CFD Simulation of a Floating Offshore Wind Turbine System Using a Quasi-static Crowfoot Mooring-Line Model

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A general quasi-static crowfoot mooring-line model is developed and applied to the 5 MW floating offshore wind turbine (FOWT) conceptualized by the National Renewable Energy Laboratory (NREL) in the Offshore Code Comparison Collaboration (OC3). This model is implemented into the CFD solver, CFDShip-Iowa V4.5. The model is first validated against experimental data for free-decay tests. A full-system simulation with wave and wind excitation is performed by utilizing the crowfoot model. The present study predicts less overall platform motion. Predicted power is shown to rely heavily on system pitching and surging motions. The system simulation shows that the crowfoot model eliminates the need for a geometric line approximation.

INTRODUCTION

Wind power is being embraced as a clean, potent, and economical power resource in the U.S. At the beginning of 2012, wind power provided 46.9 GW, or 2.9% of the total U.S. electricity supply (IEA, 2011). In 2008, the U.S. Department of Energy (DOE) issued a report, in which a plan is presented to produce 20% of America’s electricity demands from wind power by 2030 (DOE, 2008). This plan dictates that 18% of this wind power (54 GW) would be offshore power. The National Renewable Energy Laboratory (NREL) has stated that 4,150 GW of capacity is available within 50 nautical miles from shore (Schwartz et al., 2010), approximately 4 times the current U.S. usage. Almost 60% of this estimate comes from water with depths greater than 60 m, at which point it becomes economically infeasible to use a structure fixed to the seafloor, so floating structures are required (Musial and Ram, 2010). Multiple floating platform designs have been identified by the research community. While the present study focuses solely on the spar-buoy platform, a review of current floating platform strategies is available in Vire (2012). A thorough analysis of a tension leg platform compared to other floating platforms is presented in Matha (2010).

Offshore wind power provides many advantages over land-based wind turbines, including large continuous areas suitable for farm deployment, higher and steadier wind velocities, and less wind turbulence (Musial and Ram, 2010). However, designing offshore wind technology also offers significant challenges. This is especially true for the FOWT, where mooring system dynamics are introduced on top of the already complex coupling of aerodynamic and oceanic-hydrodynamic effects. Accurate simulation of offshore wind technology is a substantial task, particularly with very limited large-scale experimental data. With this in mind, ten countries participated in the International Energy Agency (IEA)-approved Task 23 Subtask 2: The OC3. The OC3 performed simulations of four offshore wind turbines using various software packages, then compared results with the intent of facilitating improvements in models or analysis methodologies.

Only one FOWT was investigated by the OC3: a spar-buoy concept, where the platform is modeled as a simple cylinder with a large ballast held in place with catenary mooring lines, referred to as the OC3-Hywind. This system was chosen for study in the OC3’s Phase IV due to its simplicity and potential for validation against a smaller full-scale prototype already in service (Jonkman, 2010; Statoil, 2012). The OC3-Hywind’s mooring system consists of three crowfoot catenary lines shown in Fig. 1. This crowfoot connection increases the yaw stiffness of the overall mooring system (Nielsen et al., 2006). To simplify the model for their Phase IV studies, the OC3 averaged line properties for the 2-point approximation and neglected all damping effects, including drag and seabed friction. The OC3 also eliminated the crowfoot connection and instead added a constant yaw-spring stiffness to compensate. Unfortunately, the magnitude of this stiffness depends on the geometry and line properties and will likely be different under various environmental conditions. The mooring system of FOWT platforms is important to the dynamic behavior of the FOWT and may significantly affect the stability of the tower. Appropriate modeling of this system is critical during the overall system design (Matha et al., 2011; Brommundt et al., 2012). An excellent background on the analysis of mooring systems for floating structures is presented by Chakrabarti (2005). In order to examine variations and optimize a crowfoot mooring connection for the NREL 5 MW offshore...
spar-buoy turbine (the OC3-Hywind model), as well as for other slender spar-buoy designs, a more general model, which considers the genuine crowfoot geometry and eliminates the need for an added yaw stiffness approximation, is needed.

MATHEMATICAL MODELING AND METHODS

The OC3-Hywind is presented in Fig. 1 in the initial position of the system. The earth-fixed frame \((X, Y, Z)\) is located at the still water line (SWL), and a moving tower frame \((x, y, z)\) is located at the center of mass of the overall system. In the initial position, the tower frame is identically oriented to the earth frame and is located at \([-0.18 \text{ m}, 0, -0.78 \text{ m}]\) in earth frame coordinates. The position of this frame in earth coordinates provides the surge, sway, and heave \((X, Y, Z, \text{respectively})\) of the system, and its orientation is used to determine the roll, pitch, and yaw (the rotation about \(x, y, z\), respectively).

All simulations are solved by using the general purpose unsteady Reynolds-Averaged Navier-Stokes (URANS) finite-difference solver, CFDShip-Iowa V4.5 (Huang et al., 2008), which features a two-phase solution module for simulations where both water and air resistances must be considered. The free surface is modeled with a level set method (Carrica et al., 2007a), enforcing kinematic and dynamic free-surface boundary conditions on the interface. The free-surface interface of two-phase simulations is subject only to the conditions of the denser liquid phase, and the air is then constrained by the calculated free surface, which is viewed as a moving immersed boundary. A projection algorithm (Bell et al., 1991) is utilized to enforce mass conservation, in which the pressure Poisson equation is solved by using the PETSc toolbox (Balay et al., 2010). The CFD solver utilizes overset grids (Carrica et al., 2007b) to solve across multiple grids moving relative to one another. The overset code SUGGAR (Noack, 2005) runs parallel to the CFD code and provides run-time overset connectivity, moving the grids based on solved forces and moments and reassembling them every time step. SUGGAR then determines which points to keep and which points to cut from the resultant grid and provides this grid to the CFD code. The code also solves for all six rigid-body degree-of-freedom (DOF) motions, discussed in detail and validated by Xing et al. (2008). The single-phase solver of CFDShip-Iowa V4.5 has been validated against an array of steady and unsteady ship hydrodynamics problems with various free-surface conditions (Xing et al., 2012). It has also been aerodynamically validated by Li et al. (2012) against the NREL’s onshore unsteady Phase IV turbine experiments (NREL, 2001).

Fluid Modeling

CFDShip-Iowa V4.5 utilizes dimensionless inputs for all variables. Air velocities are assumed to remain within the low-velocity, incompressible regime, such that the continuity and momentum conservation equations for both air and water are:

\[
\nabla \cdot \mathbf{u} = 0
\]

\[
\frac{\partial \mathbf{u}}{\partial t} + \mathbf{u} \cdot \nabla \mathbf{u} = -\nabla p + \nabla \cdot \left[ \frac{1}{Re_{\text{eff}}} (\nabla \mathbf{u} + \nabla \mathbf{u}^T) \right]
\]

where \(\mathbf{u}\) is the fluid velocity, the pressure \(p\) is the piezometric pressure, and \(Re_{\text{eff}}\) is the effective Reynolds number, which are defined as:

\[
\hat{p} = \frac{p}{\rho U_0^2} + \gamma Z
\]

\[
Re_{\text{eff}} = \frac{U_0 L_0}{v + v_t}
\]

where \(p\) is the pressure, \(\rho\) is the fluid density, \(\gamma\) is the specific gravity of the fluid, \(Z\) is the depth below the surface, \(U_0\) is the free-stream velocity, \(L_0\) is the characteristic length (chosen to be the length of the blade for this study), and \(v_t\) is the eddy viscosity.

All simulations utilize Menter’s blended \(k-\omega/k-\epsilon\) two-equation shear stress transport (SST) model (Menter, 1994) with a two-point multi-layer wall function (Bhushan et al., 2009). This model smoothly switches between the \(k-\omega\) model in the inner boundary layer and the \(k-\epsilon\) model in the outer boundary layer (and beyond the boundary layer), helping to take advantage of both.

OC3-Hywind Mooring Models

The OC3-Hywind simulations were moored by using three catenary lines offset at 120° (see Fig. 1). The simulated lines attach to the platform by using the crowfoot connection shown in Fig. 1 (main) and Fig. 1b. The simplified 2-point lines utilized by the OC3 are shown in Fig. 1a.

Three mooring-line models have been implemented in the CFD code for the purpose of studying the OC3-Hywind. The first is a quasi-static, 2-point, single-line catenary model that solves for fairlead and anchor forces. The second is also a single-line model, but supplements the yaw-axis rotational stiffness of the turbine with the aforementioned yaw-spring stiffness provided to approximate the yaw restoration forces of the physically genuine crowfoot system. The third is a crowfoot-connection model that solves for a force balance between three separate catenary lines connected at a common position, then produces fairlead forces. In all three models, the drag and dynamic effects of the mooring-line motion have been neglected, and the effective mass of the lines are integrated into the calculated fairlead forces.

2-point, Single-line, 2D Model. Jonkman (2007) derived a 2-dimensional, 2-point, single-line catenary mooring-line model that solves for the effective horizontal and vertical components, \(H_F\) and \(V_F\), respectively, of the line tension at the fairlead. It is presented as a nonlinear system of two equations in two unknowns, \(H_F\) and \(V_F\), representing the horizontal (\(H_F\)) and vertical (\(V_F\)) forces at the fairlead in a local two-dimensional coordinate system. This model allows for three catenary configurations: a portion of the line sitting on the seabed, no portion of the line sitting on the seabed, and no seabed at all (or a “floating anchor”), as shown in Fig. 2. The third situation solution is crucial for the crowfoot configuration, as will be shown. This model accounts for the weight of the line in
where flow, elastic stretching, and seabed friction. Bending stiffness and dynamic effects are neglected. In the case where a portion of the mooring line sits on the seabed (neglecting frictional interaction between the line and the seabed), the system of two equations in two unknowns and the corresponding fairlead and anchor forces are:

\[
x_F = L_B + \frac{H_F}{\omega} \sinh^{-1} \left( \frac{V_F}{H_F} \right) + H_Y L \frac{1}{E_A}
\]

\[
z_F = \frac{H_F}{\omega} \left[ 1 + \left( \frac{V_F}{H_F} \right)^2 \right] - \sqrt{1 + \left( \frac{V_F - \omega L}{H_F} \right)^2} + V_F L - \omega L^2 / 2 \frac{E_A}{H_A} = H_F
\]

\[
V_A = 0
\]

where \(L_B\) is the (unstretched) length of the line resting on the seabed, \(L\) is the total (unstretched) length of the line, \(\omega\) is the apparent weight of the line in seawater per unit length, and \(E_A\) is the extensional stiffness of the line.

For implementation into the CFD code, the earth-fixed frame anchor and fairlead positions are translated into a local coordinate frame, as shown in Fig. 2a. These translated positions, along with line properties, are input into the catenary solution module, and the resulting fairlead forces are calculated. These forces are then translated into the moving tower coordinate frame, where the CFD code calculates the system’s 6-DOF motions about its center of mass. This process is repeated for each simulation time step.

**OC3 Added Yaw Stiffness Model.** The added yaw stiffness (AYS) model is identical to the 2-point model, except that the aforementioned supplemental yaw stiffness utilized by the OC3 is added in. All force and moment calculations in CFDShip-Iowa are calculated in the moving tower coordinate system, making this a simple linear addition to the yaw moment calculation:

\[
M'_F = M_F + \psi \left( 98,340,000 \text{ Nm} \right) \text{rad}
\]

where \(M_F\) is the moment about the \(z\)-axis in the local tower frame, \(\psi\) is the tower yaw in radians, and \(M'_F\) is the augmented moment. This process is also repeated for each simulation time step.

![Fig. 2 Two-dimensional catenary forces and line configurations (reproduced from Jonkman (2007)): (a) line resting on seabed; (b) taut line; (c) slack line](image)

![Fig. 3 Crowfoot connection to fairleads with descriptions](image)

**Crowfoot Model.** The crowfoot model is composed of three catenary lines connected at a single junction point (Point J) shown in Fig. 3. The anchor of line JA is the physical anchor of the individual crowfoot structure, and its fairlead is defined as point J. The other two lines of the crowfoot model, lines JB and JC, have their anchors defined as point J and their fairlead locations defined as B and C, respectively. Accurate prediction of the location of point J is crucial to the calculation of the fairlead forces at B and C, as small displacements of this point can result in large force differences at the fairleads. To calculate the position of J, all three lines are translated into their respective local coordinate frames. It is assumed that all lines have a positive \(z_F\) (i.e., the anchors never pass above the fairleads):

\[
x_{FA} = \sqrt{\left( J_X - A_X \right)^2 + \left( J_Y - A_Y \right)^2}
\]

\[
z_{FA} = J_Z - A_Z
\]

\[
x_{FB} = \sqrt{\left( B_X - J_X \right)^2 + \left( B_Y - J_Y \right)^2}
\]

\[
z_{FB} = B_Z - J_Z
\]

\[
x_{FC} = \sqrt{\left( C_X - J_X \right)^2 + \left( C_Y - J_Y \right)^2}
\]

\[
z_{FC} = C_Z - J_Z
\]

where \(J_{X,Y,Z}\) is the location of J in the earth-fixed frame (similar for the anchor point, A, and fairleads B and C). Forces are then calculated in the lines’ local coordinate frames by using the catenary solution module (see the Force Solution Block in Fig. 5). For line JA, the coefficient of seabed friction can be set to any positive value for the case of modeled static friction between the line and the seabed, or the seabed friction may be neglected altogether. Lines JB and JC, however, have no seabed at all. Here the importance of the aforementioned “floating anchor” scenario comes in. The fact that lines JB and JC have no seabed, yet have a fixed anchor point, allows one of the lines (JB or JC) to go to a slackened position and the other line to assume the bulk of the line tension, which effectively shifts the yaw-angle-correcting moment arm towards the side that requires it. This shifting of the moment arm from B to C, or vice versa, is the main strength of the crowfoot connection concept. These forces are then summed in a three-dimensional force balance at point J. The vertical forces translate directly, but the horizontal forces must be split into X and Y components. The mooring lines are represented by geometrically linear bodies in the \(XY\) plane.
such that the angles, which each of the three individual crowfoot component lines form with the X-axis, can be calculated by their respective endpoints. See Fig. 4 for a description of these angles. Equations 16 and 17 produce the cosine and sine of $\alpha$ (likewise for $\beta$ and $\gamma$):

\[
\cos \alpha = \frac{J_X - A_X}{\sqrt{(J_X - A_X)^2 + (J_Y - A_Y)^2}} \tag{16}
\]

\[
\sin \alpha = \frac{J_Y - A_Y}{\sqrt{(J_X - A_X)^2 + (J_Y - A_Y)^2}} \tag{17}
\]

Through the use of these angles, the lines’ horizontal forces are separated into $X$ and $Y$ components in the earth-fixed frame. The force balance system of equations in fixed $X$, $Y$, and $Z$ components is set up at point $J$. The locations of $A$, $B$, and $C$ are fixed at any individual time step, such that the force balance becomes a system of three equations in three unknowns:

\[
f_1(J_X, J_Y, J_Z) = H_{F_a} \cos \alpha + H_{h_b} \cos \beta + H_{h_c} \cos \gamma \tag{18}
\]

\[
f_2(J_X, J_Y, J_Z) = H_{F_a} \sin \alpha + H_{h_b} \sin \beta + H_{h_c} \sin \gamma \tag{19}
\]

\[
f_3(J_X, J_Y, J_Z) = -V_{a} + V_{b} + V_{c} \tag{20}
\]

where $f_1, f_2,$ and $f_3$ are the net forces in the $X$, $Y$, and $Z$ dimensions, respectively. Equations 18, 19, and 20 are iterated to solve for the final position of point $J$ by using Broyden’s method outlined in Fig. 5 (Broyden, 1965). The resultant fairlead forces are transferred back to the CFD code for 6-DOF motion calculations (see Fig. 6).

Broyden’s method works by directly solving the Jacobian matrix of a system of equations. This direct solution of the Jacobian matrix eliminates the need to take analytical derivatives of Eqs. 18, 19, and 20. The initial Jacobian matrix, $\Omega_0$, is only an estimate of the final one. Therefore, a finite-difference scheme will provide sufficiently accurate results. The Jacobian matrix is defined as:

\[
\Omega^n_{j,i} = \left( \frac{\partial f_j}{\partial J^i} \right) \tag{21}
\]

Each differential term is differed by using a first-order forward scheme using $\Delta J^i = 0.0001$ m, chosen for its numeric stability given the desired tolerance of the force balance at point $J$ and the computational cost:

\[
\frac{\partial f_j}{\partial J^i} = \frac{f_{j+\Delta J^i} - f_j}{\Delta J^i} \tag{22}
\]

The earth-system coordinates of point $J^n$ are first assumed by using its position at the previous time step, $J^{n-1}$. Here $n$ represents the crowfoot solution iteration index and $t$ represents the time step. For the first time step, $J^0$ is assumed to be the location $R\%$ along a single line, where $R$ is defined as the ratio of the lengths of lines JB and the total of JB and JA. Equations 18, 19, and 20 are repeatedly evaluated by using $J^0$ and the $\Delta J$ coordinates, per Eq. 22, to assemble the initial Jacobian matrix. The iterative loop begins after the calculation of the initial Jacobian matrix. Equations 18, 19, and 20 are evaluated to determine the $X, Y, Z$-component values of the force balance at point $J^n$. This vector of net-force values, $f(J^n)$, is then used to calculate a correction vector, $\delta J^n$:

\[
\delta J^n = - (\Omega^n)^{-1} f(J^n) \tag{23}
\]

Equation 23 requires taking the inverse of the Jacobian matrix. The formulae for inverting a $3 \times 3$ matrix are readily available, but are not included for brevity. The correction vector is then summed with the current point $J^0$ to produce an updated junction point position, $J^{n+1}$:

\[
J^{n+1} = J^n + \delta J^n \tag{24}
\]

Equations 18, 19, and 20 are again evaluated by using the newly calculated $J^{n+1}$ coordinates to produce an updated net-force vector, $f(J^{n+1})$. This new net-force vector is then used to check for convergence. Multiple useful convergence criteria exist for this scenario, but ultimately, as the desired result of the net-force vector is $f(J) = 0$, the maximum absolute value of $f(J^{n+1})$ is checked against a predefined input tolerance:

\[
|f(J^{n+1})| < \text{tolerance} \tag{25}
\]

If Eq. 25 returns false, then the solution has not converged within tolerance and requires more iterations. The coordinates of point $J$ and the Jacobian matrix are updated as:

\[
J^n = J^{n+1} \quad \Omega^n = \Omega^{n+1} \tag{26}
\]

and the process is repeated. If Eq. 25 returns true, then the solution has converged, and the values calculated as $J^{n+1}$ are returned to CFDShip-Iowa as the determined position of point $J$. 

Fig. 4 Crowfoot XY plane angles

Fig. 5 Broyden’s method solution process
Both the momentum and level set convection terms are discretized by using a second-order upwind finite-difference scheme. The time discretization utilizes a second-order backward difference scheme. The overall solution strategy is shown in Fig. 6. For each time step, the grids are first moved according to the motions from the 6-DOF predictor or corrector steps from the previous time step (see Eqs. 12 and 13 in Wilson et al., 2006). The level set function is transported and reinitialized to determine the new location of the free surface. Following this, the pressure and velocity fields are then solved by using the projection algorithm. Hydro- and aerodynamic forces and moments are solved by integrating the pressure along the solid boundaries of the turbine. These forces and moments are summed with forces and moments calculated in the mooring system module. Then the nonlinear iteration residuals are evaluated. If all residuals drop below $10^{-3}$, then the time step is converged; 6-DOF motions are predicted, and SUGGAR is called to compute the overset interpolation of the new locations of all grids. If the nonlinear iteration has not converged, the 6-DOF motions are corrected, SUGGAR is called, and a new nonlinear iteration begins.

**SIMULATION CONDITIONS AND DESIGN**

The OC3-Hywind model utilizes a ballasted floating spar-buoy platform upon which is mounted the NREL’s offshore 5MW baseline wind turbine (Jonkman et al., 2009). The model uses a three-bladed rotor with blades offset 120° from one another, located (statically) 90 m above the water surface. The draft of the platform is 120 m, and its center of gravity without a mooring system is located 89.9 m below the water surface. The seabed depth is 320 m, and the mooring fairleads are located 70 m below the SWL, giving an initial, static $z_F$ of 250 m. For the 2-point and AYS models, the anchors are placed 853.87 m radially from the tower’s vertical $z$-axis. This, combined with a 5.2-m fairlead radius, gives an initial $x_F$ of 848.67 m. The crowfoot model splits the single line and connects to three common fairleads offset 60° from the radial line from the tower axis to the anchor, as shown in Fig. 1b. Specific geometry of the original crowfoot lines, around which the OC3 made approximations, was unattainable. Based on private email conversations with an industry expert from Statoil of Norway, the lengths of lines JB and JC for each crowfoot structure were estimated to be 10% of the length of the 2-point lines developed in Jonkman (2010). The catenary solution routine requires the line weight in water, the unstretched length of the line, and the extensional stiffness of the line. Details about line properties used for each model tested are shown in Table 1.

CFDShip-Iowa features a static initialization mode that computes the mass and static wetted area of the tower based on the initial grid placement and external forces. Both the AYS and crowfoot models are assumed to have the same initial position and experience, and, therefore, the same buoyant force. However, the two mooring-line models, viewed by the FOWT as external forces, do not produce the same initial fairlead forces. The crowfoot lines have approximately 90 m more line per mooring structure than the AYS lines, yet initialize at the same location, increasing fairlead tension. This added line weight is then absorbed into that of the tower, such that the mass of the crowfoot tower is slightly less than that of the AYS tower, as shown in Table 1. Both model masses closely match the OC3’s mass of 8.066E6 kg.

Cases 1.4 and 5.1 of the OC3’s Phase IV are simulated. Case 1.4 consists of six free-decay time-series tests, where each of the six rigid-body DOF is individually perturbed and the system is allowed to decay to a static position. Case 5.1 introduces excitation from regular airy waves and a steady wind. The rotor rotation is fixed at 9.4 rpm, which is the average rotor speed calculated by the NREL in their OC3 results. See Table 2 for a summary of cases.

<table>
<thead>
<tr>
<th>Case</th>
<th>Wind conditions</th>
<th>Wave conditions</th>
<th>Mooring model</th>
<th>Rotor RPM</th>
<th>Analysis</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.4b</td>
<td>Air density = 0</td>
<td>Still water</td>
<td>AYS</td>
<td>N/A</td>
<td>Free-decay time-series (each DOF individually)</td>
</tr>
<tr>
<td>1.4c</td>
<td>Air density = 0</td>
<td>Still water</td>
<td>Crowfoot</td>
<td>N/A</td>
<td>Free-decay time-series (each DOF individually)</td>
</tr>
<tr>
<td>5.1</td>
<td>Steady wind</td>
<td>Regular:</td>
<td>Crowfoot</td>
<td>9.4</td>
<td>Time-series</td>
</tr>
<tr>
<td></td>
<td>8 m/s</td>
<td>$H = 6$ m; $T = 10$ s</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**Table 2 Simulation matrix**

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**Numerical Methods and Solution Strategy**

Both the momentum and level set convection terms are discretized by using a second-order upwind finite-difference scheme. The time discretization utilizes a second-order backward difference scheme. The overall solution strategy is shown in Fig. 6. For each time step, the grids are first moved according to the motions from the 6-DOF predictor or corrector steps from the previous time step (see Eqs. 12 and 13 in Wilson et al., 2006). The level set function is transported and reinitialized to determine the new location of the free surface. Following this, the pressure and velocity fields are then solved by using the projection algorithm. Hydro- and aerodynamic forces and moments are solved by integrating the pressure along the solid boundaries of the turbine. These forces and moments are summed with forces and moments calculated in the mooring system module. Then the nonlinear iteration residuals are evaluated. If all residuals drop below $10^{-3}$, then the time step is converged; 6-DOF motions are predicted, and SUGGAR is called to compute the overset interpolation of the new locations of all grids. If the nonlinear iteration has not converged, the 6-DOF motions are corrected, SUGGAR is called, and a new nonlinear iteration begins.

**Fig. 6 Solution strategy**
Grid Topology

The grid used is shown in Fig. 7. The same topology is used for Cases 1.4 and 5.1 to eliminate the time spent preprocessing the overset grid scheme. The overall system has 14 different grids. These grids are split into individual blocks, with the points distributed relatively evenly per block. Each individual block is then assigned to an individual processor for calculations. The nacelle, tower, hub, blades, and blade tips are all meshed according to their geometry, as defined in Jonkman et al. (2009) and Jonkman (2010). Refinement blocks are added around each blade, and another refinement block (“Rotor”) is added around the entire span of the rotor, extending slightly upstream of the rotor and a short way into the wake region downstream. All of these blocks are then contained in the overall background block, which defines the absolute extent of the simulated volume. Overall, 5.75 million points are solved at each time step, although approximately 400,000 of these are repeated points between block splits.

The tower and nacelle grids are fixed together as a rigid group, and this group is allowed all 6 DOF inside the background grid, which is fixed in the earth coordinate system (see Fig. 7). The blades, blade tips, hub, and blade refinement grids are fixed as a second rigid group, which is fixed to the tower but allowed to rotate about the hub axis. The spatial resolution of grid points normal to solid surfaces is set to stay within the specifications of the chosen turbulence model. This spacing is set to approximately 0.000063 m at all surfaces. A small time step (0.0087 s corresponding to 1/2 of rotor rotation) is used for all load cases to ensure capturing of oscillatory wake activity.

RESULTS AND DISCUSSION

The results of the cases detailed in Table 2 are presented. The results of case 1.4 are compared to experimental data provided to the NREL by Statoil of Norway (Jonkman et al., 2010). Case 5.1 results are presented against the predictions of the NREL using FAST.

Case 1.4 Tests

Six individual DOF decay tests are performed using both the AYS and crowfoot lines. The tower is perturbed a prescribed amount (per Jonkman, 2010) along the DOF of interest, then released to move freely from that position. The results of four of the six simulations from cases 1.4b and 1.4c are presented in Fig. 8, along with experimental results from Phase IV of the OC3 (Jonkman et al., 2010). The decay tests show good amplitude agreement with those of the experimental results and, important to the validity of the proposed crowfoot model, they show amplitude agreement with one another. The crowfoot model (0.00939 Hz) shows a 16.7% increase, and the AYS model (0.00866 Hz) shows a 7.6% increase in surge natural frequency when compared to the experimental results (0.00805 Hz). One source of this difference is numerical error derived from the spatial and temporal discretization utilized in the present study. Modeling error is also present in both models, as both are assumed to be solid wire lines with no dynamic effects. The neglected dynamic effects of the lines will be most pronounced in surge. The AYS model is a geometric approximation of the experimental lines, and the crowfoot model in the present study utilizes the line properties used by the OC3, as the specifications of the experimental lines were not made available. This suggests that the surge restoration forces provided by the two models might not match those of the experimental lines. The crowfoot model shows a 10% higher frequency in surge free-decay than the AYS. This frequency difference is likely due to the increased surge restoration forces provided by the crowfoot model, as shown in Fig. 9. Both models show strong amplitude and frequency agreement with the experimental results in both heave and pitch tests. The crowfoot model shows a slight (−1.6%) difference in frequency (0.119 Hz) compared to the experimental data (0.121 Hz) in yaw, as well as a 5% decrease in amplitude, while the AYS model agrees well in both amplitude and frequency. Tower mass differences between the two models and the difference in yaw restoration between them (shown in Fig. 9) account for these differences. Both models agree well with the experimental results.

Case 5.1 Tests

Case 5.1 introduces regular sea wave motions and steady wind excitation. The tower is initialized at its static position, and wind and waves are introduced with parameters shown in Table 2. The time-series results of the platform surge, heave, pitch, and yaw in case 5.1 are shown in Fig. 10, along with the NREL-FAST results. The transient start-up period has been removed from the results and two 10-s wave periods are shown. The plots have been arranged such that the wave height at the platform centerline is at a maximum at 0 s. In case 5.1, the present CFD results predict a mean surge of...
12.87 m downstream, which is 5.5% less than the 13.62 m predicted by NREL-FAST. The dynamic range of the platform surge (2.23 m) is 75% of that predicted by NREL-FAST. This difference in surge is likely due to NREL-FAST’s usage of a constant drag coefficient in Morison’s equation. This does not account for the increase in drag at lower Reynolds numbers or for any turbulent behavior associated with oscillation. The current predictions show a −0.16-m mean heave, 26% closer to the SWL, as well as a slight phase lag when compared to the NREL-FAST results. The augmented viscous-drag term from Morison’s equation considers only flow perpendicular to the central axis of the platform and does not account for skin friction in heave. The present CFD results completely resolve the viscous boundary layer at the platform surface, and hydrodynamic (not just buoyant) heave forces are solved for. The captured viscous effects in heave, as well as different mooring restoration characteristics between models, are the likely reasons for both the phase and mean differences. A mean pitch of 2.83° is calculated in the current study, which agrees well with the NREL-FAST results. A 25% decrease in pitch dynamic range is predicted, as the current study includes the air drag forces experienced by the tower, hub, and nacelle—forces that are neglected in the NREL’s results. The current results predict the same mean yaw (−0.029°), but 62.3% less overall yaw than that of the NREL-FAST results, although both predict well under 1° of dynamic range. This is due to the crowfoot model providing more yaw restoration at very low yaw, although this is difficult to discern in Fig. 9. In pure yaw in Fig. 9, the crowfoot model provides more yaw restoration at yaw angles less than ±1.37°. The current results also predict a very different energy density distribution with a secondary frequency in the signal. As shown in Fig. 11, the NREL-FAST results show yaw excitation only at the wave frequency (0.1 Hz), whereas the current study shows the energy content to be distributed between the wave frequency and the rotor rotation frequency (0.157 Hz), implying that the wind has a significant effect on system yaw.

The aerodynamic power and thrust time-histories for load case 5.1 in the present study are shown in Fig. 12, along with the aerodynamic power generation predictions of the NREL-FAST. In the current study, the power is calculated by integrating both pressure and friction effects on the surface of the blade and multiplying by the constant angular velocity. The CFD results using the crowfoot model predict 1.76 MW in mean power delivered to the shaft, which is 15% lower than the NREL-FAST prediction of 2.07 MW. However, a 361% increase in amplitude is predicted in the present study. The surge velocity in the current study is also included in Fig. 12. This curve has been included for timing reference only and, as such, its axis has been inverted and left off the plot for clarity. The pitch velocity is almost perfectly in phase with the surge velocity (see the motions in Fig. 10) and is left off the plot. Note that the thrust and power are seen to be in phase with these velocities, which are, in turn, 180° phase-shifted from the wave height. This suggests that the maximum power is developed at the wave trough, and the minimum power is developed at the wave crest. This disagrees with the predictions of the NREL-FAST, where the maximum power is found 90° in front of the wave crest and the minimum power is found 90° behind the wave crest. The superimposed velocities produced from the pitching and surging motions have a significant effect on the developed thrust and power, producing steeper power and thrust curves than the NREL, and are the likely reason for the remarkable difference in predicted power amplitudes. Figure 13 shows an instantaneous prediction of the vortical structures in air using iso-surfaces of the second invariant of the rate of strain tensor—the Q-criterion (Hunt et al., 1988). Here the platform has reached the crest of the wave and is at maximum surge/pitch velocities away from the incoming wind.
wind and waves. The rotor is interacting with its wake, and power development is at a minimum.

A portion of the mean power difference is also attributed to the disruption of the blade aerodynamics by the tower, a factor not seen in the NREL-FAST results. The black circles on the power time-history plot represent the blades passing in front of the tower. An instantaneous result of this is shown in Fig. 14. In Fig. 14a, the blade is passing in front of the tower marked in the image. In Fig. 14b, the rotor has rotated 180°, and the blade is now pointing away from the tower in the freestream. Significant discontinuities in power generation occur when the tower interferes with the blade aerodynamics. However, note that not all of these discontinuities produce decreases in power. The platform begins surging/pitching away from the incoming wind and waves as the wave trough passes the platform (at t = 5, 15 s in Fig. 12), and the system begins shifting toward the vortex ring state described in Sebastian and Lackner (2010). This can be visualized by the decreasing slope of the power curve in Fig. 12. A temporary decrease in power loss is observed when a blade passes in front of the tower during this motion. The opposite effect occurs as the platform surges/pitches into the incoming wind and waves. Substantial power losses are predicted as the blades pass the tower during this period, some as large as 400 kW. The blades have been modeled rigidly in this study and, if flexible, would be presumed to be deflected even closer to the tower, providing more aerodynamic disruption and effects. A better understanding of this aerodynamic phenomenon can be used to optimize these power discontinuities. This, in turn, can lead to smoother electrical operation as well as less vibration and fatigue.

CONCLUSIONS

A general quasi-static crowfoot mooring-line model is developed and applied to the OC3-Hywind FOWT system. Free-decay time-series tests are performed for both the crowfoot model and the OC3’s AYS model for validation. The time-series results of cases 1.4b and 1.4c agree on amplitude with the results of the OC3’s simulations. However, when compared to experimental data, the two models both show frequency differences in surge caused by variances between simulations, including different restoration between mooring models and modeling error. These results, coupled with the low computational cost of the crowfoot model, alleviate the need to approximate a crowfoot mooring system and open avenues for optimization and application to other FOWT models.

The crowfoot model is used in a full-system, two-phase CFD simulation with steady wind and wave excitation based on OC3 case 5.1. The results are compared to the results of the NREL’s OC3 using FAST. The trends of the motions of the present results agree well with those of the NREL results, showing 25% less mean surge and a slight phase difference in heave. Morison’s equation utilizes a streamwise drag coefficient, set to a constant 0.6 in the OC3’s simulations, and has no mechanism for capturing viscous effects in heave. These hydrodynamic solution differences are the likely reason for most of the motion discrepancies between FAST and the present study. The crowfoot model is shown to decrease yaw from the AYS model used by NREL-FAST. The present results also show that system yaw is largely a function of the instantaneous rotor position, which implies that wind is a significant factor in system yaw. The power generated is strongly correlated with relative changes in wind velocity due to superimposed surge and pitch.
velocities. These surge and pitch velocities are 180° phase-shifted from the instantaneous wave height, such that the maximum power is seen at the trough of the wave and the minimum power is seen at the crest. It is demonstrated that aerodynamic disruption, occurring when the blade passes in front of the tower, causes substantial discontinuities in power generation. The discontinuities are shown to have either a positive or negative effect on power generation, depending on the instantaneous pitch and surge velocity.

FUTURE WORK

The grids of the present study have been redesigned to contain 28 million grid points. Load case 5.1 has been simulated, and preliminary results show little change compared to the present study, supporting the fidelity of the current results. Future verification and validation of the current results will be performed by using the factor of safety method presented in Xing and Stern (2010, 2011). The Mann wind model (Mann, 1998) has recently been implemented into CFDShip-Iowa and will be utilized in future simulations (in OC3 cases 5.2 and 5.3) for more accurate wind velocity field and power generation results. Elastic, rather than rigid, blades will be introduced into the solution process.

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REFERENCES


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