

Experimental and Numerical Aerodynamic Analysis of an Elevated Beachfront House

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Abstract

Elevating coastal houses enables residential communities to reduce the risk of flooding due to tropical cyclones. However, wind-induced damage during such events requires an understanding of the inherent wind forces to improve damage mitigation techniques and assessment of climate-related risk in insurance models. In this study, wind-tunnel experiments and computational fluid dynamics (CFD) simulations are conducted for a typical elevated 1:25 scale beachfront house, possessing a 5:12 pitched gable roof with overhanging eave. An atmospheric boundary layer (ABL) wind field is generated in a low-speed wind-tunnel to replicate conditions experienced during tropical cyclones. Testing is performed for a range of incident wind angles to understand the full aerodynamic consequences of strong winds. Measured pressure coefficient (C_p) distributions are compared with CFD simulations using steady-state and transient Delayed-Detached Eddy Simulation (DDES) within ANSYS Fluent 2021 R1. Net C_p values surrounding the overhanging eave are considered to evaluate the role of this typical geometrical feature. It was found that larger uplift suction occurred at incident wind angles of 45° and above, after which the suction remained stable. The roof panels are subjected to the greatest upward suction, where critical regions occur at the roof ridge. The size of the low-pressure regions is determined by the incident wind angle and ensuing flow separation wherein DDES is found to reproduce additional aerodynamic features arising from unsteady turbulent flow. DDES offers improved predictive capability when mean pressure forces are considered but falls short as an accurate means to efficiently evaluate peak C_p distributions.

Keywords: Wind tunnel experiment, Computational Fluid Dynamics (CFD), Steady-State RANS, DDES simulation, Coastal residential house, Overhanging eave

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1. Introduction

Coastal beachfront houses in tropical cyclone regions are vulnerable to a plethora of hazards, most notably, flooding and high-winds. To mitigate damage due to flooding, buildings may be elevated above the base flood elevation (BFE) level. As a result, not only is the house still susceptible to high wind-loading on the roof and wall cladding panels, but also on the building underside, hence its aerodynamic characteristics differ from traditional low-rise buildings. Due to the predicted increase in destructive potential of tropical cyclones resulting from climate-change (Tebaldi et al., 2012; Estrada et al., 2015; Li and Chakraborty, 2020) and the importance of improving climate-risk prediction models (Pinelli et al., 2011; Baradaranshoraka et al., 2017), research activity for this building configuration has increased within recent years. The present research is concerned with hazards due to high wind-speeds which are assessed through experimental and numerical investigations. For an overview of other remaining hazards the reader is referred to a recent comprehensive review compiled by Amini and Memari (2020).



Figure 1: Photographs of elevated coastal residential houses on Dauphin Island, Alabama, USA. 27 Nov. 2016. Author's (Guoji Xu) personal collection.

A simplified elevated beachfront house geometry is considered that resembles those found in communities of Southern US Gulf states such as Texas, Mississippi, Alabama, and Louisiana, but also South Atlantic states such as Florida, North Carolina, and South Carolina. An example of which can be seen in Fig. 1 which shows typical elevated beachfront houses on Dauphin Island in Alabama, USA. Initial experimental wind studies by Holmes (1994) regarding an elevated house were conducted for various building groupings, roof-pitches, and elevation heights to investigate the influence of the surface pressure distribution on these parameters. More recently, experimental testing has been performed at the Wall of Wind (WOW) facility within Florida International University with findings also compared to ASCE 7-16 and AS/NZS 1170.2 wind loading standards (Abdelfatah et al., 2020). Their work indicates the importance of considering the negative pressure zones on the floor section since larger suction forces were found surrounding the building piles and near the underside edges, with a higher elevation giving rise to greater negative pressures in these areas. The piles also generate additional vortices which grow in strength with elevation

height, thus resulting in greater negative peak pressure coefficient values. Other studies led by the University of Kansas, who conducted wind tunnel testing within the WOW have also focused on underside floor damage, indicating that the magnitude of the pressure coefficients on the roof and underside are similar, hence deliberate design choices should be made for the underside building components ([Kim et al., 2019, 2020](#)).

To complement the aforementioned experimental findings, computational fluid dynamics (CFD) simulations were conducted for a low-rise elevated coastal house model by [Amini and Memari \(2021a\)](#) for different elevations using steady-state Reynolds-Averaged Navier–Stokes (RANS) turbulence modeling. Their investigation numerically demonstrated that increased wind speeds and turbulence effects surrounding the house as well as a larger re-circulation zone in the wake region and underside of the structure arise from a higher building elevation. The influence of building stilt height reflecting the suction forces acting on the building underside has also been considered by [Abdelfatah and Elawady \(2021\)](#) as well as multi-story elevated houses for which the results were compared with current building standard provisions ([Abdelfatah et al., 2022](#)). The overall motivation for these studies is to improve standard practices and overall structural safety regarding the wind design of such building types.

Although experimental data is able to provide peak pressure values on the building components, the aforementioned CFD studies utilize steady-state RANS turbulence modeling which is unable to capture these important peak values due to its time-averaged numerical formulation. This therefore means flow-field information such as velocity fluctuations and turbulent kinetic energy (TKE) surrounding the critical underside and wake regions may not be fully realized. It is suggested within the literature that Large-Eddy Simulation (LES) turbulence modeling may prove favorable for an inquiry into the peak pressure coefficients and subsequent fluctuations for determination of potential wind-induced damage. To that end, a recent LES investigation has been performed by [Amini and Memari \(2021b\)](#) that considers the peak pressure coefficients and transient aerodynamics of the building underside. LES was able to provide these important peak pressure coefficients at this location and reveal more intricate aerodynamic features surrounding the building piles than RANS methodologies. However, the study still lacks a complete investigation into the unsteady aerodynamics of the entire building geometry and the authors did not consider what additional information could be obtained from wind-induced forces on the roof components.

The use of CFD in this regard has and will continue to experience widespread use and development for computational wind engineering (CWE) applications, hence it is advantageous to continue development of a building aerodynamics CFD portfolio to complement and extend experimental data sets ([Blocken, 2014](#)). For the majority of cases wherein CFD is used to complement measured data derived from wind tunnel experiments, steady-state RANS simulations form the bulk of these studies due to their practicality and reasonably accurate results. Within the building landscape, for example, studies concerning the aerodynamics of gable roof houses ([Tominaga et al., 2015](#)), pyramidal roofs ([Singh and Roy, 2019](#)), pedestrian comfort level investigations ([Fernando et al., 2020](#)), and building ventilation ([Perén et al., 2015](#)) have all made use of RANS modeling. RANS simulations have also been contrasted with current Eurocode building standings, highlighting the importance of the turbulence

modeling strategy and deficiencies in the steady-state approach for recovering/designing future building provisions (Khalil et al., 2022). Therefore, the CWE community has begun to extensively employ LES to resolve complex flow patterns. LES is capable of resolving important transient flow structures which are not captured during RANS modeling such as complex re-circulation regions and vortex shedding, as well as being able to extract peak pressure coefficients due to its inherent time-dependent formulation. Despite these obvious advantages, RANS still offers benefits in terms of its computational costs and due to a lack of best practice guidelines, LES may even generate less accurate solutions (Blocken, 2018). Examples of CWE studies that have adopted LES practices include aerodynamic flow around tall buildings (Tominaga et al., 2008a; Elshaer et al., 2016; Lamberti and Gorlé, 2020), consideration of complex building features (Zheng et al., 2020), building ventilation (van Hooff et al., 2017), as well as applications geared towards large-scale infrastructure such as bridge engineering (Han et al., 2018).

The current investigation presents experimental surface pressure data measured in a low-speed atmospheric boundary layer (ABL) wind tunnel at Wuhan University alongside a steady-state and transient CFD analysis using a simplified elevated house with overhanging eave. Current literature reveals little consideration to house models with an overhanging eave, a geometrical feature typical in the aforementioned beachfront houses and whose aerodynamic role is therefore relatively unknown in this context. Steady-state CFD simulations are performed using the common RANS practices and transient simulations are performed using Delayed-Detached Eddy Simulation (DDES) which is initialized using the RANS results. DDES employs RANS turbulence modeling in the near-wall region; however, it is capable of resolving larger scales much like LES and can therefore provide geometry-dependent unsteady statistics. Furthermore, because spatial discretization requirements and the numerical setup are relatively straightforward, CFD practitioners can apply this technology without a need for stringent best practice guidelines. More detail, including the requisite governing equations, regarding the theory of DDES is provided in Sec. 3.2. To-date, DDES has been employed within the CWE landscape by Liu and Niu (2016) for a high-rise building where it was found that similar wind-fields were reported between LES and DDES, the latter being considerably less computational expensive and hence presents itself as a viable candidate for practical engineering applications. However, the authors did not consider the predictive capability of DDES for surface pressure coefficients compared to other turbulence modeling practices.

This article presents experimental, as well as steady and unsteady CFD analysis for a gable-roof elevated beachfront house with overhanging eave. The building is subject to incident wind angles ranging from 0° through 90° in increments of 15° . This parameter space is critical for aerodynamic quantification since quartering winds were found to produce greater suction across the roof panels during field measurements taken during tropical cyclones Mujigae (2015) and Sarika (2016) on a low-rise building located in Hainan, China (Wang et al., 2018). As well as providing a unique data set for consideration of future building standards, this knowledge can assist in developing relevant performance-based design methodologies for coastal structures (Do et al., 2016) and aid policy-makers in creating appropriate disaster response and countermeasure frameworks (Suppasri et al., 2013). The remainder of

this article is organized as follows: the investigation methodology is provided in Sec. 2, the numerical setup is given in Sec. 3 that includes a description of the atmospheric boundary layer (ABL) in Sec. 3.1, turbulence modeling in Sec. 3.2, grid generation in Sec. 3.3, and simulation setup in Sec. 3.4. Results are then given in Sec. 4 beginning with a CFD validation and verification in Sec. 4.1, followed by analysis concerning wind forces on the roof components in Sec. 4.2, the overhanging eave in Sec. 4.3, aerodynamics resulting from the roof ridge in Sec. 4.4, and peak pressure coefficients in Sec. 4.5. Finally, conclusions are given in Sec. 5.

2. Experimental Setup

Experimentation was performed within the low-speed boundary layer wind tunnel at Wuhan University using a geometric scale of 1:25. The wind tunnel has a working section of 3.2 m in width, 2.1 m in height, and a length of 16 m. Vortex generators and roughness elements are positioned at the tunnel entrance to enhance exposure through turbulence generation. A photograph of the experimental setup showcasing the elevated coastal house model is shown in Fig. 2a. The model consists of a 5:12 pitched gable roof house with an overhanging eave. The coastal house model has length 0.78 m, width 0.536 m, and building height $H = 0.39$ m, which is situated on a rotating board to modify the impinging wind direction accordingly. This corresponds to blockage ratios of 4.53% and 3.11% for the 0° and 90° building orientations, respectively. The surface roughness length is approximated from the calibrated atmospheric boundary layer (ABL) profile using,

$$z_0 \approx \exp \left[\frac{u(z_2) \ln(z_1) - u(z_1) \ln(z_2)}{u(z_2) - u(z_1)} \right] = 3 \times 10^{-5} \text{ m}, \quad (1)$$

where z_1 and z_2 represent two independent reference heights within the experimental domain. At full-scale this corresponds to a roughness length of $z_0^{\text{scale}} \approx 0.001$ m which characterizes very flat open terrain (Holmes, 2018). This low value of roughness length may in part be attributed to the lower portion of the boundary layer traveling over smooth terrain after interaction with the wind tunnel roughness elements. This may lead to a smooth boundary layer forming in the region between the roughness elements and the test section. The friction velocity u_{ABL}^* is determined using the formula,

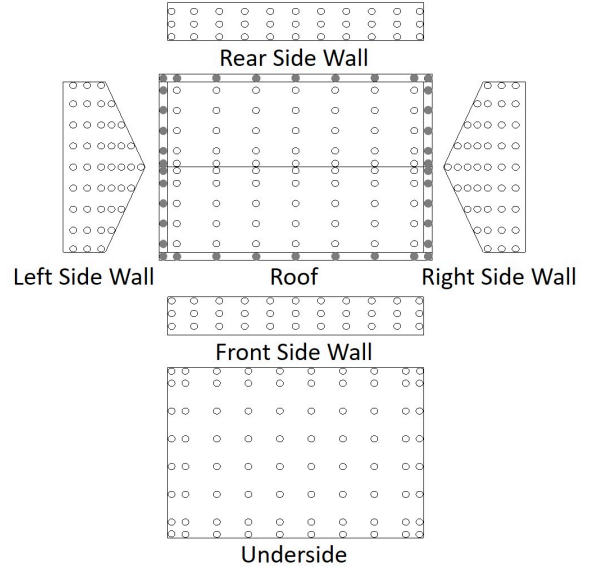
$$u_{\text{ABL}}^* = \frac{\kappa U_{\text{ref}}}{\ln \left(\frac{z_{\text{ref}} + z_0}{z_0} \right)} = 0.279 \text{ m/s}, \quad (2)$$

where $U_{\text{ref}} \approx 6.3$ m/s is the ABL velocity at the reference height $z_{\text{ref}} = 0.256$ m, which corresponds to the mean roof height. From these available quantities, the surface-roughness Reynolds number for the upwind fetch, $u_{\text{ABL}}^* z_0 / \nu = 0.59$ is found to be lower than the minimum required value of 2.5 given within the ASCE 49-12 Standard (ASCE, 2021), where $\nu = 1.78 \times 10^{-5} \text{ m}^2\text{s}^{-1}$ is the kinematic viscosity.

The house model is retrofitted with 378 surface pressure taps, the arrangement of which is shown in Fig. 2b. A multi-channel simultaneous fluctuating pressure measurement system



(a) Photograph of the coastal house model.



(b) Pressure tap arrangement.

Figure 2: Photograph and pressure tap arrangement of the elevated coastal house model used in the low-speed boundary layer wind tunnel at Wuhan University.

was used to obtain the time-histories of the fluctuating pressure with a sampling frequency of 331 Hz for which a low-pass filter was not applied. A total of ten samples per wind direction of duration 96 s were recorded which corresponds to approximately ten 10-minute samples for a full-scale model. The experimental and von Kármán power spectra of the measured fluctuating wind-speed in the streamwise direction following the methods outlined in [Holmes \(2018\)](#) are shown in Fig. 3, where the integral length scale is estimated at $L_u^x \approx 0.15$ m. The reported integral length scale is significantly smaller than the house model dimensions. This implies that the experimental results presented herein only serve to validate the subsequent numerical study and should be treated with caution if one were to make comparisons with the ASCE building design or load codes. The pressure distribution is computed for each pressure tap through the dimensionless pressure coefficient (C_p),

$$C_p = \frac{p - p_0}{0.5\rho u^2}, \quad (3)$$

where p denotes the instantaneous pressure on the building surface and p_0 is the reference static pressure. To compute the mean C_p distribution, Eq. (3) is determined at each pressure tap and averaged over the ten samples. Peak pressure coefficients are determined using the well-established Cook and Mayne method ([Cook and Mayne, 1980](#)).

3. Numerical Setup

All numerical simulations were performed using the commercial CFD software ANSYS Fluent 2021 R1 with grid generation achieved using Pointwise V18.3 R2, hereafter referred to

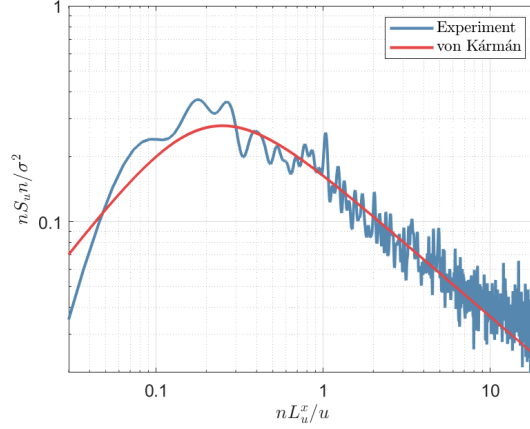


Figure 3: Normalized spectral density function of the fluctuating wind-speed in the streamwise direction.

as Fluent and Pointwise, respectively. Numerical simulations utilized the finite volume discretization method to solve the three-dimensional (3D) steady-state incompressible Navier–Stokes equations with an underlying RANS turbulence modeling approach. From previous investigations that sought to assess the k – ϵ RANS model family, it was found that the k – ϵ RNG model proved superior in terms of predicting the TKE around the roof corner and velocity profile within the re-circulation region (Tominaga and Stathopoulos, 2009). Further studies by Tominaga et al. (2008b) indicated that the k – ϵ RNG and k – ω SST (Shear Stress Transport) models fare well for building aerodynamic applications in terms of the TKE distribution and velocity profile prediction. In terms of the surface pressure distribution, the k – ω Standard model has been shown to offer better performance when directly compared to the k – ϵ Realizable model (Ozmen et al., 2016). In the present study, the k – ω SST model is extensively used through consideration of previous work by Amini and Memari (2021b) who also sought to evaluate the surface pressure distribution over an elevated coastal house using the k – ϵ Standard, RNG, and Realizable RANS formulations, as well as the k – ω SST model. In addition to the k – ω SST model, the k – ω Standard model is also briefly evaluated since the grid requirements are identical (Menter, 1994).

3.1. Atmospheric Boundary Layer (ABL)

Initial and boundary conditions are imposed following the experiment based on a 1:25 scale coastal house model, as described in Sec. 2. A best effort has been made to replicate the experimental ABL through the prescribed inlet conditions. The velocity inlet is given by (Hargreaves and Wright, 2007),

$$u(z) = \frac{u_{ABL}^*}{\kappa} \ln \left(\frac{z + z_0}{z_0} \right), \quad (4)$$

where $\kappa = 0.4$ is the von Kármán constant, and u_{ABL}^* and z_0 were found from the experimental data in Sec. 2. The initial kinetic energy profile is determined through data-fitting the

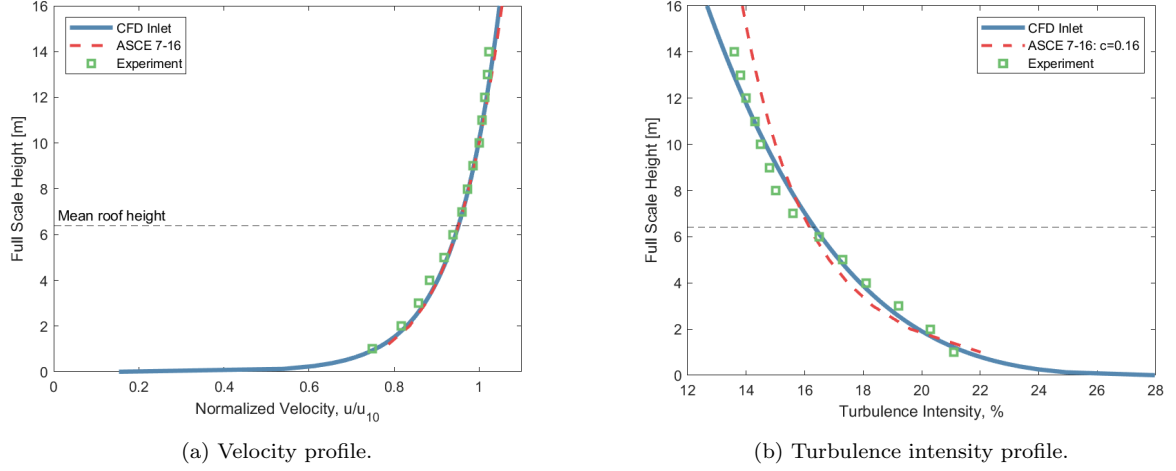


Figure 4: Computational and experimental ABL profiles for the x -velocity and turbulence intensity.

experimental turbulence intensity values $I(z)$ and the computational velocity profile from Eq. (4) using,

$$k(z) = \frac{3}{2} (I(z)u(z))^2. \quad (5)$$

The turbulence dissipation rate is given by,

$$\epsilon(z) = \frac{u_{ABL}^{*3}}{\kappa(z + z_0)}, \quad (6)$$

such that the specific turbulence dissipation is expressed through,

$$\omega(z) = \frac{\epsilon(z)}{C_\mu k(z)}, \quad (7)$$

where $C_\mu = 0.09$. An interpreted User-Defined Function (UDF) was used within Fluent for the x -velocity u , kinetic energy k , and specific dissipation rate ω at the velocity-inlet boundary. Computational and experimental inlet profiles for the x -velocity and turbulence intensity profiles are shown in Figs. 4a and 4b, respectively, where it can be seen that reasonable agreement exists between the experimental and CFD data, as well as theoretical values according to a terrain exposure of Type D within the ASCE 7-16 building standards (ASCE, 2016).

A no-slip boundary condition is applied to the ground following a wall roughness specified through the sand-grain roughness parameters in Fluent. As noted by previous authors, the roughness height k_s is sometimes mistakenly set equal to the roughness length z_0 (Hargreaves and Wright, 2007). The formula used within Fluent to estimate the roughness height, based on the roughness length, is given by (Blocken et al., 2007),

$$k_s = \frac{9.793}{C_s} z_0 = 5.9 \times 10^{-4} \text{ m}, \quad (8)$$

whereby the default value for the constant $C_s = 0.5$ is chosen. Similarly, the no-slip boundary condition is applied to the house surface such that the surface is assumed to be smooth, hence the roughness height is set equal to zero and $C_s = 1$.

3.2. Turbulence Modeling Overview

While Reynolds-Averaged Navier–Stokes (RANS) turbulence modeling prevails in the field of fluid dynamics engineering, its time-averaged statistical description also means that temporal features cannot be resolved. Extensions have been proposed for unsteady flows, labeled the unsteady RANS, or URANS, approach and applied to engineering applications with modest success. The time-averaged description was later replaced by a spatial averaging procedure with the intent to capture the dominating large features of the flow in time. This approach was termed Large-Eddy Simulation (LES) and has been successfully applied to a range of applications, showing superior performance over URANS. This gain in performance comes at the cost of computational time which is manifested by the stringent grid requirements for a wall-resolved LES grid. Several authors have provided estimates for grid requirements for wall-bounded LES flows and showed that the number of required grid points scales with $N \sim Re^{13/7}$ (Georgiadis et al., 2010; Choi and Moin, 2012). This still requires significantly fewer grid points compared to resolving all turbulence scales explicitly, which scales with $N \sim Re^{37/14}$. This scaling is obtained by requiring $50 \leq x^+ \leq 150$, $y^+ < 1$ and $15 \leq z^+ \leq 30$ for the grid points on the wall where the x -direction is aligned with the flow, the y -direction is in the wall-normal direction and the z -direction span-wise to the flow. This scaling was obtained for flat plate geometries but largely holds for wall-bounded flows.

To overcome this restrictive modeling requirement, a hybrid RANS/LES approach was proposed, where the inner part of the boundary layer is modeled through RANS (effectively removing the grid requirements near the wall) while the outer region of the boundary layer, as well as the far-field, is modeled through LES (Spalart et al., 1997). The key idea is that, away from any solid wall, LES and RANS grid resolutions are not much different and thus it is possible to obtain LES-like resolution at URANS-like computational cost. This approach is termed the Detached-Eddy Simulation (DES). When DES was first introduced, it was found that flows could spuriously separate when the grid spacing was locally too small. In these cases, LES-like structures are being resolved while using a RANS turbulence modeling approach which has lead to a so-called Grid-Induced Separation (GIS). A new model was later developed which was termed the Delayed-DES, or DDES, model, which overrides the switching from RANS to LES in portions of the boundary layer where the grid spacing becomes locally smaller than the boundary layer height (Spalart et al., 2006). This model has been used successfully for a variety of applications and proven to overcome the drawbacks from the original DES model.

This study solves the three-dimensional (3D) Navier–Stokes equations within the RANS and DDES framework using the k – ω SST turbulence model as the underlying RANS representation. The Navier–Stokes equations are solved as an incompressible flow in the form of,

$$\nabla \cdot \mathbf{u} = 0, \tag{9}$$

$$\frac{\partial \mathbf{u}}{\partial t} + (\mathbf{u} \cdot \nabla) \mathbf{u} = -\frac{1}{\rho} \nabla p + \nu \nabla^2 \mathbf{u} + \nabla \cdot \tau. \quad (10)$$

Eqs. (9) and (10) are decoupled from each other which is manifested by the lack of a pressure dependence on the continuity equation. Classical approximate projection methods such as the SIMPLE algorithm are available to couple both equations together in a semi-implicit manner. However, this approach leads to a loosely coupled system, which results in strict requirements for the computational grid and the chosen time-step. In order to relax this requirement, a fully coupled system is used in the present work, where Eqs. (9) and (10) are rewritten in matrix form, linearized, and then solved simultaneously. Within Fluent, this numerical scheme is known as the pressure-based coupled algorithm within the spatial discretization scheme options.

Eq. (10) requires the solution of the Reynolds stress tensor given by $\nabla \cdot \tau$ whereby the k - ω SST RANS model is used to close the system. The additional transport equations for the turbulence kinetic energy k and specific dissipation rate ω are given as,

$$\frac{\partial \rho k}{\partial t} + \nabla \cdot (\rho \mathbf{u} k) = \nabla \cdot [(\mu + \sigma_k \mu_t) \nabla k] + P_k - \frac{\rho \sqrt{k^3}}{l_{DDES}}, \quad (11)$$

$$\frac{\partial \rho \omega}{\partial t} + \nabla \cdot (\rho \mathbf{u} \omega) = \nabla \cdot [(\mu + \sigma_\omega \mu_t) \nabla \omega] + 2(1 - F_1) \rho \sigma_{\omega 2} \frac{\nabla k \cdot \nabla \omega}{\omega} + \alpha \frac{\rho}{\mu_t} P_k - \beta \rho \omega^2. \quad (12)$$

The additional eddy viscosity μ_t appearing in Eq. (11) and Eq. (12) is solved as,

$$\mu_t = \rho \frac{a_1 \cdot k}{\max(a_1 \cdot \omega, F_2 \cdot S)}. \quad (13)$$

The switching between RANS and LES is achieved through the blended DDES length scale l_{DDES} in Eq. (11) and expressed through,

$$l_{DDES} = l_{RANS} - f_d \max(0, l_{RANS} - l_{LES}), \quad (14)$$

where,

$$l_{RANS} = \frac{\sqrt{k}}{C_{\mu} \omega}, \quad (15)$$

$$l_{LES} = [C_{DES1} \cdot F_1 + C_{DES2} \cdot (1 - F_1)] \cdot h_{max}, \quad (16)$$

with h_{max} being the maximum edge length of a given cell. The remaining terms are either empirical correlation functions or closure coefficients, which are given in detail in Appendix 1 of [Gritskevich et al. \(2012\)](#) to which the reader is referred for more detail.

3.3. Grid Generation

An unstructured grid generation strategy was employed to resolve the building model due to the complexity resulting from the overhanging eaved roof. Prism layers surround the model whereby the height of the first cell is chosen to correspond with a dimensionless wall-distance $y^+ \approx 1$ to ensure grid compatibility with the $k-\omega$ SST RANS model. The height of the first cell adjacent to the model Δs is found through the formula,

$$y^+ \approx \frac{u_{\text{ABL}}^* \Delta s}{\nu}, \quad (17)$$

where u_{ABL}^* is the friction velocity of the ABL computed using Eq. (2). A total of 15 prism layers were generated with a linear growth-rate of 1.2 which is deemed sufficient to resolve the viscous-layer, buffer-layer, and log-law region. Fig. 5a provides a visual perspective of how the grid is generated surrounding the building surface.

The total computational domain encompasses a region $5H$ to the side, $15H$ behind, and $5H$ above the building model. This choice reflects best-practice guidelines as used by previous authors to ensure the computational domain is not overly constrained, see e.g., [Amini and Memari \(2020\)](#) and [Tominaga et al. \(2008b\)](#). A high-density grid region is created surrounding the model which, as illustrated in Fig. 6, extends to $5H$ behind the model, $3H$ in front, $2.5H$ to the sides, and $2.5H$ above. The higher density region acts to reduce the need for a large number of cells throughout the domain and provides better resolution for capturing any key aerodynamic flow tendencies within the wake region. Three grids of successively finer resolution within the high-density region were computed for verification purposes, the results of which are discussed in Sec. 4.1. The refinement region can be seen visually in Fig. 5b for the medium level of grid resolution used.

3.4. Simulation Setup

All transient simulations are initialized with solutions obtained using steady-state RANS following the methods described above. The pressure-based coupled algorithm is chosen, which offers a more robust and efficient solution for single-phase steady-state flows compared to the alternatives typically used in the literature such as the SIMPLE algorithm.

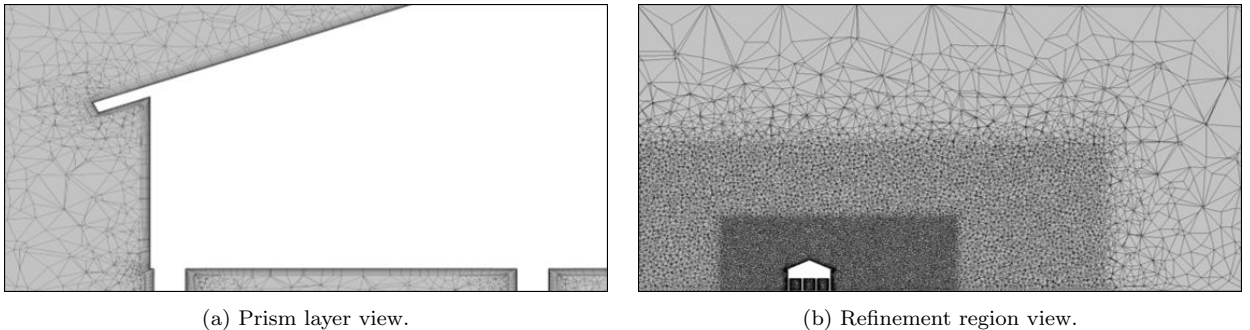


Figure 5: Center-line view of the medium computational grid showing the refinement region and prism layers surrounding the building.

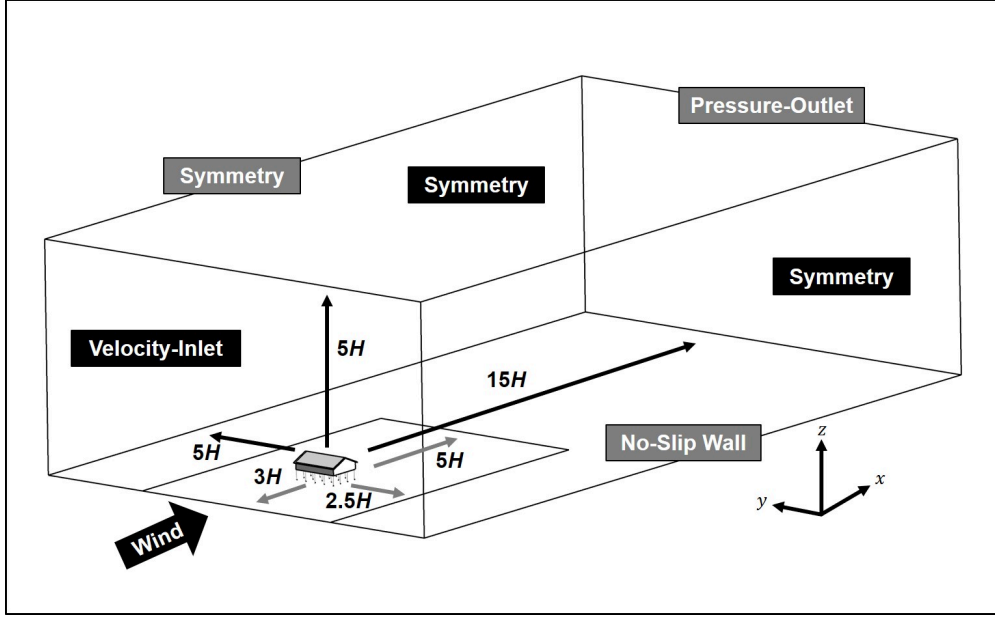


Figure 6: Schematic diagram of the computational domain and corresponding boundary conditions.

Within this choice of numerical method, second-order accurate discretization schemes for the pressure, velocity, and turbulence quantities are employed. The turbulence model is then altered to utilize the DDES methodology which is underpinned by a $k-\omega$ SST RANS model. Steady-state RANS simulations are performed in parallel using four processors on a desktop computer with 32 GB of RAM until a convergence criteria of 10^{-8} is reached for the continuity and velocity residuals, and 10^{-6} for the kinetic energy and specific dissipation rate residuals. Typically, this convergence condition is met within 200 iterations. For DDES simulations, the momentum equation is discretized using the bounded central differencing method and a first-order implicit method is employed for the time discretization. A Courant number of 200 is chosen alongside a corresponding time-step size of $\Delta t = 0.002$ s which consistently achieves convergence within 15 iterations per time-step. DDES simulations spanning 10 s of elapsed flow-time complete, on average, within 37 hours and 15 minutes for parallel computations using 16 CPUs.

4. Results

4.1. CFD Validation and Verification

Before numerical simulations were performed over the building orientation parameter space, a quantitative grid independence study by calculation of the Grid Convergence Index (GCI) and Richardson extrapolation analysis was conducted for the 0° incident wind angle using steady-state RANS simulations (Roache, 2009). Successively finer grids are generated which should result in a reduction of the grid (spatial) discretization errors as the resolution increases and asymptotically approaches zero, with the exclusion of computer round-off error. This analysis is performed for both the $k-\omega$ Standard and SST variants since previously

reported literature has indicated that the $k-\omega$ RANS family produces preferential results for the computed C_p distribution. The grid convergence study considers a coarse (4,852,121 cells), medium (7,166,840 cells), and fine (15,126,536 cells) grid, where the refinement occurs within the region near to the building, as shown by the rectangle surrounding the house in Fig. 6. To quantify the impact of refining the grid, the roof uplift force on the roof components, comprised of the roof panels, fascia, and soffit, is used. In addition, the envelope uplift force is considered which encompasses the aforementioned roof components and the remainder of the building envelope, i.e., sides and floor. The horizontal force on the building envelope was also considered, however, the reported values remained consistent between grid resolutions and was thus considered an insufficient metric for evaluating grid convergence. The forces reported within Fluent are composed of pressure and viscous forces acting on the building components.

Table 1 lists the computed forces and their respective GCI values. It can be seen that the $k-\omega$ SST produces a GCI percentage of less than 1% for the roof uplift force and less than 10% for the envelope uplift force between the medium and fine grid resolutions. In contrast, the $k-\omega$ Standard model reports GCI percentages of more than 50% and 25% for the roof and envelope uplift forces, respectively. These two models therefore demonstrate conflicting convergence properties wherein the $k-\omega$ SST performs favorably.

In addition to the GCI analysis, Richardson extrapolation is used to understand how the solution may further change if an extremely high grid resolution is employed. Fig. 7 presents findings of the Richardson extrapolation procedure for the two uplift forces considered. It can be seen that the two RANS models predict remarkably different values for the forces as the resolution is increased, and that the extrapolated value for the $k-\omega$ Standard model is a far-cry from the previously obtained results. Given the increased computation cost of employing the finest grid resolution, and the fact that the GCI indicates a subtle difference between the medium and fine grids for the $k-\omega$ SST model, one would opt to use this RANS model and the medium grid resolution for further analysis on balance of these arguments. Typically, CFD practitioners will perform a grid convergence study using a single RANS model and proceed to obtain results using the chosen grid for all models. In practice, it is argued that this may not always yield a fair comparison, since it has been observed that

Table 1: Grid Convergence Index (GCI) evaluation for increasing grid resolutions during the 0° incident wind angle.

	Grid	Roof Uplift Force [N]	Grid Convergence Index (GCI), %	Envelope Uplift Force [N]	Grid Convergence Index (GCI), %
$k-\omega$ SST	Coarse ^a	2.42	-	-2.18	-
	Medium ^b	2.21	9.72	-2.65	-55.40
	Fine ^c	2.16	0.88	-2.87	-9.13
$k-\omega$ Standard	Coarse ^a	1.22	-	-2.66	-
	Medium ^b	0.77	139.01	-3.24	-103.02
	Fine ^c	0.51	57.76	-3.61	-25.15

No. of Cells: ^a4,852,121, ^b7,166,840, ^c15,126,536.

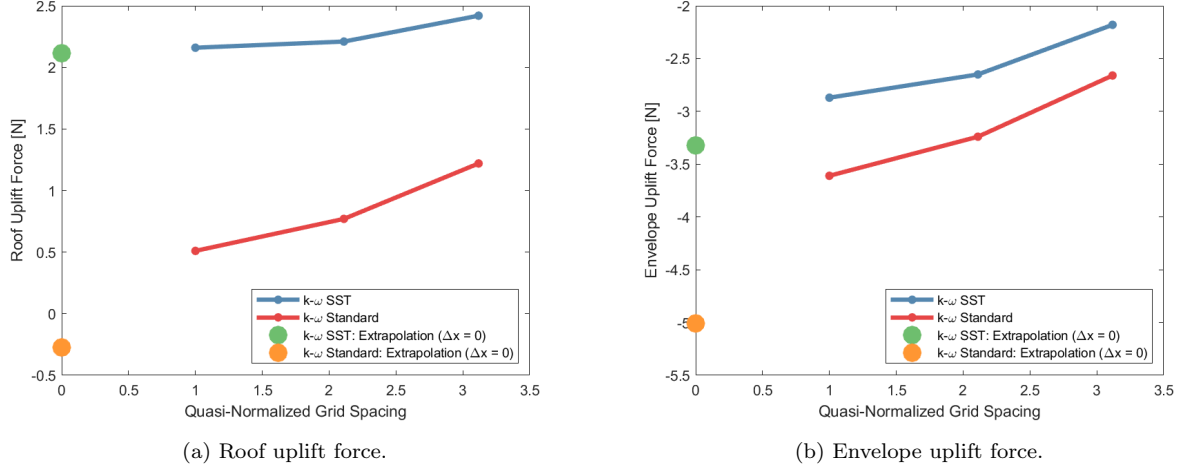


Figure 7: Richardson extrapolation findings for the roof uplift force (7a) and envelope uplift force (7b) considering the $k-\omega$ Standard and $k-\omega$ SST RANS models.

the $k-\omega$ Standard variant does not reproduce satisfactory grid convergence properties for this problem. Therefore, practitioners are advised to conduct systematic grid convergence studies for all turbulence models under their consideration.

By choosing the medium grid resolution, RANS simulations are then quantitatively assessed with respect to the mean experimental data. Two profiles are selected surrounding the building envelope to directly compare the computed and measured mean C_p values: one surrounding the center of the envelope (Center) and one surrounding the eave (Eave) at the end of the building, which is chosen to be the windward eave as the orientation increases. These profiles can be seen labeled accordingly in Fig. 8. Locations of the experimental pressure taps are replicated on the CFD model which are then directly compared through calculation of the mean absolute error (MAE) given by,

$$\text{MAE} = \frac{1}{n} \sum_{i=1}^n |C_{p_i}^{\text{EXP}} - C_{p_i}^{\text{CFD}}|, \quad (18)$$

where $C_{p_i}^{\text{EXP}}$ and $C_{p_i}^{\text{CFD}}$ refer to the experimental and numerical value of the mean pressure coefficient at pressure tap i , respectively, and n denotes the total number of pressure taps in the profile.

Results of the MAE calculation are listed in Table 2 for the two $k-\omega$ variants over the incident wind angle parameter space. For both models, it can be seen that the Eave profile presents the greatest disparity between experimental and numerical results, which can largely be attributed to the difficulty of steady-state numerical calculations in regions of physically unsteady turbulent flow. For both the Center and Eave profiles, the MAE grows as the building rotates between 0° and 45° , again indicating further difficulty for oblique wind loading directions due to cornering winds. While both models report similar trends in their MAE over the orientation parameter space, the $k-\omega$ SST model is shown to provide better agreement with respect to the mean experimental data.

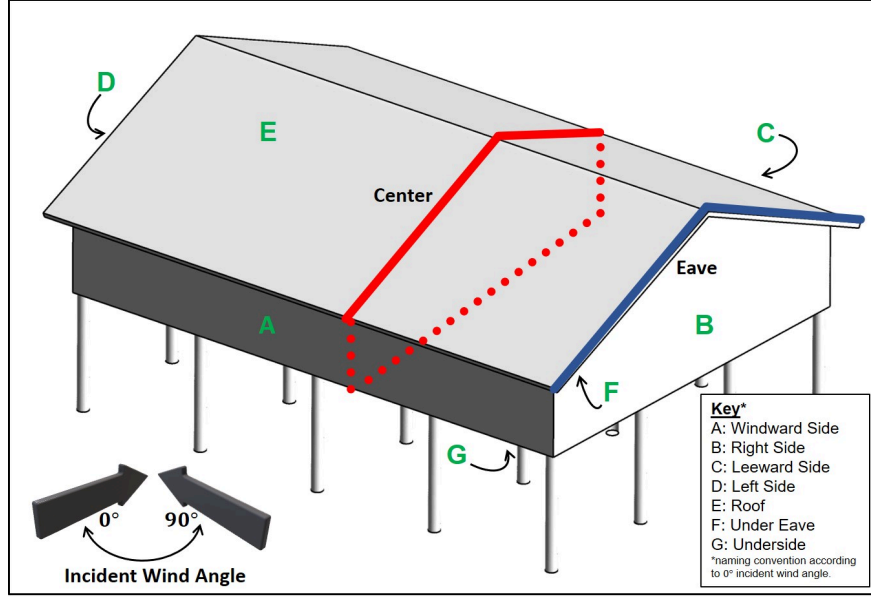


Figure 8: Schematic diagram of the building model with Center and Eave profiles used in the CFD validation process.

Table 2: Mean Absolute Errors (MAE) reported at the building center-line (Center) and windward eave (Eave) locations for all building orientations.

		Mean Absolute Error (MAE)							
Orientation [deg.]		0	15	30	45	60	75	90	
$k-\omega$ Standard	Center	0.20	0.19	0.17	0.15	0.11	0.06	0.11	0.98
	Eave	0.21	0.25	0.38	0.56	0.76	0.94	0.98	4.08
$k-\omega$ SST	Center	0.07	0.07	0.10	0.09	0.09	0.06	0.06	0.54
	Eave	0.16	0.16	0.17	0.26	0.22	0.18	0.10	1.25
									Total

Given the disparity so far between the $k-\omega$ Standard and SST variants, the C_p profiles across the Center profile are shown in Fig. 9 to better understand how these models differ in performance. Considering the 0° incident wind angle, it can be seen how the $k-\omega$ SST model reproduces the trend of the experimental profile with greater consistency than the $k-\omega$ Standard model. C_p values on the windward side and roof panels are reportedly much higher for the $k-\omega$ Standard model compared to the $k-\omega$ SST model and experimental data. In the leeward regions however, the data sets demonstrate better agreement. For the building underside, the $k-\omega$ SST model is able to better replicate the experimental data in terms of the maximum suction experienced due to flow separation yet yields a later re-attached point than the $k-\omega$ Standard model. It is also noted that the RANS models yield differences in the C_p value at the roof ridge, where lower C_p values with respect to the experiment are reported.

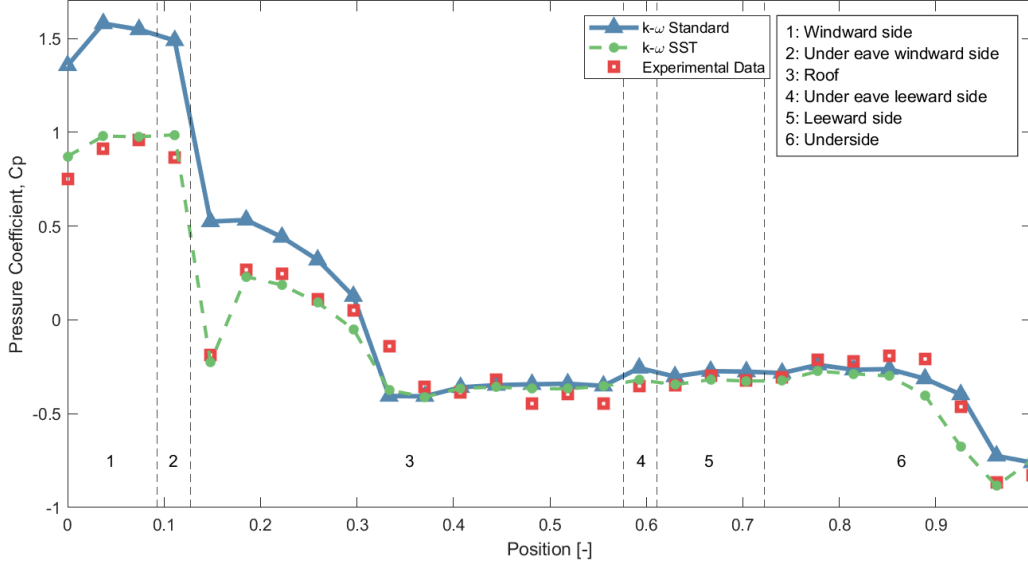


Figure 9: Pressure coefficient (C_p) profiles over the Center profile as specified in Fig. 8 for the $k-\omega$ variants and mean experimental data subject to the 0° incident wind angle.

4.2. House Forces

Following the initial RANS validation and verification procedure, transient effects of the wind forces exerted across the building envelope and roof components are used to examine the statistical convergence of the DDES simulations. The time histories of the roof uplift forces acting on the building roof components for the duration of the DDES simulations are shown in Fig. 10 alongside their corresponding steady-state RANS predictions. The forces are exported directly from Fluent at each time-step and a running average is computed to determine the statistical convergence. For each building orientation, the fluctuations of the roof uplift force demonstrate differing tendencies. For the incident wind angles ranging from $0-30^\circ$, the force variations are characterized by periodic oscillations whereas for the angles of 45° and above, the forces fluctuate at a much higher frequency. For all cases, the running average uplift force can be seen to have stabilized before the final elapsed simulation time of $t = 10$ s which is indicative that the results have achieved statistical convergence, i.e., fully-developed flow, and mean flow quantities can be safely recovered from the DDES simulation results.

The DDES simulations are generally found to predict a smaller uplift force on the roof components than the steady-state RANS simulations, with the exception of the 75° and 90° incident wind angles. This is illustrated in Fig. 10h which shows the forces computed using the two simulation methodologies over the orientation parameter space. The smallest and largest uplift force is found to occur at 0° and 45° incident wind angles, respectively, which is a consistent conclusion between DDES and RANS. However, whereas RANS predicts a decline in the uplift forces as the orientation surpasses 45° , DDES maintains a similar, marginally smaller, force. This finding can be attributed to the inability of RANS to capture

important transient flow behavior since this disparity between RANS and DDES is found to occur when the forces oscillate at a high-frequency, indicating more aggressive unsteady flow characteristics that RANS is incapable of realizing in any great detail. These findings are however in contrast to a previous study where the maximum roof uplift force is found to be when the house resides at an angle of 0° to the wind direction¹ (Amini and Memari, 2021b). Their work did not consider an overhanging eave and lower angles of roof pitch (10° and 11.3°), whereas the present work considers a 22.6° pitch. This is indicative of the importance of roof pitch and consideration of an overhanging eave for such analysis since conclusions regarding the roof forces cannot always be generalized.

4.3. Overhanging Eave Consideration

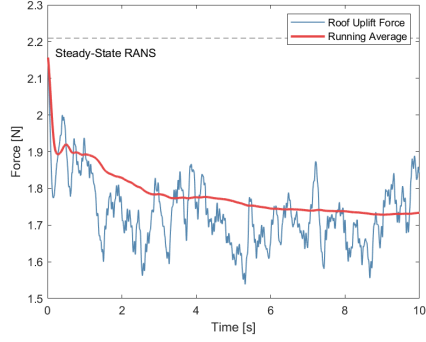
Pressure coefficient measurements are taken at isolated pressure taps at equivalent locations for the experimental and numerical data on the roof panel and soffit. Measurements surrounding the overhanging eave include the taps on top of and underneath the overhang (soffit), the gray dots on the roof section in Fig. 2b show their locations.

The pressure coefficients reported at locations under, i.e., on the soffit, and above the overhanging eave are reported in Fig. 11. This figure illustrates how the mean C_p changes around the eave as the incident wind angle varies. For incident wind angles of 0° and 15° , the eave experiences suction on either side. At angles of 30° and above, positive C_p values on the soffit (with the exception of the RANS result) indicate that this geometric feature is directly contributing to an upward force on the roof. The pressures in these locations are then combined and shown alongside the pressures reported on the main roof panels to yield insight in to the net upward suctions experienced by the roof.

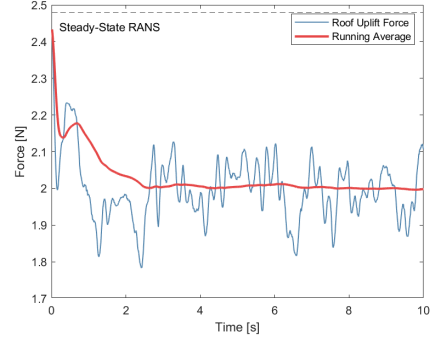
The average mean and minimum C_p values over all pressure taps are determined and the net C_p value for the roof components are shown in Fig. 12. RANS, mean DDES, and mean experimental data are shown in Fig. 12a, and minimum peak coefficients obtained from DDES and experimental data are plotted in Fig. 12b. The total net C_p is shown in terms of the roof panel and eave surfaces (top and soffit). Immediately, it is noted that the entire roof experiences a net negative C_p value corresponding to upward suction, the largest proportion of which is due to the major roof panels. All data sets demonstrate an increasing trend of upward suction as the orientation transitions between 0° and 45° , and slightly decreases thereafter.

For the mean data-set in Fig. 12a, RANS data is found to consistently over-predict the reported suctions from experimental and DDES data. The maximum suction is found at a 45° orientation for the RANS and experimental data but at 60° for the mean DDES data. The DDES data marginally under-predicts the experimental data until the orientation surpasses 60° . DDES minimum peak pressure coefficients shown in Fig. 12b consistently under-predict that of the experimental minimum peaks yet interestingly their trend is identical. Minimum and maximum suction are found at 0° and 45° orientations, respectively. As the orientation exceeds 45° the suction decreases slightly before rising at the final 90° orientation. As

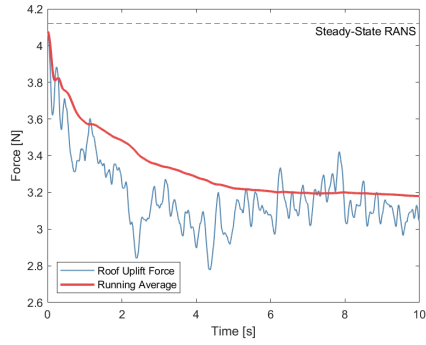
¹In the study conducted by Amini and Memari (2021b), a rotation angle of 0° in their work corresponds to 90° in the present work.



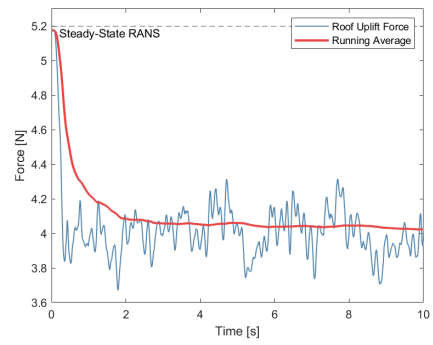
(a) 0° orientation.



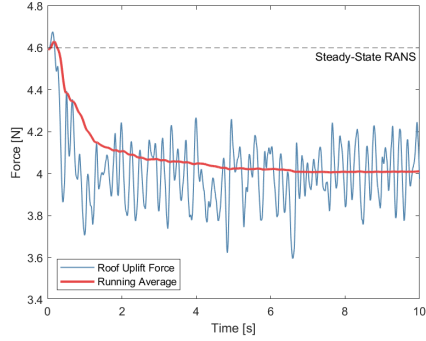
(b) 15° orientation.



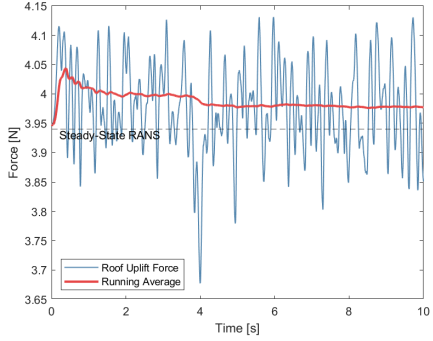
(c) 30° orientation.



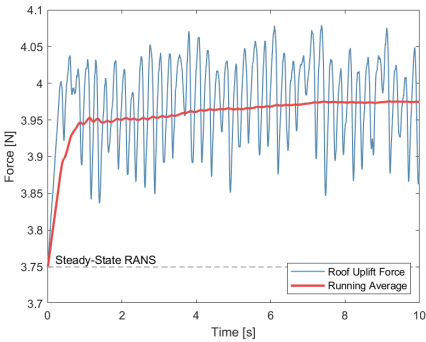
(d) 45° orientation.



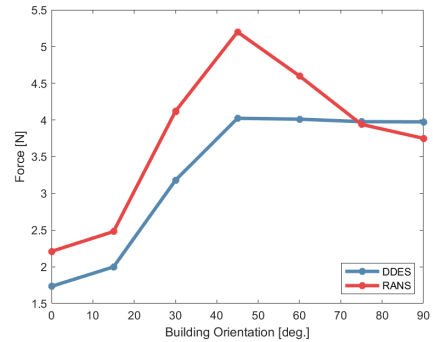
(e) 60° orientation.



(f) 75° orientation.



(g) 90° orientation.



(h) Summary of building orientations.

Figure 10: Time histories of the uplift force exerted on the roof components.

demonstrated in Sec. 4.2, incident wind angles exceeding 45° leads to increased transience in the roof uplift force. This may partly explain why the trends of the peak pressure coefficients differ from that of the mean values since geometry-dependent aerodynamics produce more unsteady flow surrounding the roof components.

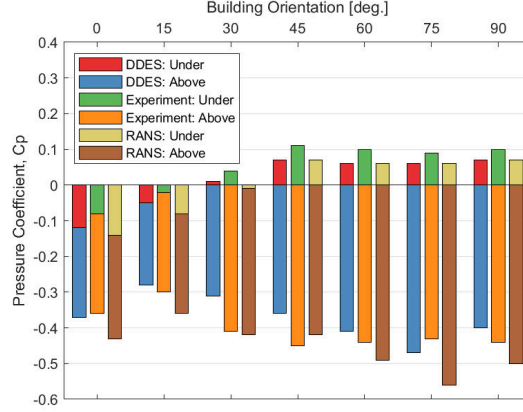
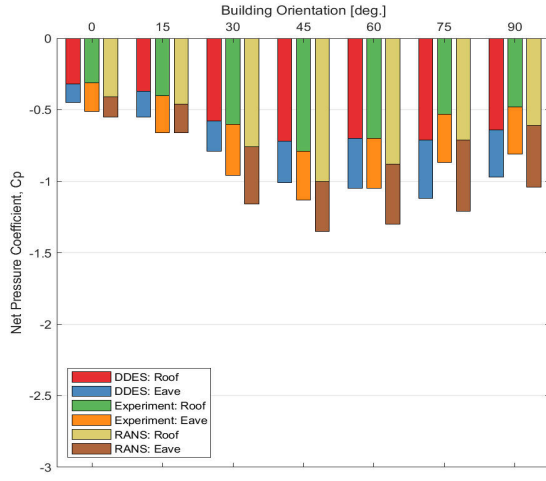


Figure 11: Mean pressure coefficient (C_p) values at pressure tap locations above and under the overhanging eave.

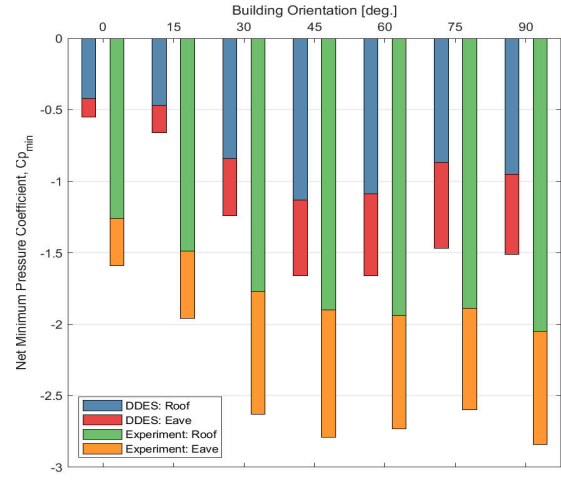
Fig. 13 displays selected net C_p profiles surrounding the overhanging eave for the DDES and experimental data. Mean data profiles for the 0° incident wind that recorded the smallest roof uplift force are shown in Fig. 13a. A similar pressure distribution is reported with the most disparity found at the windward eave section and across the eave section sides where the CFD over-predicts and under-predicts the net C_p values in the windward and leeward regions, respectively. The 45° incident wind yields a large suction region at the roof ridge of the overhanging eave, which is only captured to a much lesser extent by the DDES simulations. Finally, the 90° incident wind angle is considered both in terms of the mean and minimum peak pressure coefficients in Figs. 13c and 13d, respectively. While similar profiles are observed for both mechanisms of analysis, the largest deviation between data-sets is found at the windward eave section, whereby the experimental data predicts greater suction. The same finding is also found at the central roof ridge on the windward side, in similar fashion to the 45° incident wind case. This suggests critical room for improvement regarding CFD practices since these geometrical features, which are typical for elevated beachfront houses, present a serious challenge regarding numerical modeling capability. Future work should therefore carefully consider the implication of different turbulence modeling approaches and choice of numerical schemes for this important aerodynamic detail.

4.4. Roof Ridge Considerations

CFD can be further leveraged to understand the building aerodynamics causing the suction regions previously noted at the roof ridge. Elevation of the building envelope has been shown to result in a separation bubble in the leeward vicinity of the building and a



(a) Mean and RANS data.

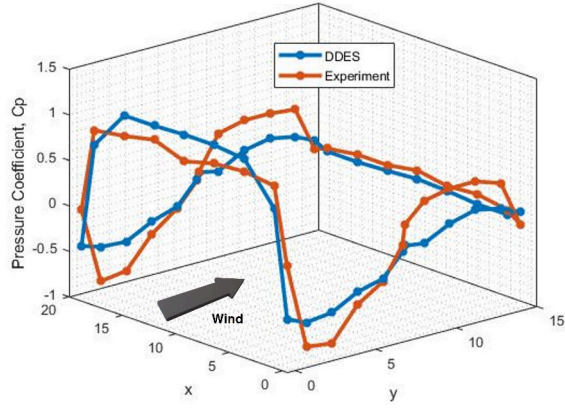


(b) Minimum peak coefficients.

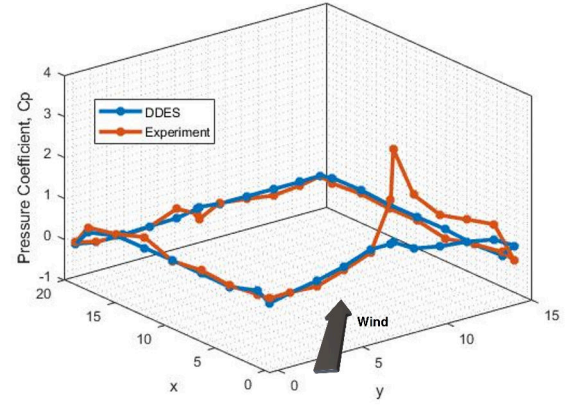
Figure 12: Net C_p contributions to roof panel and overhanging eave components.

heightened region of turbulent kinetic energy (TKE) downwind of the roof ridge for steady-state RANS simulations (Amini and Memari, 2021a). In the case of a 0° orientation, this is shown in Fig. 14 as a further comparison between the capabilities of RANS and DDES modeling strategies. It is clear within Fig. 14a that a distinct separation bubble is present that contains a re-circulation region. DDES results, however, do not reproduce such a distinct bubble. Instead, the simulation exhibits a more diffuse region in the wake whereby a separation bubble is unable to clearly form. These flow characteristics also have important repercussions for the leeward turbulent kinetic energy (TKE) zones, wherein RANS predicts high-levels of TKE at the building windward face and leeward of the roof ridge. Higher levels of TKE are also found at the windward face under the DDES procedure, yet the same region of heightened TKE leeward of the roof ridge is absent. Instead, the TKE is dissipated throughout the wake region due to a now-resolved unsteady wake. This difference in predicted TKE distribution may be crucial for determining safe building spacing within communities of elevated beachfront houses since the level of TKE produced by the wind–building interaction may be less severe than previous CFD studies have indicated.

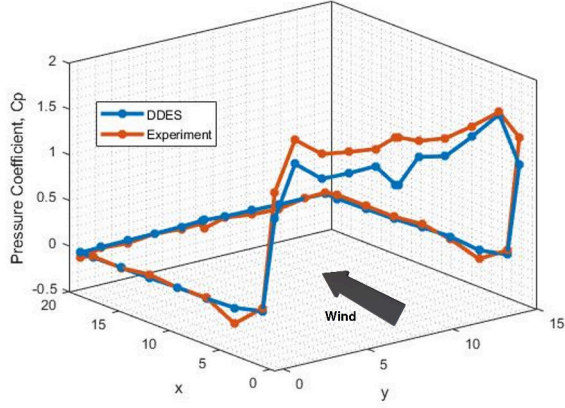
Fig. 15 provides a plan view of the velocity field using a plane located at eave height for the 0° , 45° , and 90° incident wind angles. This reveals insight into the flow features captured during RANS and DDES simulations and their general wake structures. RANS results yield clear, well-structured, wake regions whereas DDES reproduces unorganized regions of time-dependent turbulent flow. For the 45° orientation in Figs. 15c and 15d, RANS predicts two distinct wake regions separated by a region of faster-moving flow. DDES, on the other hand, indicates that these regions are connected across the leeward side wherein some degree of vortex-shedding is likely to occur. Overall, the wake regions show clear vortex structures using DDES which cannot be captured accurately during steady-state analysis. Finally, it is noted that the 90° incident wind angle in Figs. 15e and 15f reveals a bypass transition on the building sides towards the leeward edge. These vortices are captured in both RANS



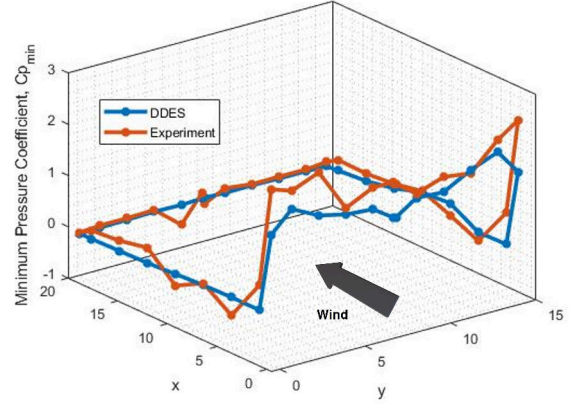
(a) Mean: 0° orientation.



(b) Mean: 45° orientation.

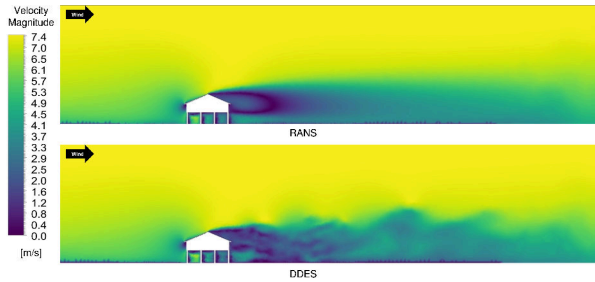


(c) Mean: 90° orientation.

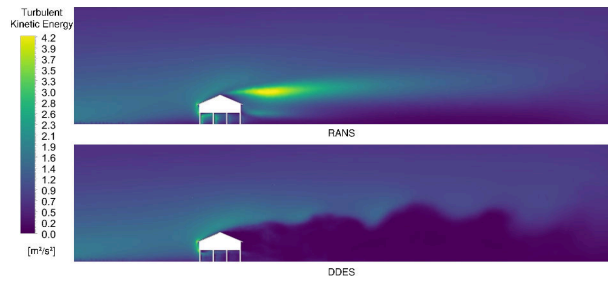


(d) Minimum: 90° orientation.

Figure 13: Net C_p values surrounding the overhanging eave pressure taps.



(a) Velocity magnitude.



(b) Turbulent kinetic energy (TKE).

Figure 14: Centerline comparison of the velocity magnitude and turbulent kinetic energy (TKE) between RANS and DDES (instantaneous at $t = 10$ s) for a 0° incident wind angle.

and DDES simulations, yet in more detail for the latter, as shown in the figure insets. It was noted in Sec. 4.3 that at these windward corners the CFD and experimental data differed substantially in their prediction of the net C_p acting on the overhanging eave. This can therefore likely be attributed to the complex aerodynamics at this location which

will require special attention in any future investigations. Furthermore, experimental wake profiles should also be obtained to facilitate a more complete comparison between CFD and experimental findings.

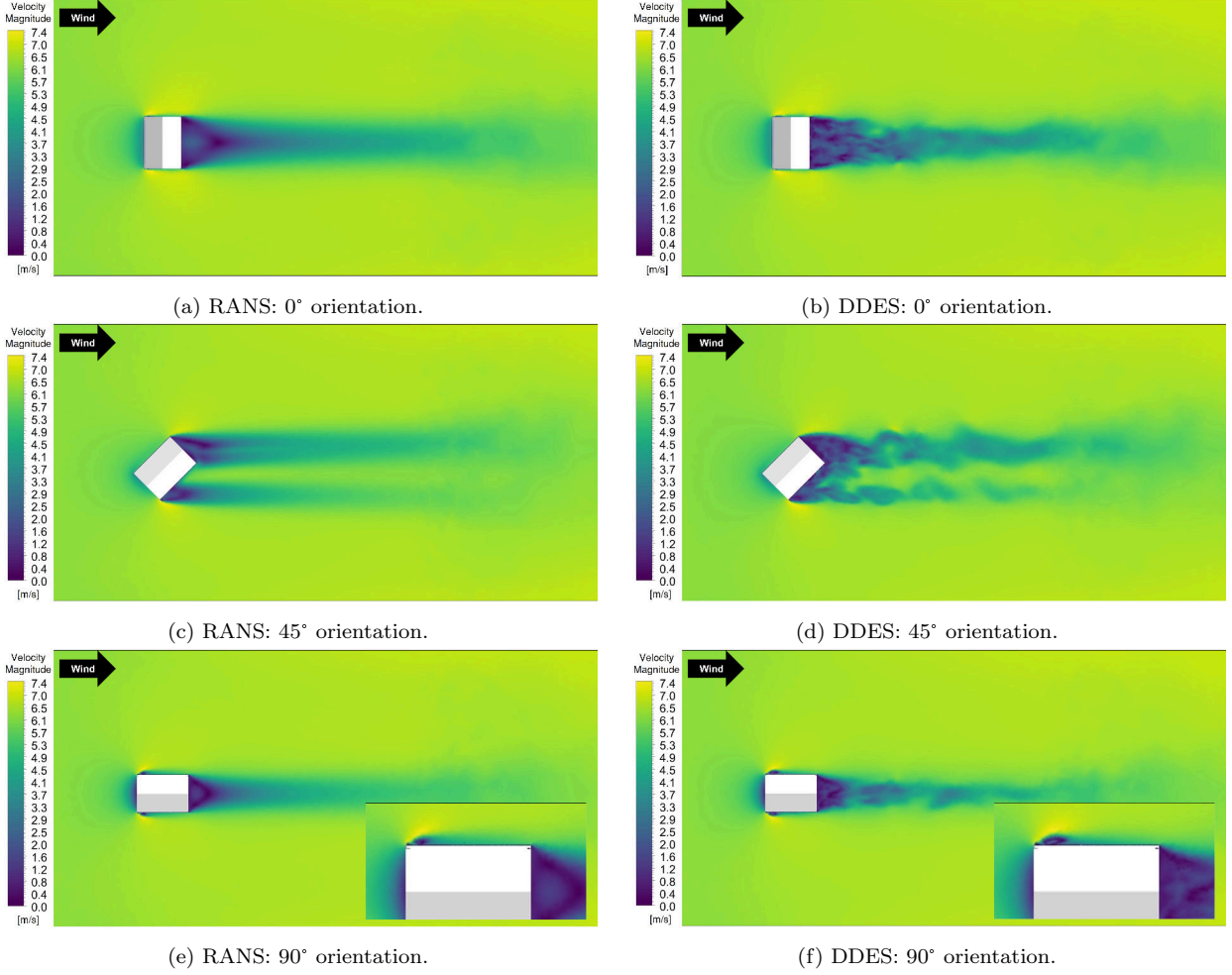


Figure 15: Comparative plan view of velocity magnitude contours at the eave height for RANS and DDES (instantaneous at $t = 10$ s).

Given knowledge of the wake structure, it is possible to visualize the influence it has on the roof C_p distribution as shown in Fig. 16 which exhibits the RANS, mean experimental, and mean DDES C_p distributions. In all cases, a greater negative C_p region is found leeward of the roof ridge, the distribution of which however differs between the alternate mechanisms of analysis. The experimental data predicts the greatest suction at the right-hand-side of the ridge, whereas the CFD findings indicate the greatest suction occurs closer to the center of the ridge. It is shown previously in Sec. 4.3 that the 45° orientation is critical for producing the greatest roof uplift force which can be hypothesized as being due to two aerodynamic features, i.e., the leeward separation regions and their morphology as the building rotates. This combination of aerodynamic analysis may therefore assist in explaining the critical

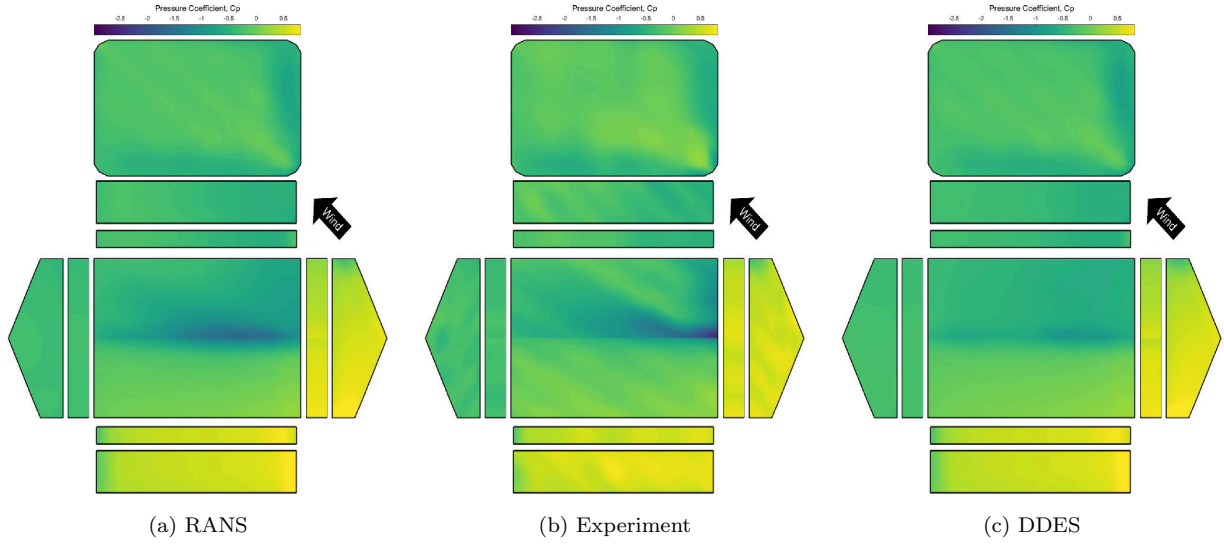


Figure 16: RANS, mean experimental, and mean DDES pressure coefficient (C_p) contours for the 45° building orientation.

suction region at the roof ridge as previously noted in field measurements by [Wang et al. \(2018\)](#).

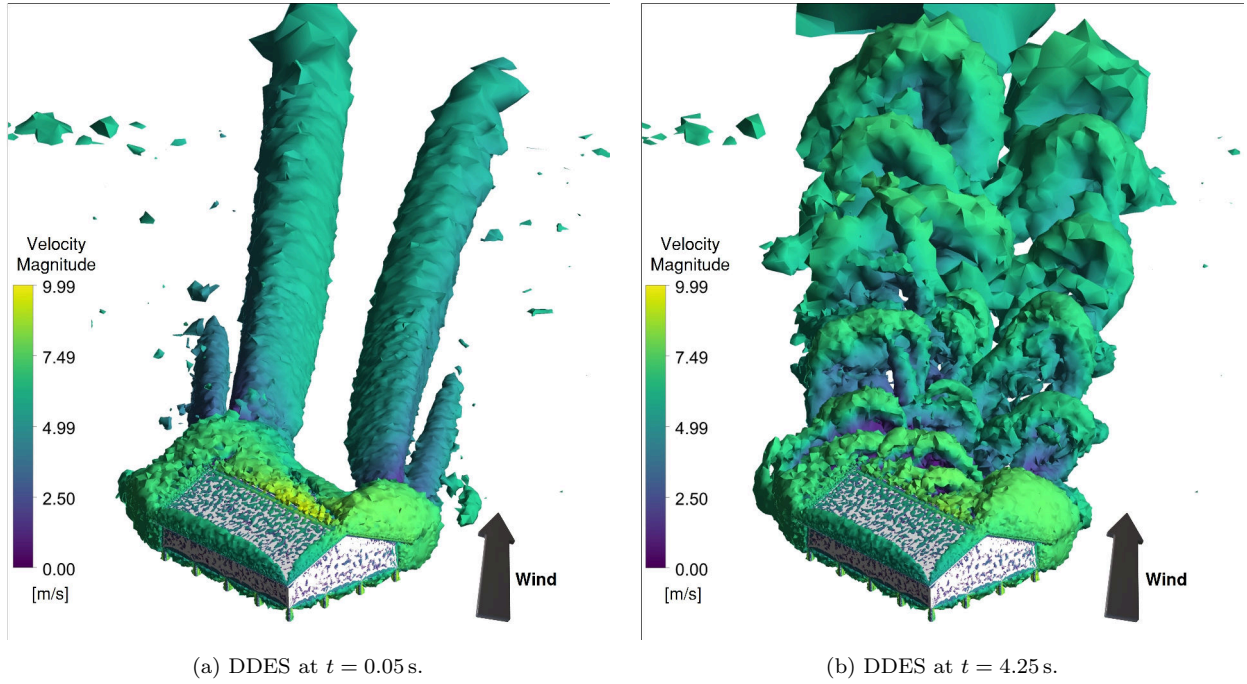


Figure 17: Contours of velocity magnitude and instantaneous Q-Criterion ($Q = 2 \text{ s}^{-2}$) for the 45° incident wind angle.

Fig. 17 shows the instantaneous Q-Criterion ($Q = 2 \text{ s}^{-2}$) for the DDES simulation at $t = 0.05 \text{ s}$ in Fig. 17a and $t = 4.25 \text{ s}$ in Fig. 17b, the former being a closer resemblance to the

initial RANS results that the DDES simulations are initialized from. At the early time, the vortical wake structure is consistent with that already reported in that two distinct wake regions extend leeward of the building. As the solution progresses, it can be seen that this structure disintegrates into a series of vortex tubes that encompass the entire leeward side of the building. Figs. 10h and 12a reported a large amount of roof uplift and indicated a greater upward suction, respectively, when RANS was employed. It can be seen through this analysis that at the earlier time, the two distinct wake patterns lead to high-velocity flow through their center, which in turn will lead to lower surface pressure on the roof. In contrast, DDES exhibits a much slower velocity in this region due to the formation of vortical structures across the leeward roof face which in turn causes a slower velocity and therefore permits a higher static pressure on the roof surface. It is likely that consideration of different roof pitch angles could mitigate these suction forces and warrants further investigation using a transient CFD methodology. Since it is clear that DDES simulations, when initialized from a RANS result, required a sufficient elapsed simulation time to reach a fully-developed state, practitioners should pay careful attention to and report the statistical convergence properties of the solution. This was shown in Fig. 10 where the time histories and running average of the house forces were reported.

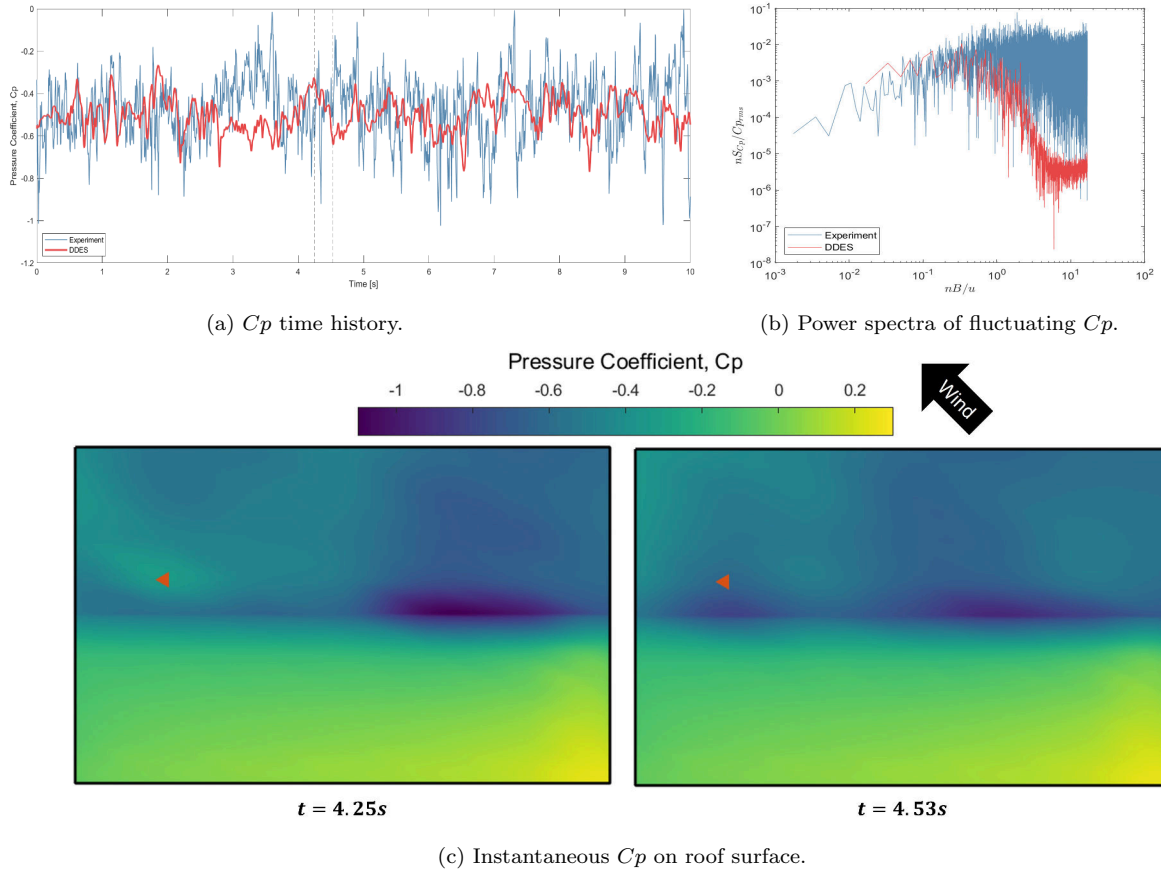


Figure 18: Isolated pressure tap analysis.

It is possible to use DDES to gain further information regarding the transient aerodynamic behavior and its influence on the surface pressure distribution. Since it is clear that vortical structures emanate leeward of the roof ridge, Fig. 18 studies an isolated pressure tap at a location where vortex passage is seen to occur, which is shown by the symbol within Fig. 18c. The C_p time histories are reported in Fig. 18a wherein the C_p is seen to fluctuate into high and low suction peaks which are attributed to the rolling-up of a shear layer present at the roof ridge (Holmes, 1994). Considering the instantaneous surface C_p values in Fig. 18c at times $t = 4.25$ s and $t = 5.3$ s to correspond with Fig. 17b, it can be seen that these peaks in C_p are reflected within the instantaneous C_p values and time history, which shows the DDES simulations are capturing vortex passage. Finally, the power spectra of the fluctuating C_p is noted within Fig. 18b at the selected pressure tap, where it can be seen that the DDES simulations are not as capable of resolving all details of the experimental data at small-scales but demonstrates more consistent resemblance in the energy-containing regions.

4.5. Peak (Minimum) Pressure Coefficients

Fig. 19 provides an overview of the building Center profile according to Fig. 8 for the averaged and minimum peak profiles subject to a 45° incident wind angle. The mean DDES and RANS C_p profiles are displayed in Fig. 19a wherein it can be seen that the mean DDES and RANS results are in relative agreement with the exception of the roof ridge, where the C_p distribution was previously shown to be heavily influenced by transient behavior. The CFD findings present improved relative agreement on the windward faces when compared to the experimental data, however differences in reported C_p values differ particularly on the leeward roof section and underside of the building, whereby CFD predicts greater suction. Minimum peak pressure coefficients determined from the experimental and DDES data are shown in Fig. 19b. Large differences are found at the windward side and roof section, with the DDES predicting much larger C_p values. Greater suction is also found to occur at the roof ridge and windward region of the building underside within the experimental data.

To better understand why the DDES simulations fail to realize peak pressure coefficients at the windward locations, Fig. 20 presents pressure tap time histories at windward and leeward locations across the aforementioned profiles. The precise locations with respect to the wind direction are shown within the figure insets. For the windward location in Fig. 20a, DDES and RANS simulations showcase similar values. The DDES time history remains relatively stable, which is in stark contrast to the experimental data which demonstrates clear transient behavior. This helps to explain why the DDES simulations failed to recover accurate minimum peak pressure coefficient values in windward regions within Fig. 19b. Instead, minimum peak and mean DDES pressure coefficients were similar. In leeward regions, as shown in Fig. 18 on the leeward roof panel, and on the leeward side in Fig. 20b, DDES reveals transient fluctuations in the pressure coefficient. Because the DDES simulations are able to predict aerodynamic features such as vortex-shedding, which was previously seen to occur in leeward regions, fluctuating C_p values are recovered more effectively. However, since DDES employs RANS modeling in the near-wall region, local pressure fluctuations are not fully resolved due to Reynolds averaging within the turbulence model. The consequences of this

are two-fold. Firstly, in windward regions where the incident ABL flow can be considered as steady, DDES presents no advantage over RANS. Secondly, in turbulent leeward regions, DDES captures larger-scale aerodynamic features and provides insight into how they modify the surface pressures over time; however, due to the RANS modeling in the near-wall region the pressure fluctuations exhibit a decreased amplitude when compared with experimental findings. In addition to the underlying modeling strategy, a curve-linear mesh is required to grow inflation layers out into the far-field at an acute angle due to the roof pitch and overhanging eave. This results in converging normal directions and thus skewed elements within the boundary layer close to the solid wall. As a result, the skewed elements introduce numerical diffusion which stabilizes the transient behavior (Thomas et al., 2011). While the same is true for the windward and leeward directions, the fact that the transient behavior is resolved suggests a stronger non-linear behavior in this area. It is possible to construct a different mesh in this area either through a Cartesian cut-cell or polyhedron-based grid; however, these approaches come with their own strengths and weaknesses. The curve-linear meshing approach taken in this study is a trade-off and could be investigated further.

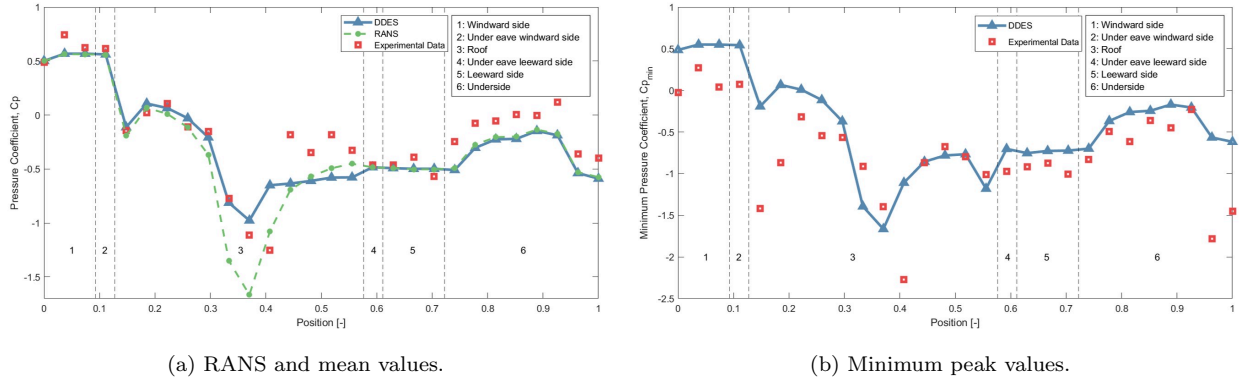


Figure 19: Mean and minimum peak pressure coefficient (C_p) Center profiles across for a 45° incident wind angle.

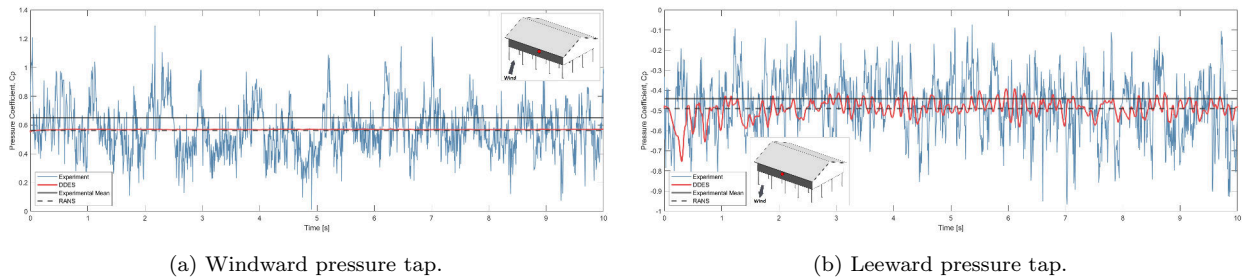


Figure 20: Pressure tap time histories for a 45° incident wind angle of a windward and leeward location.

5. Conclusions

Experimental and numerical studies using computational fluid dynamics (CFD) were adopted to assess the performance of an elevated coastal beachfront 1:25 scale house under applicable tropical cyclone conditions. Elevating coastal houses offers protection from flooding during tropical cyclones or storm surge, yet previous studies have indicated this may leave the house more vulnerable to wind-induced damage. Therefore, aerodynamic analysis is necessary to improve understanding of elevated house performance in order to develop damage mitigation strategies and assist the insurance market to incorporate climate-related risk within their models. The coastal house model was investigated under a range of different incident wind angles within a low-speed atmospheric boundary layer (ABL) wind tunnel at Wuhan University. Mean and peak pressure coefficient (C_p) distributions surrounding the elevated house were compared with results replicated through steady-state Reynolds-Averaged Navier–Stokes (RANS) and transient Delayed-Detached Eddy Simulation (DDES) turbulence modeling using the commercial CFD software ANSYS Fluent 2021 R1.

CFD validation and verification was performed by calculation of a Grid Convergence Index (GCI) and Richardson extrapolation analysis. The mean absolute error (MAE) was then used to quantify the agreement between experimental and numerical data. Generally, it was found that better agreement is achieved at lower incident wind angles. Wind angles of 45° and above proved more challenging, due to the more complex aerodynamics occurring at locations surrounding the building eave and roof ridge sections. The $k-\omega$ SST and Standard model were considered during this process, from which it was found that the Standard model demonstrated insufficient convergence properties for this problem and lower accuracy compared to the SST variant. It is recommended that if comparisons are made between turbulence models, then a full grid convergence study should be conducted for each model.

Mean C_p were considered at location under the eave (soffit) and above the eave to reveal how this feature influences overall suction on the roof. For incident wind angles of 30° (with the exception of RANS findings) and above, the soffit experiences a mean positive C_p meaning the overhanging eave was contributing to an overall upward force on the roof. The upward force resulting from the eave increased as the orientation angle increased from 0° to 45° and then stabilized for the remaining incident wind angles. Therefore, upward forces resulting from the overhanging eave are more prevalent when cornering winds are present. Net mean C_p values computed on the major roof panels and surrounding the eave revealed that an overall upward force is experienced by the roof. Suction on the roof panels was found to increase between 0° to 45° and then decrease thereafter for RANS simulations but remain relatively consistent when DDES simulations were performed. This is attributed to the unsteady behavior exhibited through the force time history which indicates RANS is less-accurate under such circumstances due to its time-averaged numerical formulation. Future work may consider how different lengths of overhang influence these forces and the possibility of a fully-hipped overhanging roof.

The remaining critical suction region at the roof ridge was studied through further leveraging transient simulations. RANS observations revealed separation bubbles leeward of the

building and regions of heightened turbulent kinetic energy (TKE) leeward of the roof ridge. DDES, on the other hand, indicated that for quartering winds, the separation bubbles became less well-defined and TKE was dissipated with greater effect. This finding may have important implications in designing community layouts since steady-state simulations are unable to fully realize wake regions with high-fidelity. In addition, the vortex-shedding found to occur during DDES across the leeward roof panels during quartering winds acted to reduce the air-flow which resulted in less suction. The vortex-shedding was analyzed in accordance with a pressure tap time history and illustrated how the transient formulation of DDES was able to realize the resulting fluctuating pressure due to vortex passage. However, while DDES was better able to reproduce mean pressure forces when compared to experiment, it fell short in its reproduction of minimum peak C_p values, particularly in windward regions. This was attributed to both the underlying Reynolds averaging occurring in the near-wall region and a possible grid-induced stabilizing effect. Therefore, Large-Eddy Simulations (LES) may be preferred by future practitioners concerned with peak C_p predictions whereas DDES offers a promising alternative for studies on a larger scale where computational costs associated with LES may prove infeasible and general unsteady aerodynamic tendencies are of primary concern.

In general, CFD simulations found difficulty in simulation peak pressure values, particularly at the eave corners. In this article, DDES is advocated for future transient analysis due to its ability in resolving more complex aerodynamic features, however particular attention should be paid to improving simulations surrounding these important geometrical features. Overall, the influence of overhanging eave and roof pitch warrant further investigations and any future studies of elevated coastal houses should take these important geometrical features into account since they are typical of beachfront houses at locations such as Dauphin Island, Alabama, USA.

CRedit Author Statement

Jamie F. Townsend: Investigation (CFD), Writing - Original Draft. **Tom-Robin Teschner:** Investigation (CFD), Writing - Review & Editing. **Guoji Xu:** Supervision, Funding acquisition, Writing - Review & Editing. **Lianghao Zou:** Investigation (Experiment). **Yan Han:** Resources. **C.S. Cai:** Writing - Review & Editing.

Conflict of interest statement

The authors declare there is no conflict of interest.

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