-rotor models later in the report, where we will investigate the influence of both frozen and distorting vortex effects using a Lagrangian disturbance modeling.

Table 11: Rotor properties considered in fundamental response analysis.

Vehicle	Number of blades	Chord [ft]	Radius [ft]	Rotation speed [rad/s]	GW [lbs]	Ibeta [slug-ft ²]	Solidity	Disk loading [lbs/ft ²]
MQ-8C Fire scout (Bell 407)	4	0.9	17.5	43.25	5400	143.8	0.065	5.6
UH-60	4	1.73	26.83	27	16500	1512.6	0.082	7.3
V-22	3	2.09	19	41.57	39500	721.6	0.105	17.4
CH-53E	7	2.44	39.5	18.53	60000	6744.7	0.138	12.2

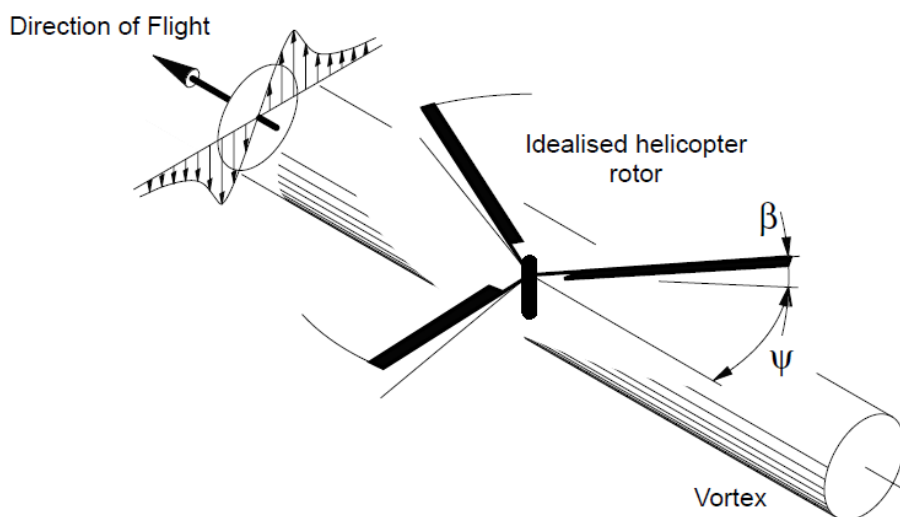


Figure 159: Depiction of idealized vortex[7].

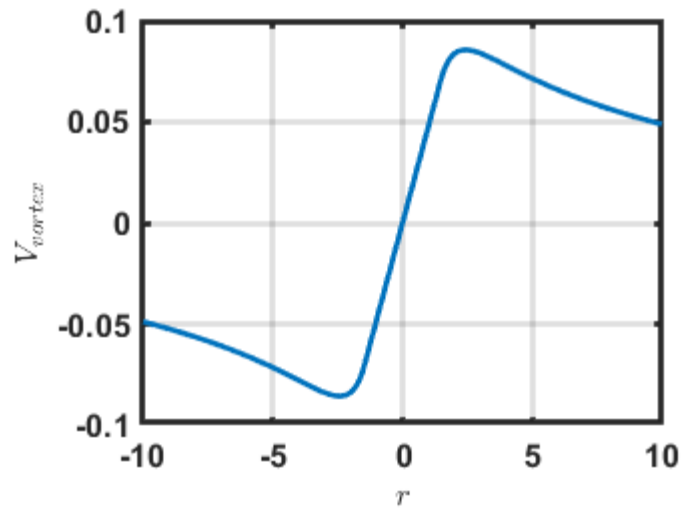


Figure 160: Idealized vortex velocity distribution.

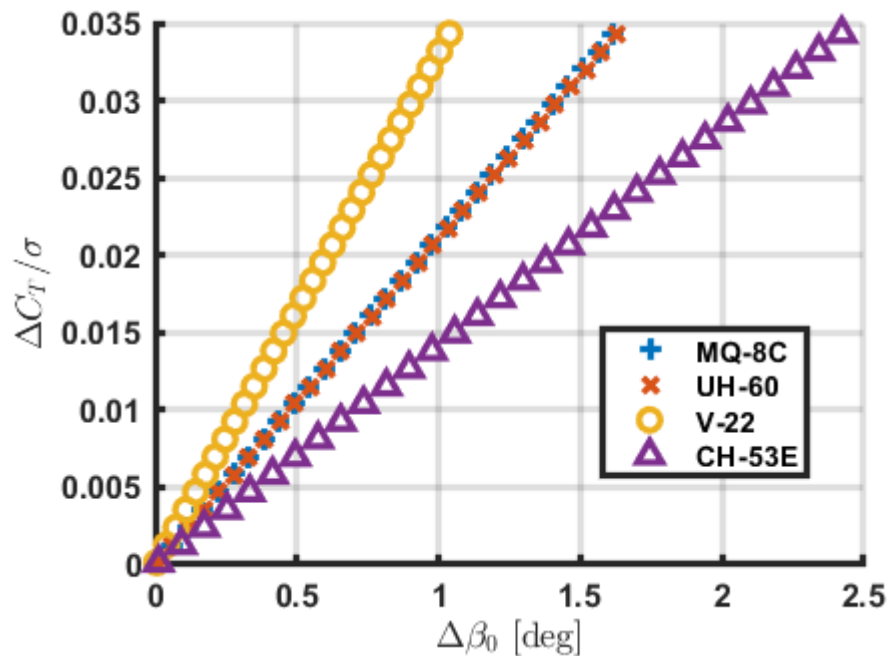


Figure 161: Influence of idealized vortex on rotor response for increasing advance ratios;

$$C_T^* = W/\rho(\pi R)^2(\Omega R)^2, \Delta C_T = C_T - C_T^*, \Delta\beta_0 = \beta_0 - \beta_0^*.$$

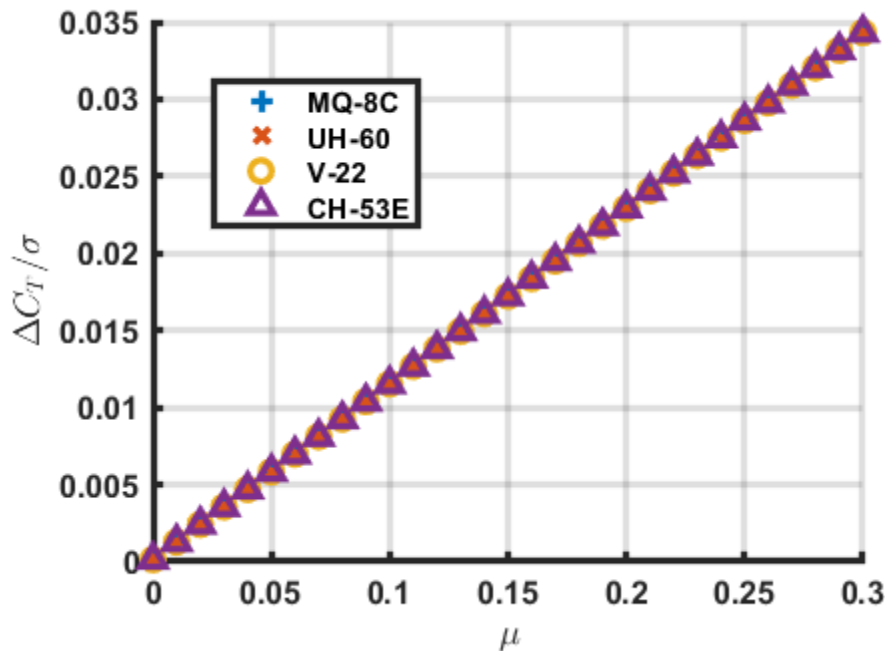


Figure 162: Influence of idealized vortex on rotor thrust coefficient for increasing advance ratios; $C_T^* = W/\rho(\pi R)^2(\Omega R)^2$, $\Delta C_T = C_T - C_T^*$, σ is blade solidity.

Rotor Fundamental Response to an Arch Vortex Disturbance

Earlier, we derived a velocity model for the arch vortex that develops aft of rectangular obstacles in headwind flow. This model is equivalent to the function $v_{vortex}(r, \psi)$ from Eq. (39) and enables the computation of fundamental rotor response to the airwake velocities associated with the arch vortex. For the fundamental response analysis, we consider the rotors in Table 11. The expressions in Eq. (39) are evaluated using the rotor properties from Table 11 and the arch vortex model vertical velocity predictions obtained using Eq. (35). **Error! Reference source not found..** We consider two locations for the rotor hub center in our analyses: $[x, y, z] = [0, 8, -20]$ m (location #1) and $[x, y, z] = [0, 6, -25]$ m (location #2), both defined relative to the coordinate system where x points port, y points up and z points fore

The intensity of the arch vortex influence on the different rotors is depicted in Figure 163 for hover ($\mu = 0$). Points that lie furthest from the origin indicate the strongest influence. At location #1, the influence of the arch vortex on the smaller MQ-8C rotor is strongest, followed in order of decreasing influence by the UH-60A rotor, the V-22 rotor and the CH-53E rotor. At location #2, the MQ-8C is again the most affected rotor, but this time, we note a greater arch vortex influence on the responses of UH-60A and CH-53E rotors. The influence of ship airwake flow features on rotor responses is thus characterized by both rotor properties (size, number of blades etc.) and position. Contours of velocity predictions using the arch vortex model and CGE/VorTran-M over the MQ-8C and CH-53E rotors (smallest and largest rotors considered, respectively) at locations #1 and #2 are shown in Figure 164 and Figure 165, respectively, for reference. Reasonable agreement in velocity predictions is noted with the largest discrepancies noted for the vertical velocity predictions, as was noted previously as well.

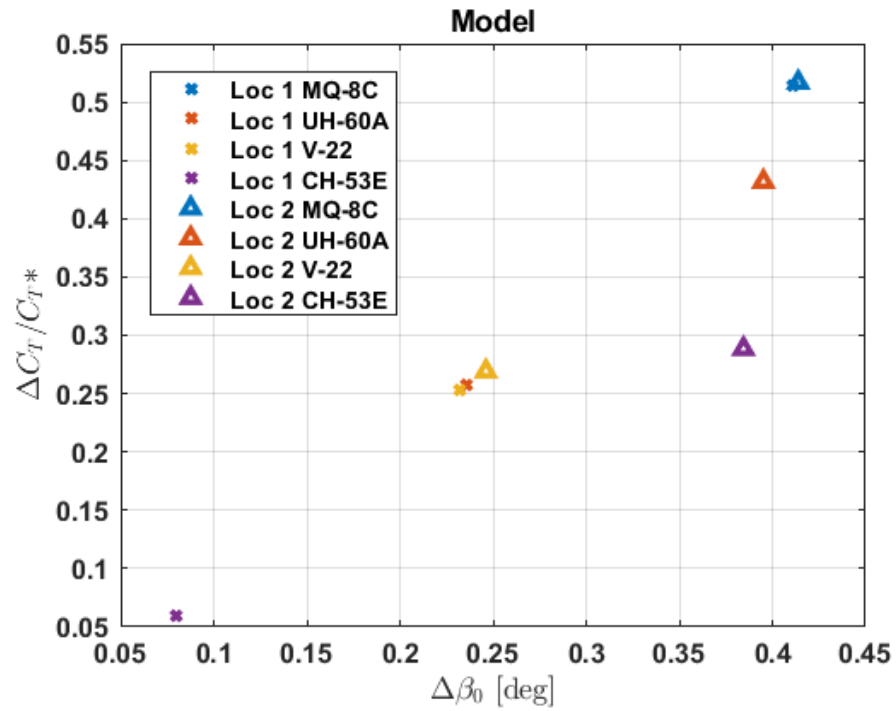


Figure 163: Influence of arch vortex on rotor fundamental response; $C_T^* = W / \rho (\pi R)^2 (\Omega R)^2$,
 $\Delta C_T = C_T - C_T^*$, $\Delta\beta_0 = \beta_0 - \beta_0^*$.

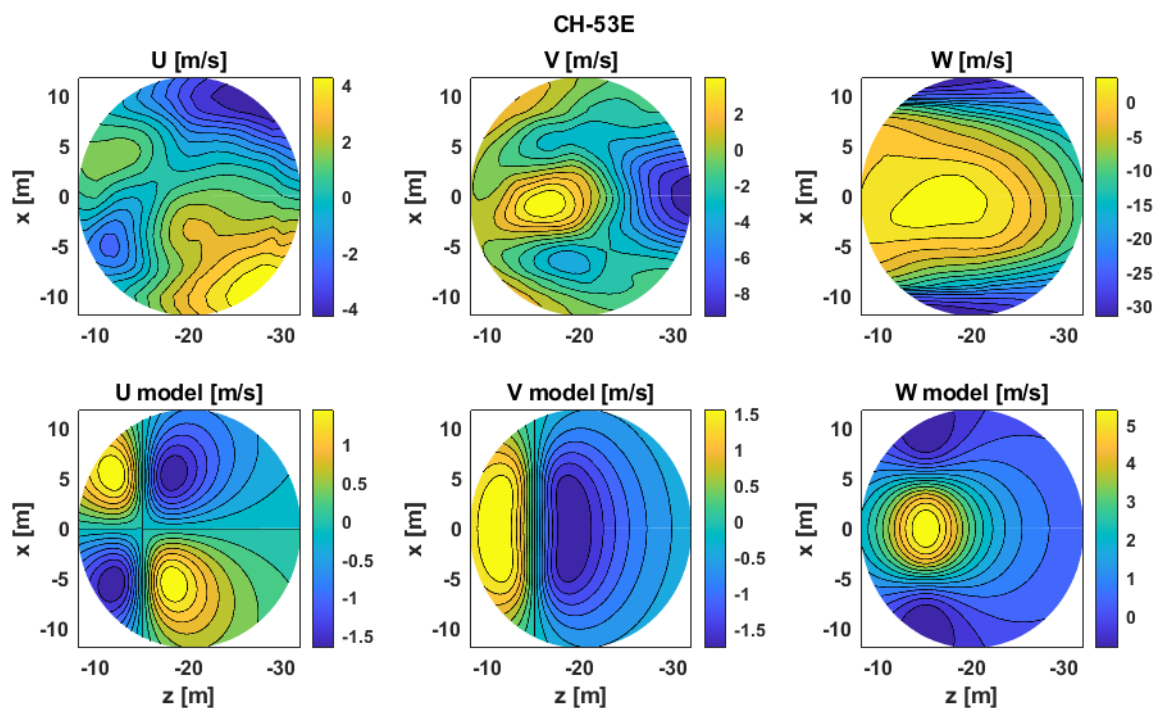
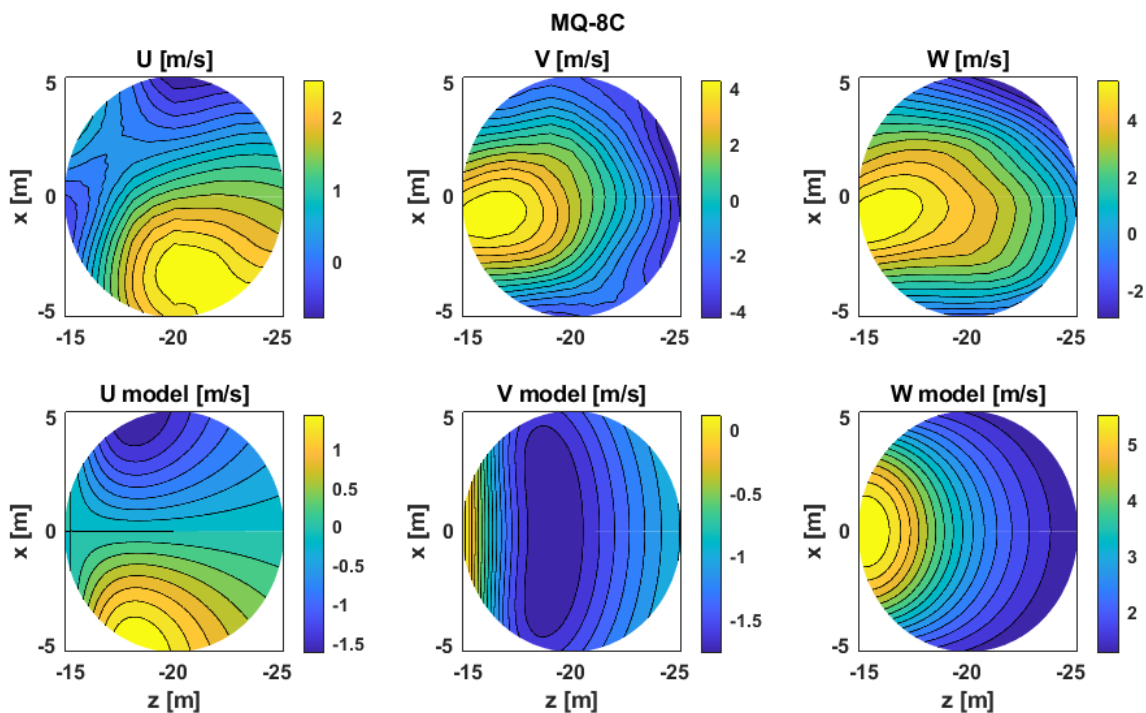


Figure 164: Contours of airwake velocity components over MQ-8C and CH-53E rotor centers at location #1 where $[x,y,z] = [0, 8, -20]$ m.

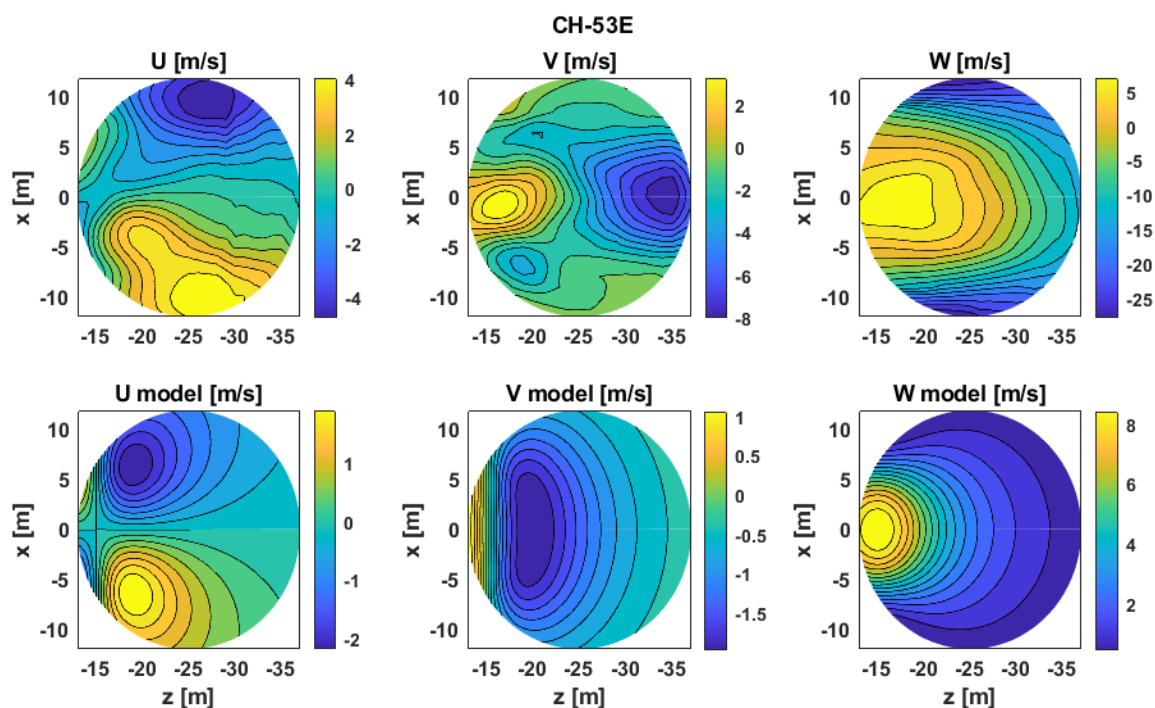
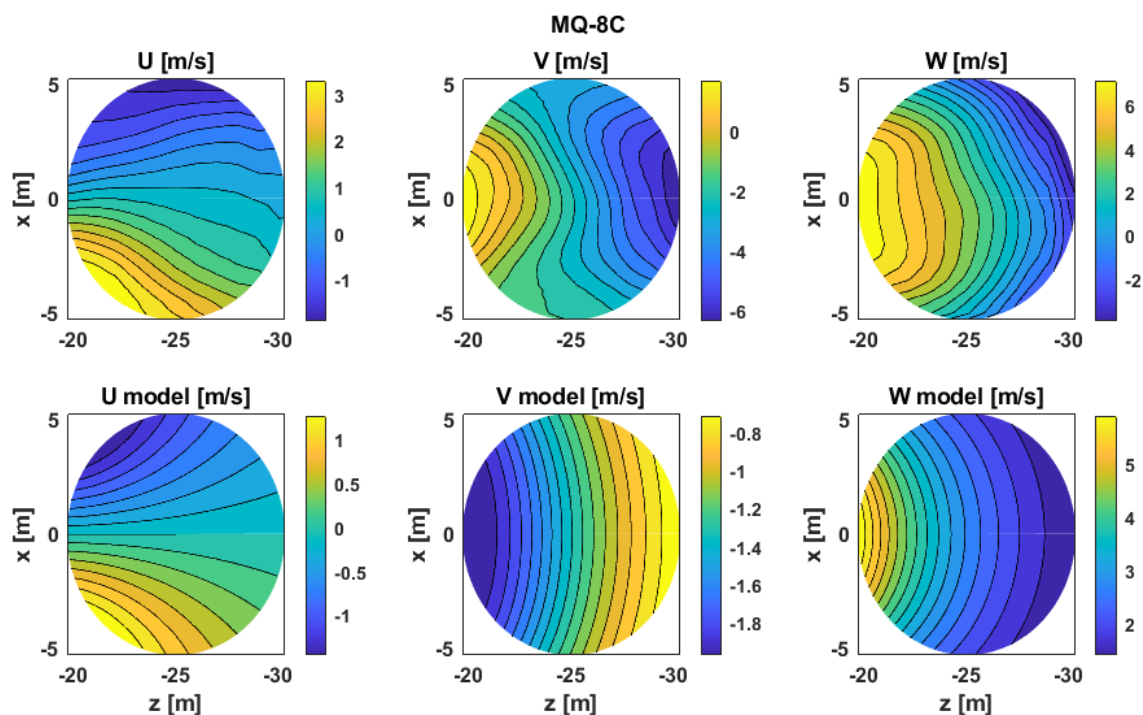


Figure 165: Contours of airwake velocity components over MQ-8C and CH-53E rotor centers at location #2 where $[x,y,z] = [0, 6, -25]$ m.

Rotor Fundamental Response during a Representative Approach trajectory

The horseshoe vortex model when implemented using Eq. (35) **Error! Reference source not found.** is equivalent to the function $v_{vortex}(r, \psi)$ from Eq. (39) and enables the computation of fundamental rotor response to the associated airwake velocities. We again use the rotors from Table 11. These rotors follow a straight-line approach trajectory with a constant flight path angle $\gamma = 6^\circ$, shown in Figure 166. The rotor hub centers coincide with the ship centerline. The descent begins at an altitude $y_0 = 45.7\text{m}$ (150 ft.) with a forward speed $V_0 = 41.2\text{ m/s}$ (80 kts), and ends in hover at an altitude $y_f = 6\text{m}$ (19.7 ft) above the center of the deck ($z = -20\text{m}$). We use the analytical model developed by Heffley [102] to determine the velocity and deceleration profile during this descent. This model provides velocity and deceleration expressions that are based on a human pilot's perception of the visual field during a deceleration operation from forward flight to hover and have been validated against flight test data [102]. The descent trajectory, velocity and deceleration profiles are plotted in Figure 167.

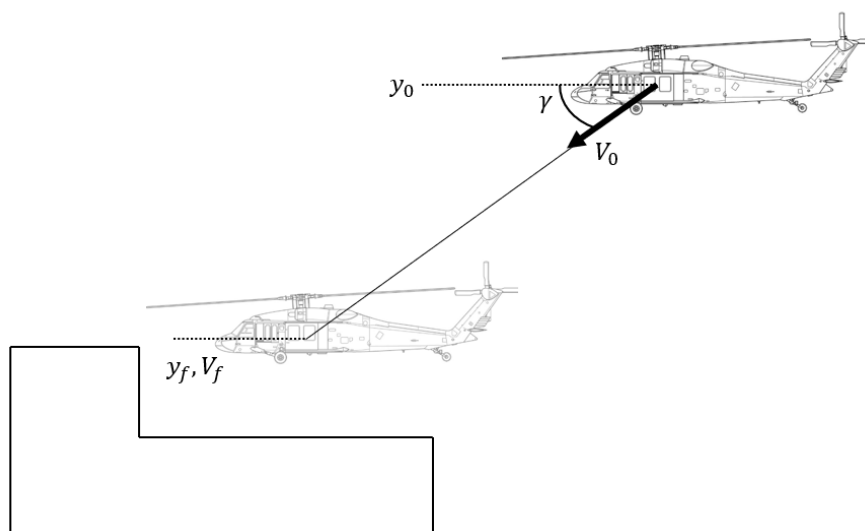


Figure 166: Illustration of approach trajectory.

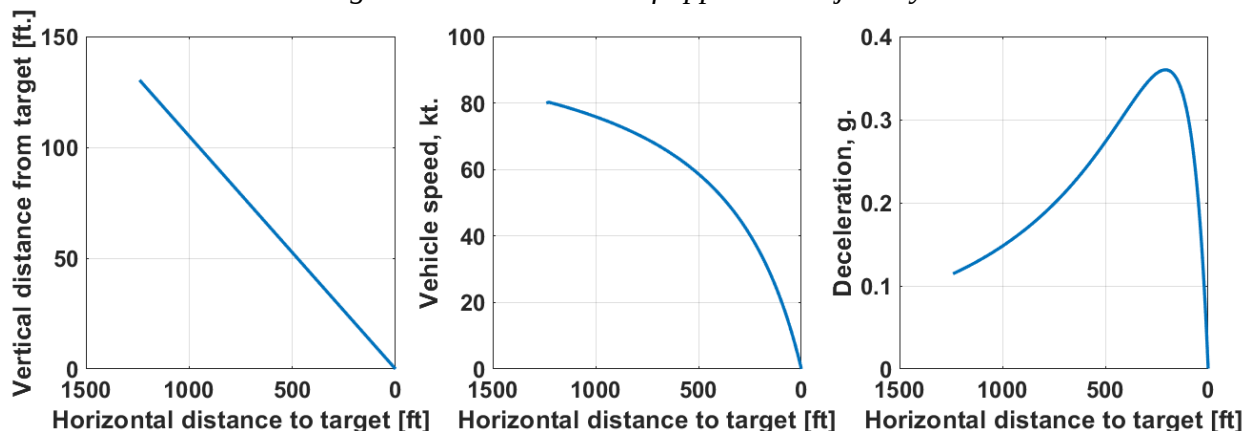


Figure 167: Approach trajectory (left), velocity (middle) and deceleration (right) profiles.

The magnitude of the horseshoe vortex influence on the thrust coefficient (C_T), disk coning (β_0) and longitudinal disk tilt (β_{1c}) as a function of nondimensional horizontal distance from the target location ($\bar{D} = D/L_a$) for the different rotors is depicted in Figure 168. Similarly, the

influence of the vertical velocities obtained from CFD are given in Figure 169. Note that the change in lateral disk tilt (β_{1s}) is negligible due to symmetry of the low-speed approach and the vortex model and is thus not shown here. We make the following observations from the figures:

1. There is a steeper decrease and kinks in responses near the target location ($\bar{D} = 0$) in Figure 168 since that is close to where the horizontal part of the horseshoe vortex is situated ($z=-14\text{m}$).
2. In both Figures, the MQ-8C rotor experiences the greatest change in thrust coefficient, followed by the UH-60 rotor. In Figure 168, we note that the V-22 rotor experiences a slightly greater change in thrust coefficient than the CH-53E rotor, while in Figure 169 the opposite is true, with a clear distinction in the response curves. This is likely due to the influence of additional flow features in the “truth” data in Figure 169, in addition to the rotor properties and size. This difference is under further investigation.
3. A greater response in coning magnitude is noted in the truth responses in Figure 169, when compared to those in Figure 168. Apart from the CH-53E coning response, the coning response of the MQ-8C rotor is the largest, followed sequentially by that of the UH-60 rotor and the V-22 rotor in both figures, which gives further confidence in the horseshoe vortex model approach.
4. For the longitudinal tilt response, the CH-53E rotor is noted to have the largest response away from the target location for $\bar{D} \in [1,2]$ and smallest response closer to the target hover location. In both Figures, the β_{1c} response for the MQ-8C and V-22 rotors appear to overlap, with that of the UH-60 overlapping with them as well in Figure 168.

These results are encouraging and indicate further showcase the potential impact of the horseshoe vortex as a fundamental airwake flow feature that influences aircraft responses.

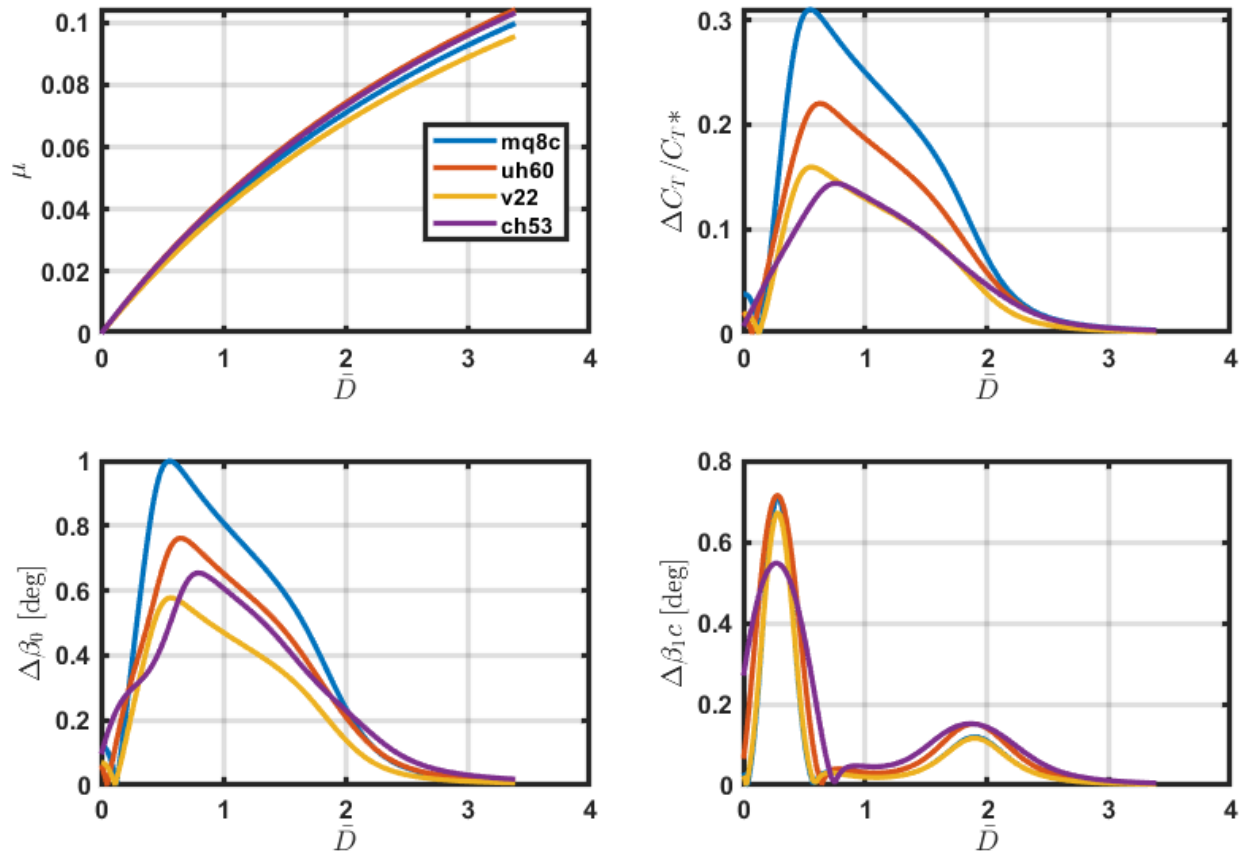


Figure 168: Advance ratio $\mu = V/\Omega R$ (top left), change in thrust coefficient (top right), change in coning (bottom left) and change in longitudinal tilt (bottom right) during approach to target hover position in the presence of horseshoe vortex; $C_T^* = W/\rho(\pi R)^2(\Omega R)^2$, $\Delta C_T = C_T - C_T^*$, $\Delta \beta_0 = \beta_0 - \beta_0^*$.

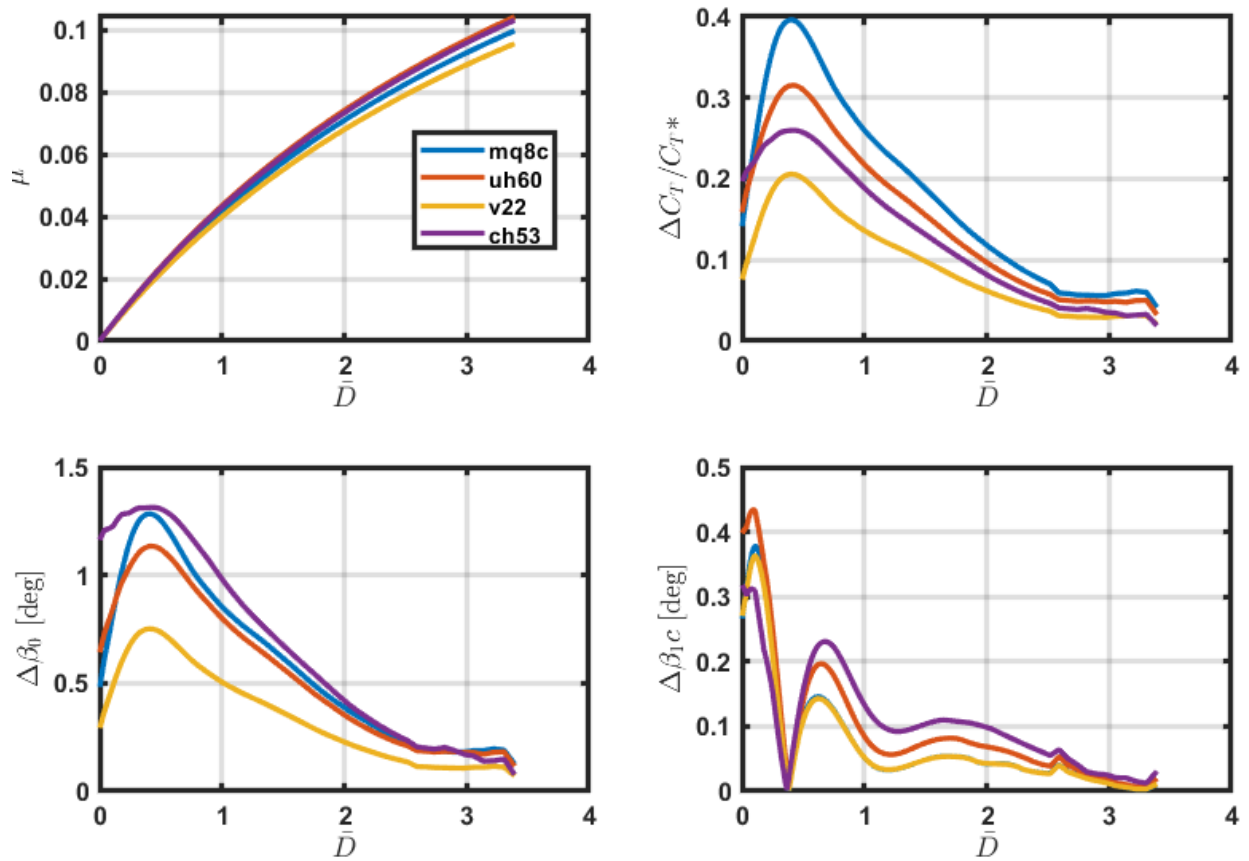


Figure 169: Advance ratio (top left), change in thrust coefficient (top right), change in coning (bottom left) and change in longitudinal tilt (bottom right) during approach to target hover position in the presence of CFD predicted vertical velocities; $C_T^* = W / \rho (\pi R)^2 (\Omega R)^2$, $\Delta C_T = C_T - C_T^*$, $\Delta \beta_0 = \beta_0 - \beta_0^*$.

Nondimensional Rotor Responses to Simplified Representation of Ship Airwake

The horseshoe vortex parameterization that we adopted relies on ship geometry dimensions (hangar height H , beam W) and freestream speed for dimensionalization. This representation, together with the analytical rotor response model given by Eq. (39), allows us to investigate the influence of varying ship geometry, freestream, and rotor properties (see Table 11) on rotor fundamental response, from which nondimensional scaling trends can be identified. We examined the following cases using the 4 rotors considered previously:

- Effect of varying ship beam;
- Effect of varying rotor vertical position; and
- Effect of varying free stream speed.

Baseline ship geometry dimensions consisted of $H = 12\text{m}$ and $W = 26\text{m}$, which represent the average dimensions of category #2 ships from Table 9. A freestream speed of 15.4 m/s (30 knots) was assumed. All rotors are assumed to be in hover and positioned in plane with the horseshoe vortex, downstream of the horizontal part of the vortex. The hub centers lined up with the center of the vortex.

Distribution Statement A

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Effect of Varying Ship Beam

The influence of ship beam variation is shown in Figure 170. The vertical lines delimit the region of typical beam-to-rotor radius ratios and reflect the MQ-8C and CH-53 values for the DDG ship in Table 12, which summarizes ship beam-to-rotor radius ratios for the four rotors and category #2 ships from Table 9. The quantity λ^* in the Figure represents a nondimensional measure of the inflow induced by the vortex, computed using momentum theory for a rotor in axial climb, and given by:

$$\lambda^* = \frac{\Delta C_T}{\sqrt{2C_T^*}} \quad (41)$$

where C_T^* is the baseline thrust coefficient of the rotor in hover determined using gross weight and $\Delta C_T = C_T - C_T^*$ (see Eq. (39)). From Figure 170, all the responses follow an inverse bell-curve pattern, with peak inflow noted when ship beam $W \cong 1.3R$. The offsets in λ^* responses for the four rotors point towards the existence of an additional scaling factor that can “collapse” all the responses onto each other.

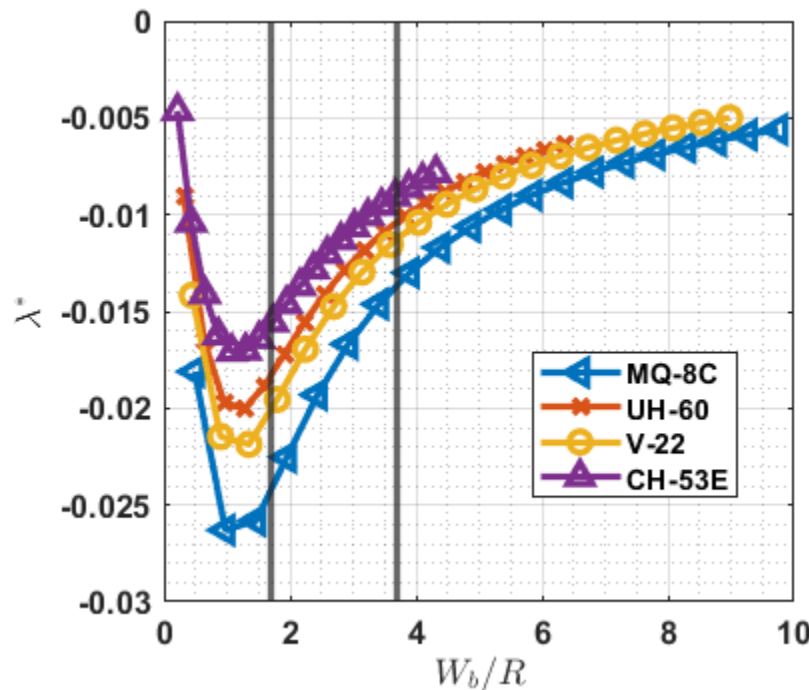


Figure 170: Influence of ship beam variation on rotor fundamental response; $\lambda^* = \Delta C_T / \sqrt{2C_T^*}$, where $C_T^* = W / \rho (\pi R)^2 (\Omega R)^2$, $\Delta C_T = C_T - C_T^*$ vertical lines used to delimit typical range for a DDG type ship .

The results shown in Figure 170 were obtained with the nondimensional viscous core radius scaled by beam. We repeated the investigation with the core radius scaled by height and discovered that changes in rotor thrust, when expressed in terms of blade loading, appear to exhibit a scaling relationship with the product of beam-to-rotor radius ratio W_{beam}/R and vortex strength $\bar{\Gamma}$, when the latter is nondimensionalized by the product of rotor area and rotation speed, see Figure 171. The vertical lines again delimit typical ranges for the MQ-8C and CH-53 for a DDG type ship, determined using the average $\bar{\Gamma}$ value for the four rotors. There are offsets in the

blade loading predictions up to $(W_b/R)/\bar{\Gamma} \cong 80$, notably in the single V-22 rotor relative to the remaining rotors, whose responses remain close to that of each other.

Table 12: Representative ship beam-to-rotor radius values.

Ship	W [m]	W/R (MQ-8C)	W/R (UH-60)	W/R (V-22)	W/R (CH-53E)
LPD	32	6.0	3.9	5.5	2.7
DDG	26	3.7	2.4	3.5	1.7
LSD	32	4.9	3.2	4.5	2.2

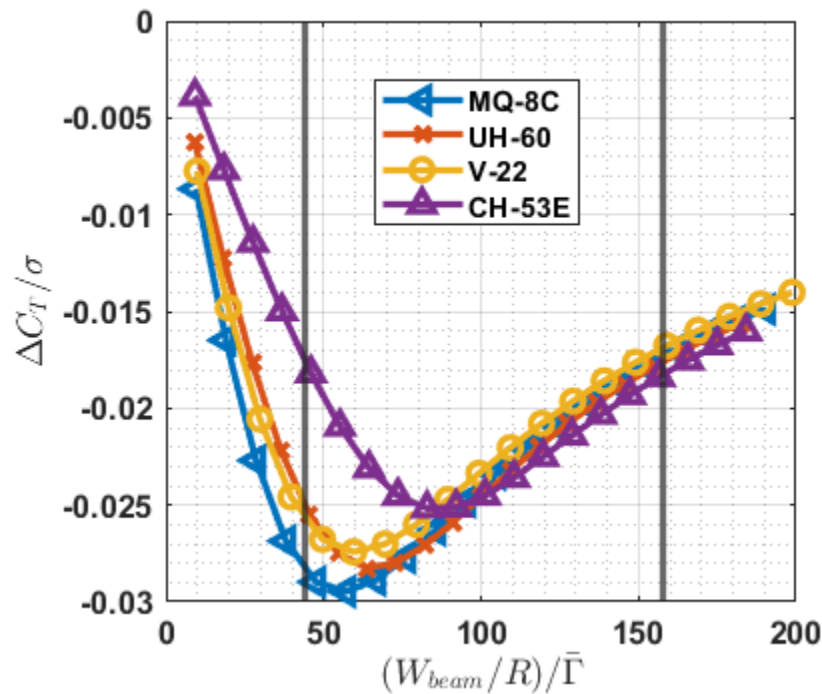


Figure 171: Influence of ship beam variation on rotor fundamental response; ; $\Delta C_T = C_T - C_T^*$, where $C_T^* = W/\rho(\pi R)^2(\Omega R)^2$, σ is the rotor solidity, and $\bar{\Gamma} = \Gamma/A_{rot}\Omega$; vertical lines used to delimit typical range for a DDG type ship

Effect of Varying Rotor Vertical Position

The influence of rotor vertical position variation is shown in Figure 172, where we plot the nondimensional change in blade loading $\Delta C_T/\sigma$ against changes in vertical position Δy , which is nondimensionalized by hangar height H . All the responses follow the same trend and are close to each other, highlighting blade loading as a viable scaling parameter. Peak offsets in the responses are noted when the rotor is in line with the vortex ($\Delta y = 0$), where the MQ-8C and UH-60 responses and the V-22 and CH-53E responses overlap, respectively. Given the horseshoe vortex structure, the rotors experience peak disturbance when they are in line with the vortex.

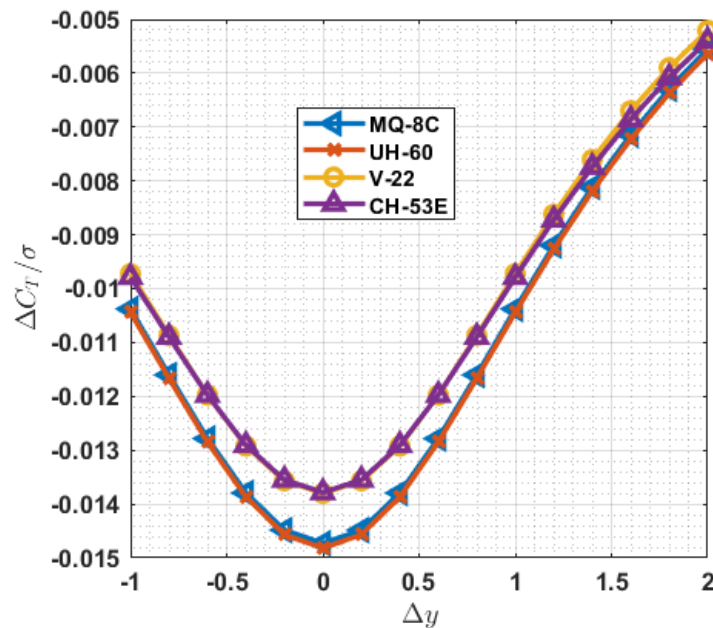


Figure 172: Influence of rotor vertical position variation; $\Delta C_T = C_T - C_T^*$, where $C_T^* = W/\rho(\pi R)^2(\Omega R)^2$, σ is the rotor solidity and Δy represents the change in vertical position from the starting position and nondimensionalized by hangar height H ; vortex core radius dimensionalized by beam W .

The results in Figure 172 were obtained with the nondimensional viscous core radius scaled by beam. We repeated the investigation with the core radius scaled by height and obtained a similar inverse bell curve pattern with higher response magnitudes, Figure 173.

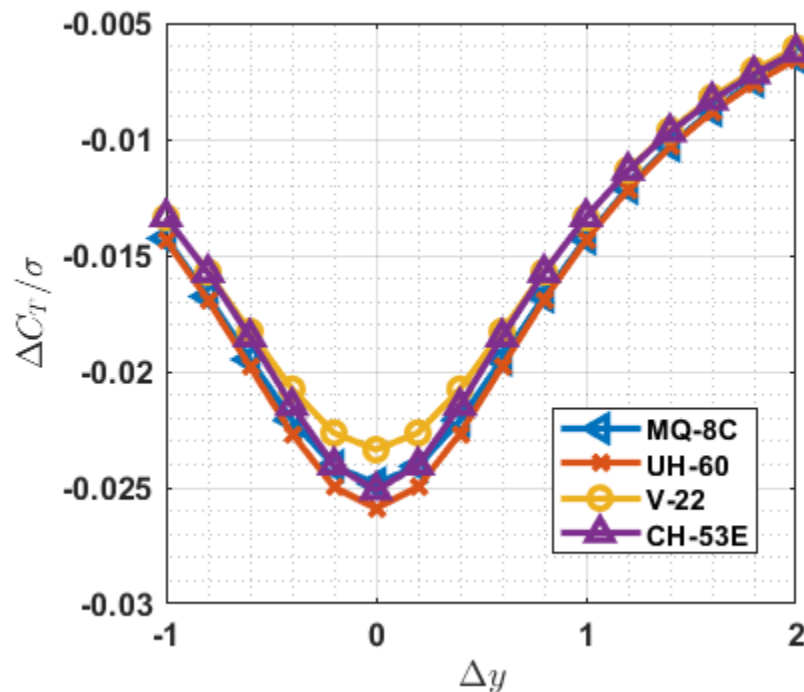


Figure 173: Influence of rotor vertical position variation; $\Delta C_T = C_T - C_T^*$, where $C_T^* = W / \rho(\pi R)^2(\Omega R)^2$, σ is the rotor solidity and Δy represents the change in vertical position from the starting position and nondimensionalized by hangar height H ; vortex core radius dimensionalized by H .

Note that the magnitude of the rotor responses to the disturbance scales linearly with freestream speed since this quantity scales the vortex strength accordingly.

Assessment of Flying Qualities / Pilot Workload / Handling Qualities

One focus of the effort has been the identification of suitable methods for objective assessment of aircraft handling qualities during shipboard operations. It is desirable to examine analytical methods versus subjective handling qualities ratings (HQRs) since the latter requires the presence of a skilled pilot within a virtual simulation environment. Objective assessment of rotorcraft handling qualities, in particular during shipboard operations, has received considerable attention over the past two decades.

Several prior studies have focused on quantitative assessment of rotorcraft handling qualities during shipboard operations. Mitchell and co-workers [103] examined extensions to conventional rotorcraft handling qualities assessment techniques (based on ADS-33 methods) for the maritime recovery mission, which incorporated test methods initially led by Canada's National Research Council. This work focused on extension of the hover mission task element (MTE) to include tracking of a moving target to account for ship motion effects. One conclusion from this study indicated that the existing hover MTE criteria is sufficient for low sea states (SS3 or less) but does not capture the increased workload experienced by pilots in higher sea states. Note that "sea state" in this context only accounts for ship motion. In practice, wave motion effects are correlated with wind (airwake) effects. In addition, platform motion couples with rotor wake re-ingestion effects, which can lead to torque margin reductions and their impact on rotorcraft handling qualities. One logical goal of the present research and development will be to examine individual contributions to the overall assessment of "workload", thus providing additional basis for characterizing rotorcraft-shipboard operations.

Regarding purely analytical methods for shipboard handling prediction, two distinct approaches have been used in previous work. Several studies have been performed using a control model for the human operator / pilot combined with defining an objective relationship between predicted control response and the corresponding HQR value. Hess [104] implemented a "structural model" of the human pilot applied to each control axis. The structural model is an extension of the so-called "crossover model" pioneered by McRuer. Hess's implementation includes prediction of HQR level using the handling qualities sensitivity function (HQSF). In contrast, Moon et al. [105] applied an optimal pilot model based on the development by Kleinman and co-workers [106], which uses a multi-input, multi-output (MIMO) controller structure versus multiple single-input, single-output (SISO) controllers used in the study by Hess. Moon and co-workers correlated control displacements and rates to predict workload based on a regression / neural network model [105]. Note that the analytical framework developed by both Hess and Moon et al. used simplified models for the aircraft response, ship motion, and airwake disturbance. A similar approach may be examined in the present work, albeit using higher fidelity rotorcraft and airwake disturbance models (such as in **Error! Reference source not found.**).

An alternate approach for assessment of rotorcraft handling qualities has been examined by researchers in the United Kingdom (DERA Bedford, Glasgow Caledonian University, and University of Glasgow) from the mid-1990s to early 2000s. In addition to inverse simulation and pilot modeling methods (similar to Hess and Moon et al.), an analysis technique based on wavelet decomposition of the pilot control inputs was pioneered by Jones et al. [107] and applied to rotorcraft handling qualities evaluation (Macdonald and Bradley [108], Leacock [109]). The purpose of the wavelet decomposition was to determine the control “attack” metric, which is analogous to the attitude quickness parameter used as part of ADS-33 evaluation. Examining the control attack parameter relative to the magnitude of control inputs allowed a given pilot input time history (from flight testing of ADS-33 MTEs or from piloted simulation) to be separated into components associated with “stabilization” and “guidance”. Wavelet decomposition provided a method to identify the most dominant components of a given MTE / maneuver. A relationship also was determined between the dominant components of the control input wavelet decomposition and the pilot workload, which can be correlated to pilot rating of a given MTE task.

The rest of this section has been removed because it contains tiltrotor predictions that are Distribution D. The full text can be found in CDI Report 24-01.

CASTLE Simulations

Model Rehost

Work was undertaken to rehost NAVAIR Manned Flight Simulator (MFS) simulation models at CDI to support airwake modeling and its effect on dynamic interface virtual simulation. For this effort, two airframe models will be used: the MFS “ExampleHelo” model and the V-22 simulation. The ExampleHelo (also denoted ExHel) model is a generic model of the H-60 based on publicly-available source data (i.e. [15] amongst others) that is distributed by MFS for advanced aerodynamic model integration studies, among other applications. The simulation model was provided by MFS to CDI in support of this effort and subsequently has been re-hosted at CDI (i.e., source code built and test cases executed). Representative results illustrating the pedal response in hover are shown in Figure 174.

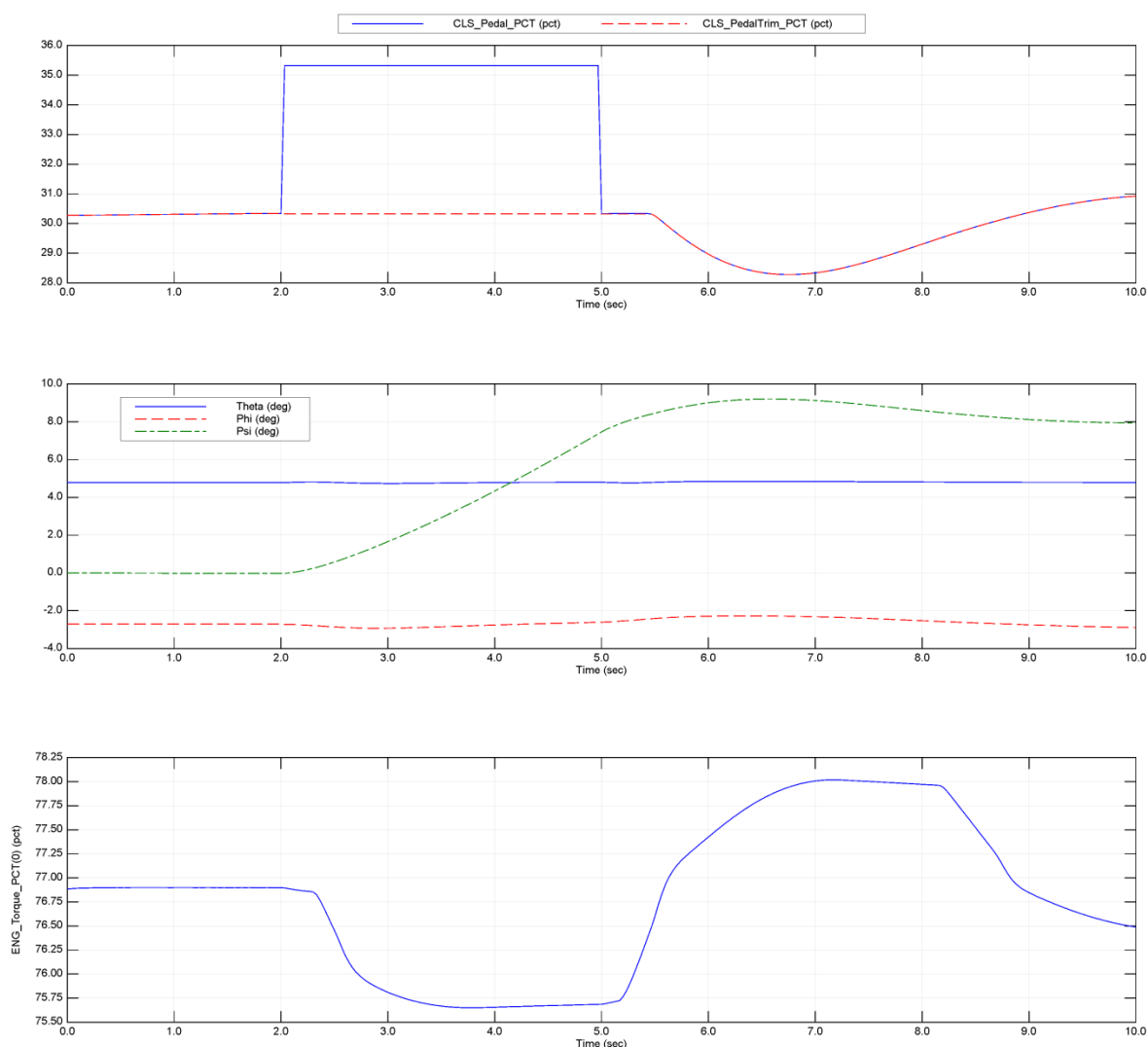


Figure 174: ExampleHelo Simulation Time Histories for Hover Check Case

An important aspect of the current effort is to examine aerodynamic modeling fidelity requirements for predicting rotorcraft handling qualities and dynamic response characteristics during shipboard operations (i.e., due to ship airwake disturbances). A core component of this investigation is use of physics-based models, including free wake (vorticity-based) models for the rotor wake and rotorcraft-airwake interactions. This investigation was supported by integration of the CDI CHARM Module with the airframe rotor model. In previous work, CDI has coupled a variant of the CHARM module (denoted CharmSI for CHARM with Shipboard Interactions) with the MFS “FVMS Models” library, which is a generic flight dynamics modeling library for supporting rotorcraft simulation model build-up. In this case, CHARM provides an alternative inflow model to more conventional (dynamic inflow / finite-state inflow) models, which may be enabled in a given airframe simulation through the CASTLE user interface. This framework has been used in previous pilot evaluation of advanced aerodynamic models within a Navy fleet trainer (see Figure 3 and [10]).

A primary focus of the current effort has been to update the FVMS rotor-CHARM interface since both the FVMS Models library and CHARM interface have evolved since the H-60 pilot evaluation performed in 2016 and described in [10]. In parallel to this effort, CDI has updated the FVMS-CHARM interface to support a pilot evaluation for the MFS V-22 simulation (conducted in December 2022). This simulation model, which was applied in the present effort, required updating the CHARM interface for use in FVMS Models version 2020.1. Some additional development was required for use with the ExampleHelo simulation, which is based on FVMS Models version 2021.1.

FVMS/CHARM Coupling for Simulating Rotorcraft Responses to Disturbances

The simulation environment to capture rotorcraft response to disturbances is built upon the NAVAIR CASTLE software, which provides a generic simulation executive and model development tools. CASTLE is structured so that simulation development requires specification of aircraft-unique models for aerodynamic loads, engine/transmission, weight/balance, control laws, and sensor models. CASTLE also provides a user interface and scripting (macro) language to perform various simulation functions and automation of simulation tasks.

The CHARM software is a well-validated comprehensive aircraft aerodynamics and dynamics analysis applicable to a wide range of fixed- and rotary-wing aircraft [110, 111]. CHARM couples a full-span, free-vortex wake model with a vortex lattice lifting surface blade model (VLM) and a fast doublet panel fuselage model to provide a capability for modeling complete aircraft aerodynamics involving combinations of propellers, rotors, wings, fuselages, and tail surfaces. CHARM exists as a standalone analysis capability and in the form of modules that can be integrated with existing simulation frameworks for specific computations. For integration with CASTLE, the CHARM wake panel module that specializes in computing induced velocities for specified aircraft components and spatial locations is used.

While CASTLE provides basic simulation executive functions, CHARM is integrated within the FVMS Models codebase, which provides a collection of re-usable code and tools for building the simulation model components. The FVMS Models library includes physics models (weight/balance, power plant, landing gear); environment models (airwake); systems models (sensors, avionics); and math utilities including coordinate transformations, conversion constants, general function tables and interpolation functions, and control system components. This library of generic models is coded in C++ and has been developed to provide a common codebase for validated (documented) models and simulation construction tools.

The FVMS physics models include a generic blade element model for application to rotary-wing aircraft simulations. The FVMS rotor system model is structured similarly to conventional rotor blade element models (such as GenHel [15]). This blade element model includes a distributed air loads calculation based on two-dimensional sectional aerodynamic data; rigid blade flap/lag dynamics; a simplified (empirical) dynamic twist correction; and inflow dynamics using the Pitt-Peters inflow model [112, 113]. The FVMS rotor system supports multiple rotors with different rotational speeds so that tandem, side-by-side (tiltrotor), and main rotor/tail rotor aircraft configurations can be modeled. Optionally, a simplified Bailey model may be used for the second (tail) rotor system.

The rotor system model for a specific aircraft simulation implementation can be developed by deriving from code components (base classes) defined in the FVMS codebase. It is during this development process where aircraft configuration-specific parameters are defined. Note that the base FVMS rotor system model includes all necessary executive functions for performing

rotor trim and run mode operations. The inflow model is one component of a complete rotor system definition; it is at this level that the CHARM wake model is coupled.

The CHARM model (module) is integrated with the CASTLE / FVMS Models library using a separate interface class (C++ wrapper) that executes the free vortex wake and induced velocity model (function calls) in an external (dynamically linked) library. The FVMS/CHARM interface class maps simulation variables to CHARM variables, and vice versa, in addition to performing wake model initialization, update, and induced velocity evaluations.

In contrast to conventional dynamic inflow model formulations, the CHARM model provides induced velocity evaluations for control points corresponding to blade segments of multiple interacting rotors and for evaluation points off the rotor system (i.e., at aerodynamic control points at the empennage, lifting surfaces, and fuselage). Wake elements from multiple rotors, as well as fixed lifting surfaces, are fully interacting with each other and the surrounding environment (e.g., ground effect). Thus, the FVMS/CHARM implementation provides an induced velocity for the entire rotor system and airframe. Empirical “interference factors” used in conventional rotorcraft flight dynamics simulations are replaced by direct (dynamically varying) wake induced velocity evaluations.

To account for rotor wake and ship airwake interactional effects, the interface of the ship airwake model component needs to be adapted. In a more conventional rotorcraft simulation formulation, rotor inflow and ship airwake effects are treated independently since their effects are superimposed in the blade aerodynamic calculation. Given the nature of the coupled rotor wake/ship airwake model, ship airwake velocity evaluations must be managed by the CHARM model to properly account for rotorcraft and ship airwake interactions. This approach allows different levels of airwake model coupling to be supported (e.g., rotor wake/airwake superimposition, airwake effects on rotor wake only, and fully-coupled rotor wake/airwake interactions) while ensuring that airwake effects are not double-counted. To support a generalized ship airwake interface to the CHARM model, an external airwake model “loader” interface (denoted SHIPWAKE) was previously developed. This external interface adopts a “plug-in” software architecture so that the specific airwake model (library) can be specified as a run-time parameter.

To provide an internal test environment for the coupled CASTLE/FVMS and CHARM models, the FVMS codebase has been applied to CASTLE’s Example Helicopter (or ExHel) simulation. The ExHel model is based on data describing the UH-60A helicopter, using a combination of equations and software developed for the FVMS Models codebase and original GenHel math model [15]. Note that the ExHel model is not intended to be a comprehensive simulation of an H-60 helicopter but to provide a representative conventional main/tail rotor helicopter configuration for research, tool integration, and training simulation engineers.

As part of the present effort, we revamped and tested out the enhanced ExHel simulation environment. We introduced a uniform 10 ft/s downwash disturbance field to the model in hover using the same input file format as would be required for a CFD based ship airwake data file to ensure proper execution and debug any workflow issues. The ExHel simulation provides the user with options to include a basic stability augmentation system (SAS) and/or an automatic flight control system (AFCS). For the initial results presented below, both are activated. The roll and pitch attitudes of the aircraft in the presence of disturbance are shown in Figure 175 for three cases: Pitt inflow model (no disturbance), CHARM inflow model (no disturbance), CHARM inflow model with two-way coupled airwake. As evident from the figure, choice of

inflow and inclusion of the disturbance influences the aircraft attitudes, even in the presence of the SAS and AFCS, highlighting the importance of performing dynamic interface studies. The corresponding swashplate control inputs are shown in Figure 176. The main rotor collective θ_0 remains constant throughout the simulation because there is no control feedback associated with it that is implemented in the SAS or AFCS of the Exhel model. The remaining three controls are the ones used to maintain the trim flight condition in the presence of the disturbance. The influence of disturbance is also evident in the vertical force generated by the main rotor and shown in Figure 177. Note that the UH-60 tail rotor has a 20° cant angle from vertical to assist with lift. When the CHARM inflow is used, the tail rotor generates more lift than when the Pitt inflow model is used to sustain the hover flight condition as evident in Figure 176, where the starting value of the tail rotor collective for cases with CHARM included is higher than that with the Pitt inflow model.

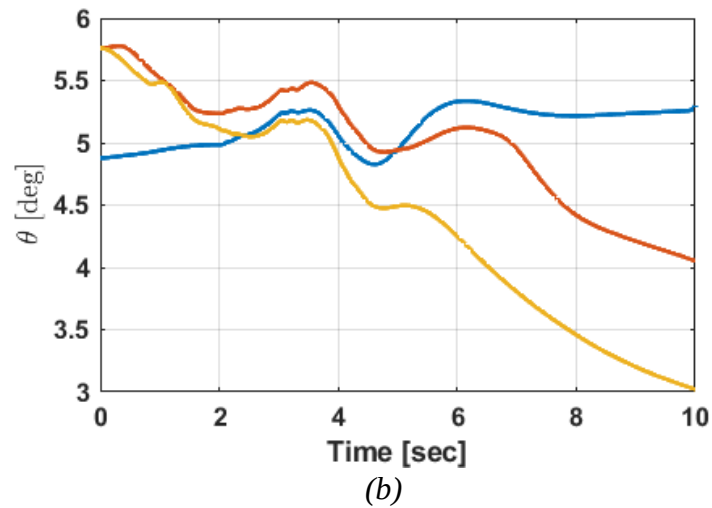
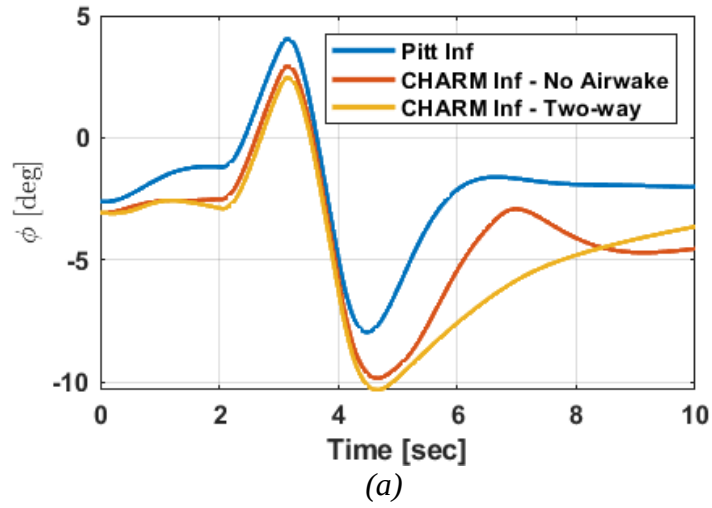


Figure 175: (a) Roll and (b) pitch attitude responses of CASTLE Exhel model to a uniform 10 ft/s downwash disturbance field in hover for three cases: Pitt inflow model (no disturbance), CHARM inflow model (no disturbance), CHARM inflow model with two-way coupled airwake.

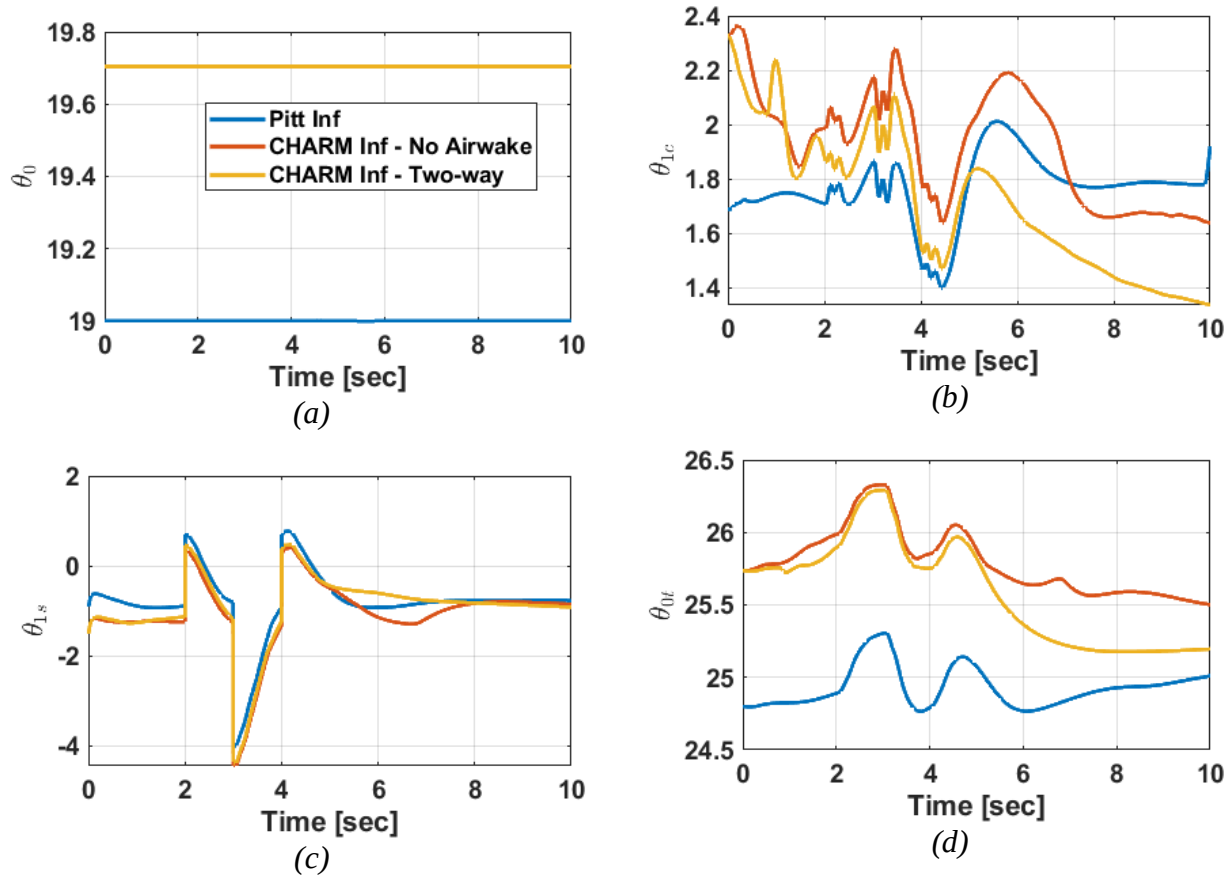


Figure 176: Time histories of main rotor swashplate (a) collective, (b) lateral cyclic, (c) longitudinal cyclic and (d) tail rotor collective inputs in the presence of a uniform 10 ft/s downwash disturbance field in hover for three cases: Pitt inflow model (no disturbance), CHARM inflow model (no disturbance), CHARM inflow model with two-way coupled airwake.

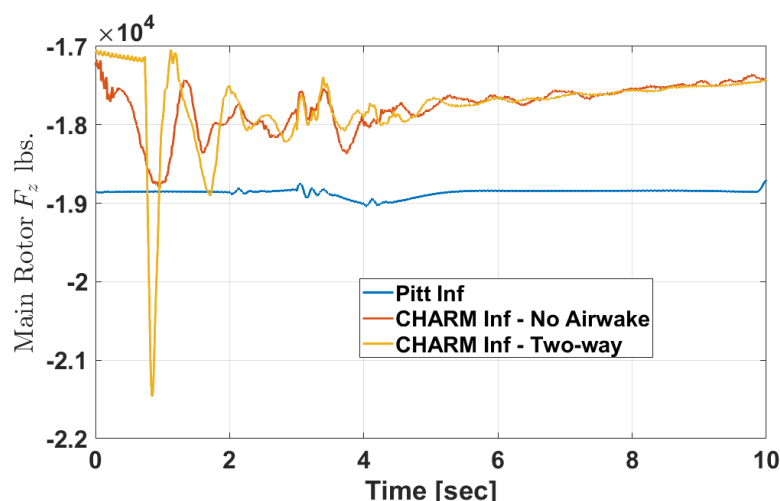


Figure 177: Time histories of main rotor vertical force in the presence of a uniform 10 ft/s downwash disturbance field in hover for three cases: Pitt inflow model (no disturbance), CHARM inflow model (no disturbance), CHARM inflow model with two-way coupled airwake.

Aircraft Response to an Isolated Vortex Disturbance

We previously examined analytical responses of the four rotors given in Table 11 to the isolated vortex depicted in Figure 159. From this idealized investigation, blade loading $\Delta C_T / \sigma$ was indicated to be a potential scaling parameter for gaining insights into how rotor fundamental response scales with forward speed μ . Our investigation follows a similar approach to that undertaken in Ref. [7], where an isolated idealized rotor is subjected to the vortex disturbance with modeling fidelity ranging from analytical idealizations and traditional frozen field and finite state wake models, to fully distorting CFD models solving the grid-based vorticity-velocity form of the incompressible Navier-Stokes equations. Results from Ref. [7], see Figure 178 (duplicate of Figure 4 copied here for convenience), indicated that the frozen field assumption, where the velocity fields are fixed in space and do not distort/respond to the aircraft, overpredicted the severity of rotor responses at low forward speeds. Here, we perform similar investigations using the coupled ExHel / CHARM aircraft flight dynamic model which, unlike the prior isolated rotor investigations, includes fully coupled rotor-airframe dynamics and employs a distorting Lagrangian wake model. Simulations were performed with the rotor hub center aligned with the vortex center as depicted in Figure 159. For each forward speed considered, the aircraft was first trimmed without disturbance. Time-marching simulations were then performed with the disturbance turned on for a duration of 30 seconds. Results presented below employ time-averaged values. The first 5 seconds of the simulation are discarded to eliminate transient effects. Given our focus on fundamental response, the aircraft position and controls are held fixed during the time-marching simulation. We considered two gross weight values for comparison: 15,000 lbs. and 20,000 lbs.

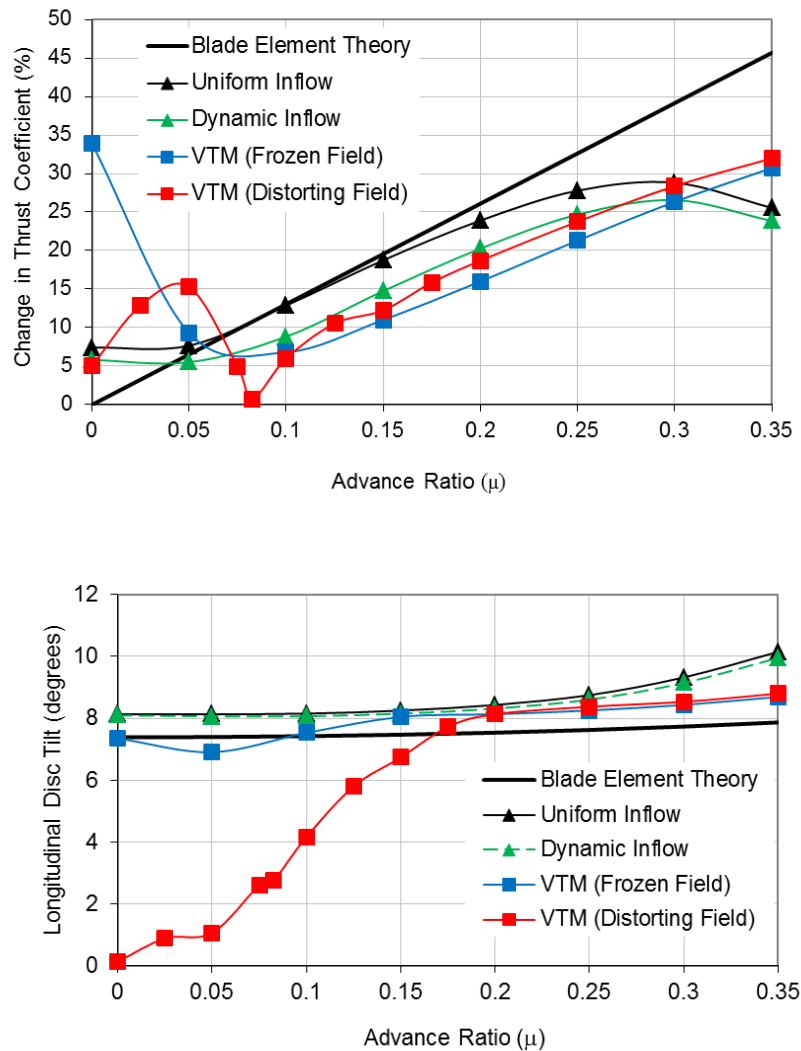


Figure 178: Mean perturbations to rotor thrust and longitudinal disc tilt for an isolated rotor interacting with a vortex aligned with the flight path. Adapted from [6, 7] (repeat of Figure 4)

Changes in main rotor thrust, coning, longitudinal tilt and lateral tilt are shown in Figure 180 to Figure 184, where the mean perturbations from Ref. [7] are also plotted for comparison. From Figure 180, main rotor thrust responses for both frozen and distorting cases from the ExHel simulations follow similar trends to their counterparts from Ref. [7]. Notably, the frozen assumption predicts a significant increase in thrust that is close to 35% greater than that from the distorting case at $\mu = 0.025$. At $\mu = 0.075$, the change in thrust predicted in the distorting case becomes larger than that from the distorting by approximately 6%. The offsets in the responses between the two cases diminish progressively with increasing advance ratios. For $\mu \geq 0.15$, changes in thrust are attenuated relative to corresponding predictions from Ref. [7], with changes in thrust coefficient in the frozen case being close to zero. The exact reason for this discrepancy remains to be identified. The full aircraft model is more complex than the idealized rotor considered in Ref. [7] and includes contributions from the fuselage, empennage, and the canted tail rotor. Additionally, the main rotor model has a built-in shaft tilt. At trim, the aircraft has

nonzero attitudes at all advance ratios as shown in Figure 185. Predictions in Ref. [7] were made using a CFD solver, where the vortex was superimposed over the entire domain with fixed conditions at the upstream and downstream boundaries before initiating the time-marching. This is fundamentally different from the initialization approach employed with the Lagrangian vortex particle model implemented in CHARM, which is more sensitive to setup parameters such as number of particles and viscous core model assumptions. Disturbance particles are initialized upstream of the aircraft and convected towards the aircraft by freestream, effectively simulating a vortex encounter scenario, see Figure 179. Discrepancies between results from this investigation and those from Ref. [7] are thus expected. Although changes in thrust predicted in the frozen and distorting cases diminish with increasing forward speed, a notable decrease in torque requirements is present at all speeds as shown in Figure 181, pointing towards a potential climb velocity effect being introduced by the disturbance.

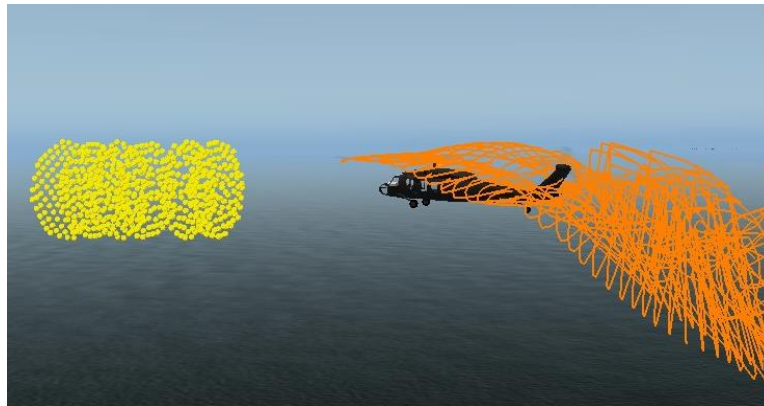


Figure 179: Vortex particles corresponding to the straight vortex disturbance are initialized upstream of the aircraft and transported with freestream towards the aircraft.

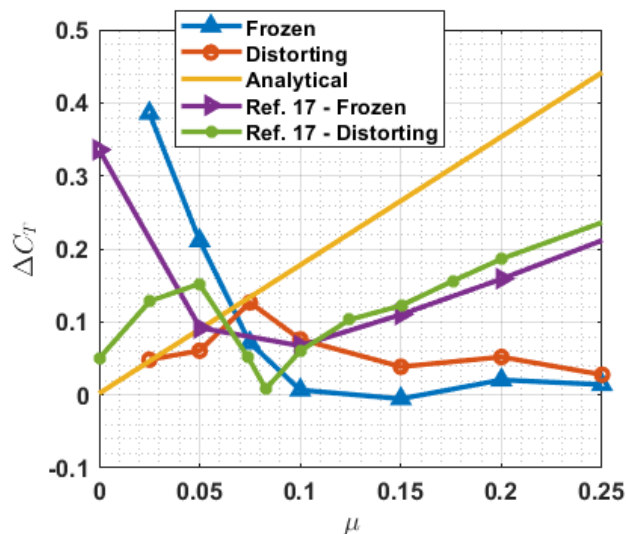


Figure 180: Changes in rotor thrust relative to trim in response to isolated straight vortex disturbance.

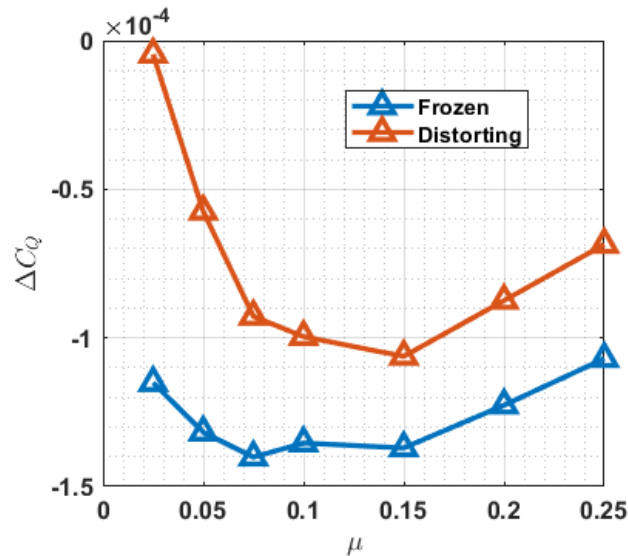


Figure 181: Changes in rotor torque coefficient relative to trim in response to isolated straight vortex disturbance.

It is interesting to note that the flapping responses in Figure 182 to Figure 184 all indicate more severe responses with the frozen disturbance assumption at low speeds ($\mu \leq 0.075$). For instance, at $\mu = 0.025$, all flapping predictions in the distorting case are close to 0° , which contrasts with $\beta_0 = 0.8^\circ$, $\beta_{1c} = -5.3^\circ$ and $\beta_{1s} = 1.5^\circ$, predicted using the frozen field assumption. This discrepancy in response between the two modeling fidelity cases is consistent with findings from Ref. [7], as indicated in the figures. The coning responses in Figure 182 follows a similar pattern to those of thrust responses with attenuated values noted at higher speeds when compared to results from Ref. [7]. A similar offset in the longitudinal tilt responses at higher speeds is also noted in Figure 183. The magnitude of the offset between the responses from Ref. [7] and those predicted using the ExHel simulation is approximately 3° , which matches the main rotor shaft tilt angle. For the lateral disk tilt responses shown in Figure 184, the ExHel simulations predict tilt values that are greater than those from the isolated rotor investigations at higher advance ratio values ($\mu \geq 0.15$). The offset is likely related to the negative aircraft roll angle at trim (see Figure 185). The difference between the isolated rotor tilt responses from Ref. [7] and the ExHel predictions in Figure 184 is approximately equal to the magnitude of the aircraft roll attitude for $\mu \geq 0.15$.

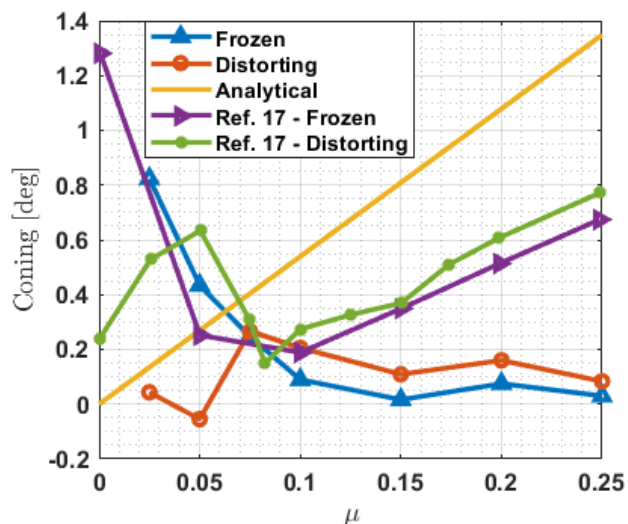


Figure 182: Changes in rotor coning relative to trim in response to isolated straight vortex disturbance.

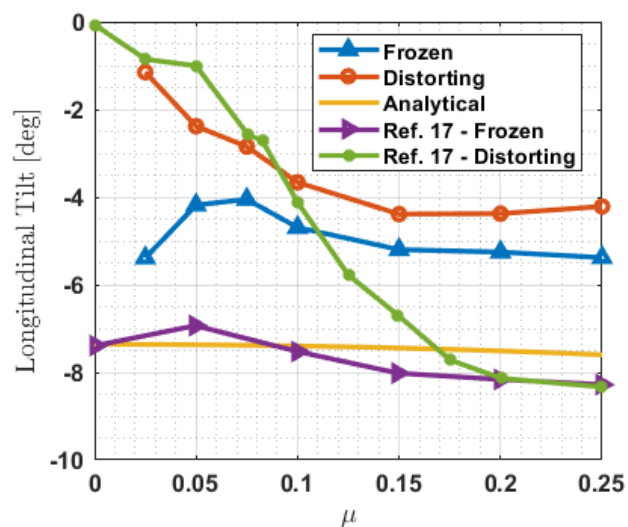


Figure 183: Changes in rotor longitudinal tilt relative to trim in response to isolated straight vortex disturbance; positive tilt is nose-down.

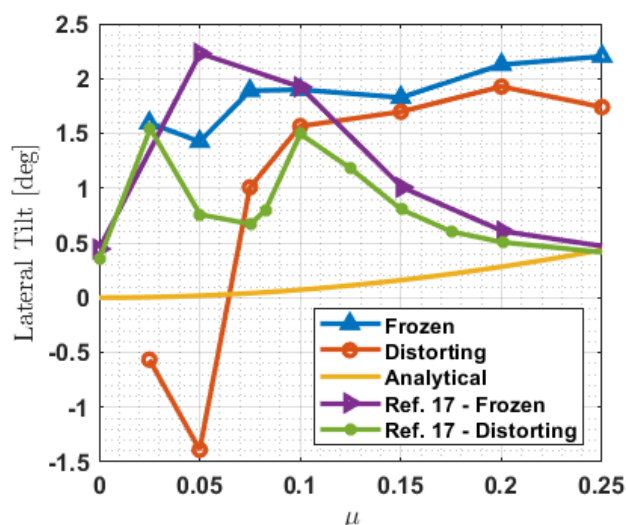


Figure 184: Changes in rotor lateral tilt relative to trim in response to isolated straight vortex disturbance; positive tilt is towards the right when vehicle viewed from the rear.

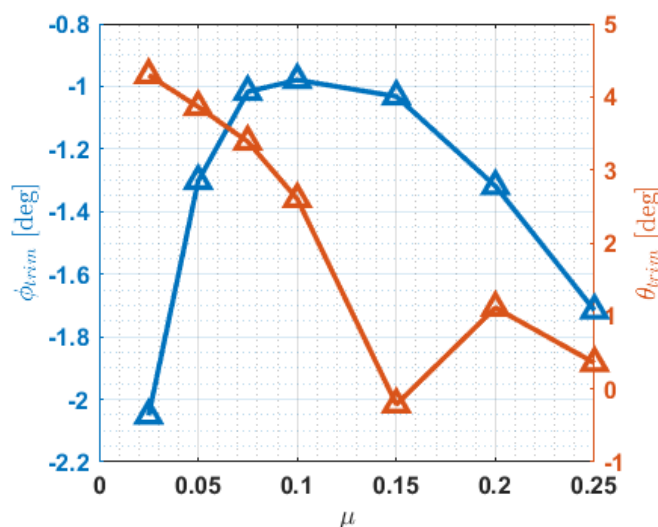


Figure 185: Aircraft trim state in simulations investigating response to the isolated straight vortex disturbance field.

Conventional Helicopter Response to a Horseshoe Vortex Disturbance

We had previously used the horseshoe vortex representation of ship airwake disturbance together with the analytical model given by Eq. (39) to investigate the influence of ship geometry and rotor vertical position variation on fundamental rotor response. Here, we examine the response of the ExHel flight dynamic model to the disturbance associated with the vortex, modeling it both as a frozen and distorting field. The aircraft is assumed to be in hover with the rotor placed in line with the disturbance, which is read in via airwake data files as perturbations to the freestream, set to a 30-knot headwind. The model is placed sufficiently downstream to render the influence of the starting filament negligible, effectively generating a symmetric disturbance field from the trailing filaments of the vortex. The ExHel model is first trimmed without disturbance. Time-marching simulations are then performed with the disturbance module turned on. To isolate the aircraft fundamental response, aircraft position and controls are held

fixed during this time-marching simulation. We record thirty second time-histories of the responses and use time-averaged values in the results that will be presented below. The first 5 seconds of the simulation are discarded to eliminate transient effects. We consider two gross weight values: 15,000 lbs. and 20,000 lbs. Note that we only consider the effect of vortex width variation, and that the vortex core radius is dimensionalized using the hangar height. A snapshot of the distorting vortex particle initialization is shown in Figure 186.

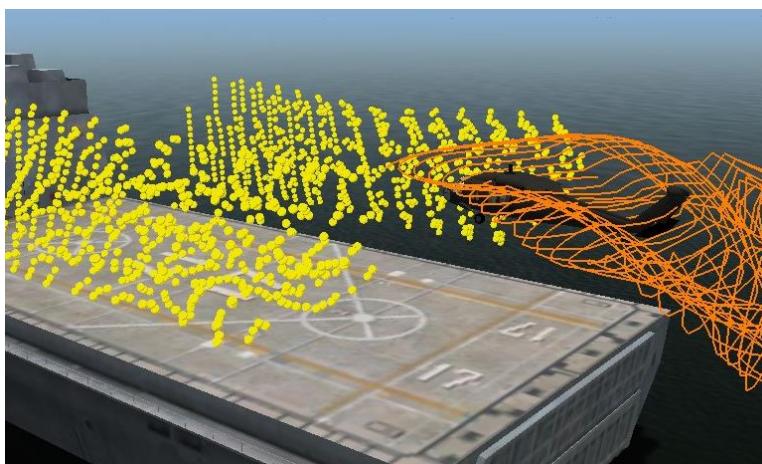


Figure 186: Vortex particles corresponding to the horseshoe vortex disturbance are initialized upstream of the aircraft and transported with freestream towards the aircraft.

The influence of the horseshoe disturbance on the ExHel main rotor thrust response is given in Figure 187 for the two gross weight values considered. The analytical rotor response for a 15,000lb idealized UH-60 rotor is plotted for comparison. The responses for the distorting case are close to each other while a slight offset is present between the values predicted with the frozen field assumption. Additionally, the distorting case results in smaller changes in blade loading when compared to the frozen case. The analytical response is the most conservative and displays the largest changes in response. With increasing width, the mutual distortion of the ship airwake and rotor wake results in less significant changes. Peak response is noted for $W_{\text{beam}} = 2.4R$.

Changes in rotor coning induced by the influence of the horseshoe vortex are shown in Figure 188, where results from ExHel have greater magnitudes than the analytical predictions. Peak response is again noted at $W_{\text{beam}} = 2.4R$. Changes for both gross weight values are close to each other when the disturbance is modeled as a frozen or distorting field, with a slight offset of $\sim 0.1^\circ$ between the two. Similarly to the thrust responses, the distorting case produces smaller response changes that quickly reduce in intensity with increasing widths.

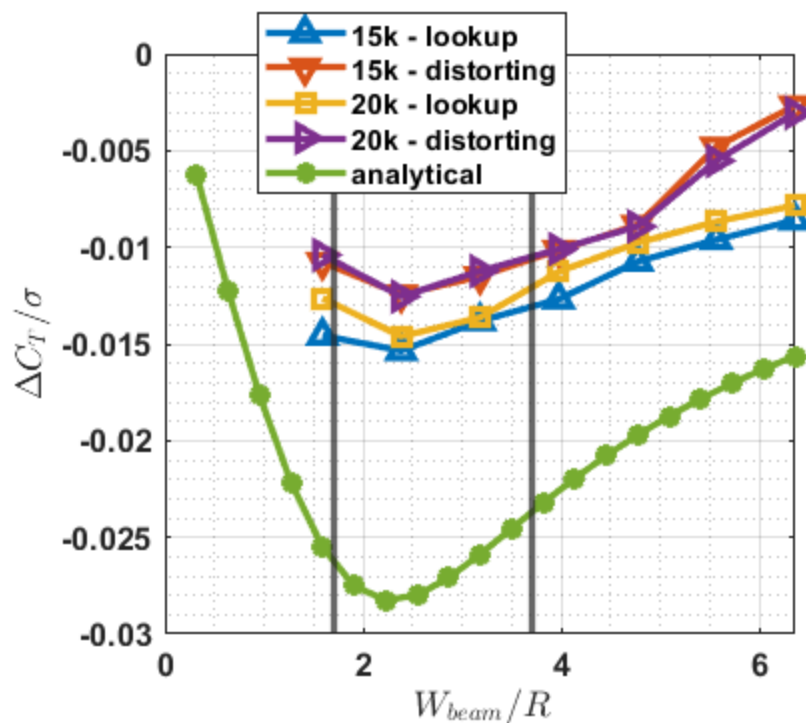


Figure 187: Changes in blade loading due to horseshoe vortex influence predicted using ExHel simulations; vertical lines delimit typical range for a DDG type ship.

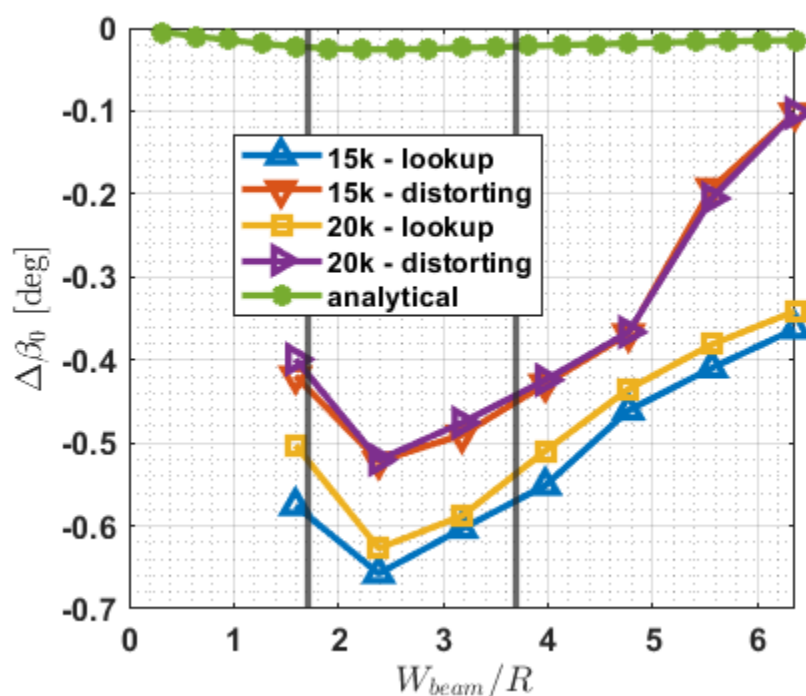


Figure 188: Changes in rotor coning due to horseshoe vortex influence predicted using ExHel simulations; vertical lines delimit typical range for a DDG type ship.

The modeling of full aircraft introduces shaft tilt, fuselage, empennage, tail rotor and aircraft attitude effects in the solution, resulting in nonzero longitudinal and lateral tilt responses to the

vortex disturbance, as shown in Figure 189 and Figure 190. Within the typical width region bounded by the vertical lines, the longitudinal tilt responses in Figure 189 have an average change value close to 1° , with the changes in 20,000 lb. model being approximately $\sim 0.2^\circ$ greater than those in the 15,000 lb. model for both frozen and distorting cases. The lateral disk tilt responses in Figure 190 have relatively small magnitudes when compared to the coning and longitudinal tilt responses due to the symmetry of the vortex disturbance, with a peak change of approximately 0.2° at $W_{\text{beam}} = 1.5R$. Interestingly, the response for the 20,000 lb. model has the opposite sign of that from the 15,000 lb. model at this width. This could be potentially due to the difference in baseline trim state, summarized in Table 13 for the two models.

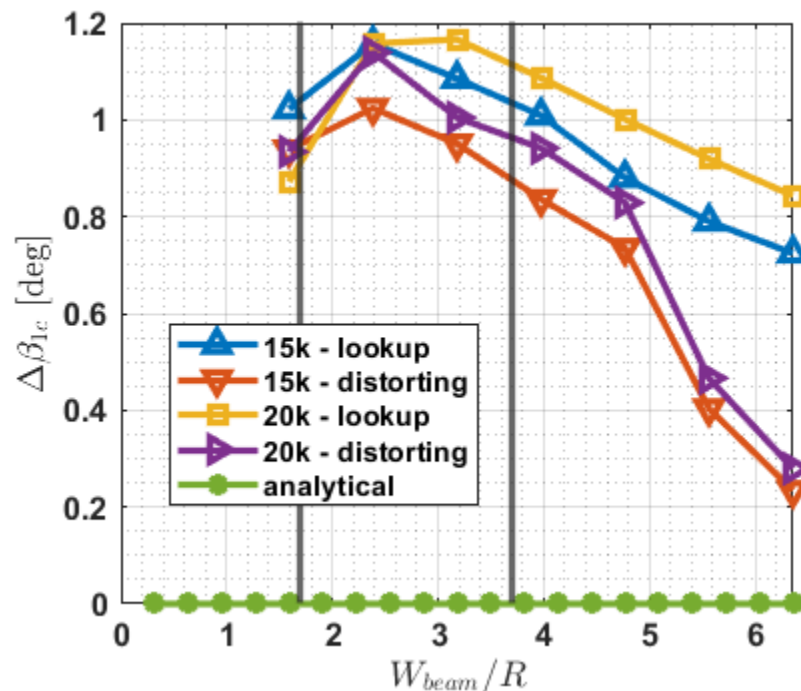


Figure 189: Changes in rotor longitudinal tilt due to horseshoe vortex influence predicted using ExHel simulations; vertical lines delimit typical range for a DDG type ship.

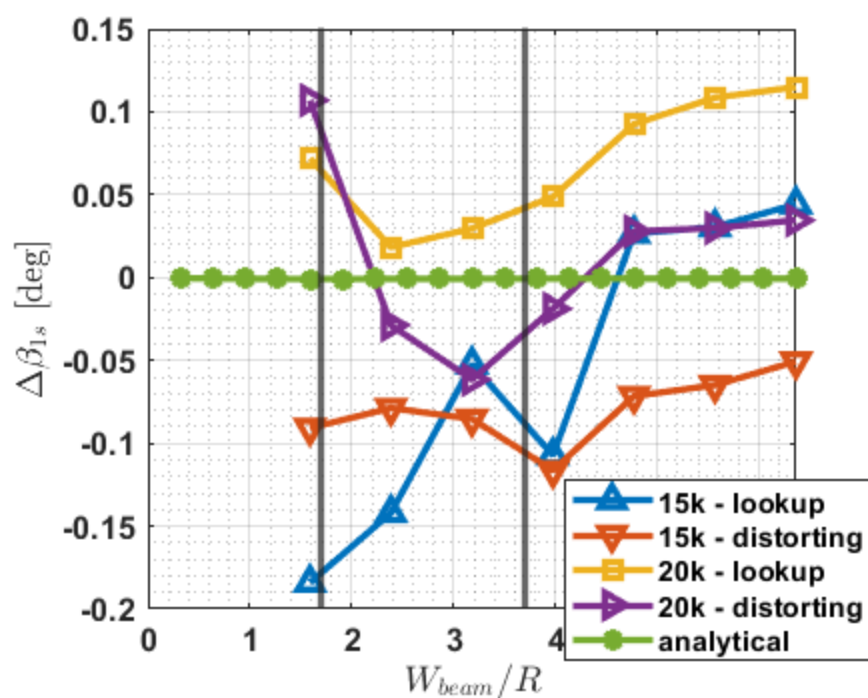


Figure 190: Changes in rotor lateral tilt due to horseshoe vortex influence predicted using ExHel simulations; vertical lines delimit typical range for a DDG type ship.

Table 13: Aircraft trim state (in degrees) during simulations investigating influence of horseshoe vortex.

Property	15000 lb. model	20000 lb. model
ϕ_{trim}	-1.06	-1.04
θ_{trim}	3.44	4.02
$\beta_{0\text{trim}}$	2.75	3.89
$\beta_{1c\text{trim}}$	-0.08	0.09
$\beta_{1s\text{trim}}$	-1.43	-1.77

Tiltrotor Response to Horseshoe Vortex Disturbance

The rest of this section has been removed because it contains tiltrotor predictions that are Distribution D. The full text can be found in CDI Report 24-01.

Conclusions

Flight simulation has long been recognized as a valuable tool for gaining insights into the complex ship-rotorcraft dynamic interface, where highly unsteady ship airwake from a moving ship structure interacts with the aircraft and increases pilot workload. A fundamental understanding of what is “good enough” when selecting modeling fidelity for use in such simulations is currently missing. This effort aimed at investigating both ship airwake and rotorcraft response from a fundamental physics perspective to derive scalable parameters and observations that can provide initial insights into dynamic interface of any given ship-rotorcraft dynamic interface without having to rely on expensive configuration-specific simulations. We approached the problem by first focusing on ship airwake characterization. We employed two approaches to ship airwake characterization, both of which use time-averaged data from time-accurate CFD simulations of flow over a cube—a simplified ship superstructure representation—as basis. In one methodology, two dimensional analytical models were fitted onto the features to capture the associated longitudinal and vertical velocity profiles. Two-dimensional models were chosen as starting point for simplicity and because they provide a basis for a fundamental, “ground-up” approach to parametric airwake reconstruction. The model input parameters also provide some insight into physical phenomena and can be nondimensionalized and applied to other rectangular obstacles. In the second methodology, we characterized 3D flow features directly as vortical structures in terms of geometry (position, size) and strength. Fundamental rotorcraft responses to simplistic disturbance representations were subsequently examined. We considered various rotor modeling fidelity levels ranging from isolated analytical rotor response investigations to full aircraft dynamic simulations where representative vortical disturbance structures were modeled as a frozen field with the disturbances fixed in space (also dubbed “one-way coupling”) and as a distorting field (“full coupling”) obtained by solving the Lagrangian vorticity-velocity form incompressible Navier-Stokes equations. We considered responses to two primary vortical structures: an isolated vortical tube representative of the trailing wake from a fixed-wing aircraft and a horseshoe vortex derived from the ship airwake characterization investigation. We summarize the primary conclusions of the effort next:

1. The parametric model fitting approach to airwake characterization provides an avenue for fast and representative reconstruction of time-averaged velocities associated with flow over rectangular obstacles while providing insights into flow phenomena such as recirculation regions. In this work, we derived analytical parametric expressions for time-averaged longitudinal and vertical velocity profiles and demonstrated their applicability to flow over a rectangular geometry that was not considered in the derivation of the parametric relations.
2. The vortex-based characterization of airwake provides a physical basis for scalable representation of potent vortical structures that directly impact rotorcraft flying qualities based on ship geometry and freestream. We characterized the arch vortex typically observed aft of rectangular obstacles in headwind conditions. We also identified and characterized a horseshoe vortex that allowed for representative reconstruction of vertical velocity, which directly influences rotor inflow, downstream of the obstacle using the Biot-Savart law combined with the Vatistas core model. We demonstrated the applicability of this horseshoe characterization, which was obtained using a headwind simulation of flow over a cube, to flow over a generic ship geometry derived using publicly available ship geometry data.

3. The time-averaged airwake characterization from the two methods we considered can be combined with unsteady predictions from a stochastic model such as the von Karman or Dryden turbulence model to obtain representative time-dependent airwake data. For unsteadiness, the nondimensional vortex shedding frequency or the Strouhal number is an important scaling parameter. We investigated the use of a simple Fourier representation of unsteadiness along with a multi-bin proper orthogonal decomposition representation of fluctuating velocity components during the effort.
4. For all disturbances considered, inclusion of full aircraft dynamics attenuates rotor thrust and flapping responses when compared to isolated analytical predictions. This can be attributed to the inclusion of additional helicopter components and to the trim state of the aircraft, which, unlike the isolated rotor analytical investigations, introduces additional disk tilts relative to the center line of the idealized disturbances.
5. Rotor responses to the simplistic disturbances point towards the existence of scaling parameters that can “collapse” responses of different rotors onto each other. For the horseshoe vortex disturbance, a potential scaling relationship exists between nondimensional blade loading and vortex width, when the latter is expressed as a product of beam-to-rotor radius ratio and vortex strength, which is nondimensionalized, in turn, by the product of rotor area and rotation speed.
6. Modeling fidelity requirements vary depending on the type of disturbance considered. For the isolated straight vortex, modeling the disturbance as a distorting field for low forward speeds is appropriate given the large differences in response between the frozen and distorting cases. For the horseshoe vortex, rotor responses in the distorting cases were less than those from the frozen cases for all widths considered.

Recommendations

The work performed during the effort enabled a more in-depth and fundamental look into the complex ship-rotorcraft dynamic interface. Given the complexity of the problem, additional research on the topic remains to be done.

Between the two ship airwake characterization methodologies considered, the physical vortex-based approach provided a more reliable basis for scaling studies. Further characterization of flow structures such as deck edge vortices associated with flow over flat-top ships should be conducted for better understanding of ship airwake disturbances that influence rotorcraft flying qualities. Additionally, more research into unsteady characterization beyond Strouhal number is also warranted. All simulations in the present work considered only headwind flow features. Consideration of oblique flow is another area for further research.

Both analytical and full aircraft dynamic simulations were carried out using idealized vortex structures. Aircraft responses with actual ship airwake data for various ship configurations would supplement results from these idealized disturbance investigations. For simplicity and more transparency into fundamental aircraft response, all dynamic flight simulations were performed with the aircraft state and controls fixed at trim values. The test matrix of cases that were considered should be expanded to include increasingly complex flight control influence so as to better contextualize results from our investigations in informing future DI simulations.

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REPORT DOCUMENTATION PAGE				<i>Form Approved</i> OMB No. 0704-0188	
Public reporting burden for this collection of information is estimated to average 1 hour per response, including the time for reviewing instructions, searching data sources, gathering and maintaining the data needed, and completing and reviewing the collection of information. Send comments regarding this burden estimate or any other aspect of this collection of information, including suggestions for reducing this burden to Washington Headquarters Service, Directorate for Information Operations and Reports, 1215 Jefferson Davis Highway, Suite 1204, Arlington, VA 22202-4302, and to the Office of Management and Budget, Paperwork Reduction Project (0704-0188) Washington, DC 20503. PLEASE DO NOT RETURN YOUR FORM TO THE ABOVE ADDRESS.					
1. REPORT DATE (DD-MM-YYYY) 03-05-2024		2. REPORT TYPE Final report		3. DATES COVERED (From - To) 3/4/2021 – 3/3/2024	
4. TITLE AND SUBTITLE Establishing Fluid Dynamics Scales Critical to Dynamic Interface Applications and their Impact on Handling Qualities				5a. CONTRACT NUMBER N00014-21-C-1044	
				5b. GRANT NUMBER	
				5c. PROGRAM ELEMENT NUMBER	
6. AUTHOR(S) Glen R. Whitehouse, Abhinav Sharma, Jeffrey D. Keller				5d. PROJECT NUMBER	
				5e. TASK NUMBER	
				5f. WORK UNIT NUMBER	
7. PERFORMING ORGANIZATION NAME(S) AND ADDRESS(ES) Continuum Dynamics, Inc. 34 Lexington Ave., Ewing, NJ, 08618				8. PERFORMING ORGANIZATION REPORT NUMBER CDI Report 24-07	
9. SPONSORING/MONITORING AGENCY NAME(S) AND ADDRESS(ES) Office of Naval Research 875 North Randolph Street Arlington VA 22203				10. SPONSOR/MONITOR'S ACRONYM(S) ONR	
				11. SPONSORING/MONITORING AGENCY REPORT NUMBER	
12. DISTRIBUTION AVAILABILITY STATEMENT Distribution Statement A:					
13. SUPPLEMENTARY NOTES This is the Distribution A version of CDI Report 24-01. CDI Report 24-01 is Distribution D and includes tiltrotor predictions.					
14. ABSTRACT Understanding of the aeromechanics, flight dynamics and handling qualities of a rotorcraft flying through a disturbance field, such as a ship airwake, is limited. Research suggests that the rotorcraft's response is fundamentally different to that of fixed wing aircraft, but very little is understood about the relationship between the disturbance field's length and time scales and the rotorcraft's response. The goal of the effort was to improve our understanding of this interaction.					
15. SUBJECT TERMS rotorcraft, aeromechanics, handling qualities, flight dynamics, interactions, disturbance fields					
16. SECURITY CLASSIFICATION OF:			17. LIMITATION OF ABSTRACT U		18. NUMBER OF PAGES 230
a. REPORT U	b. ABSTRACT U	c. THIS PAGE U			
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