CFD Simulation of a Floating Offshore Wind Turbine System Using a Variable-Speed Generator-Torque Controller

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ABSTRACT

Prediction and control of rotor rotational velocity is critical for accurate aerodynamic loading and generator power predictions. A variable-speed generator-torque controller is combined with the two-phase CFD solver CFDShip-Iowa V4.5. The developed code is utilized in simulations of the 5MW floating offshore wind turbine (FOWT) conceptualized by the National Renewable Energy Laboratory (NREL) for the Offshore Code Comparison Collaboration (OC3). Fixed platform simulations are first performed to determine baseline rotor velocity and developed torque. A prescribed platform motion simulation is completed to identify effects of platform motion on rotor torque. The OC3's load case 5.1, with regular wave and steady wind excitation, is performed and results are compared to NREL's OC3 results. The developed code is shown to functionally control generator speed and torque but requires controller calibration for maximum power extraction. Generator speed variance is observed to be a function of unsteady stream-wise platform motions. The increased mooring forces of the present model are shown to keep the turbine in a more favorable variable-speed control region. Lower overall platform velocity magnitudes and less rotor torque are predicted corresponding to lower rotor rotational velocities and a reduction in generated power. Potential improvements and modifications to the present method are considered.

KEYWORDS: Floating offshore wind turbine, CFD, wind power, turbine control, crowfoot mooring, overset/chimera grid technique

INTRODUCTION

Offshore wind capacity in the United States is becoming a reality. Initial manufacturing has begun, in mid-2015, on the Block Island Wind Farm [1], expected to be in service in late 2016. The US Department of Energy (DOE) has drafted a plan in which the US will utilize 86 GW of offshore wind power by 2050 [2], an aggressive goal considering the US uses no offshore wind power as of year-end 2014. The US has this amount, and much more, available to it [3]. However the majority of offshore capacity available to the US comes from deeper waters where FOWT are required [4]. Siting concerns regarding noise, visual aesthetics, shipping lanes and ecology have made waters farther from shore attractive, and FOWT technology is being aggressively pursued.

Designing for FOWT introduces a level of complexity not seen in onshore designs due to platform motions that, in turn, produce unsteady aerodynamic loading at the blades. Predictions of power require accurate predictions of loading, both aerodynamic and hydrodynamic. Most FOWT simulations to date

have used the blade-element momentum theory (BEM), explained in detail in [5], to determine aerodynamic loading on the rotor and Morison's equation [6] to determine hydrodynamic loading on the platform. The certified wind turbine simulator code FAST from NREL [7], widely used in both the industry and research communities and compared to herein, uses BEM and Morison's equation. BEM is a 2-dimensional quasi-steady method utilizing empirically determined lift and drag coefficients and other correction models, such as dynamic stall and wake models. BEM was developed for analysis of flow perpendicular to the rotor plane. As such BEM, and its current corrections, may not be appropriate for general purpose offshore simulations given the varying yawed inflow conditions, dynamic stall, and potential for rotor-wake interaction [8, 9]. BEM, as designed, also does not consider the tower geometry and requires a correction model to account for the presence of the tower in wake deficit and blade-tower aerodynamic disruption [10, 11]. Morison's equation is a 1-dimensional, semi-empirical function developed to determine hydrodynamic loading, requiring experimentally derived added mass and drag coefficients for any given geometry. Morison's equation assumes the diameter of the structure is small relative to incident wavelength such that wave diffraction effects can be neglected. This is not appropriate for many FOWT platforms, notably buoyancy stabilized platforms such as barges [12]. The use of computational fluid dynamics (CFD) can help to overcome the limitations of BEM and Morison's equation.

With CFD the governing Navier-Stokes equations (NSE) are discretized spatially and temporally into algebraic equations and solved. CFD can intrinsically solve in 3-dimensions, requiring empirical corrections only to determine turbulent characteristics, and can provide details of flow physics that BEM and Morison's equation cannot. With computational resources becoming more readily available, especially parallel-computing resources, finer resolution of both time and space discretization can be accomplished, allowing CFD to scale in a way that correction models may not be able to. 3-dimensional aerodynamic CFD simulations of a wind turbine require a relative rotational motion between a blade or rotor and the surrounding fluid. This presents a challenge to the usage of CFD as many solvers require static grids and cannot model dynamic geometric situations, such as an accelerating rotor. Techniques such as overset or "chimera" meshing [13] and sliding-mesh [14] have been employed for the purposes of platform motion and rotor rotation relative to the tower. The most notable application of CFD to date are simulations based on NREL's onshore unsteady phase VI experiments [15-17]. In these experiments the rotational velocity of the turbine was prescribed, making the dataset excellent for code validation. The rotational velocity of the rotor and developed aerodynamic torque cannot be de-coupled, however, especially considering underlying platform motions. The component of velocity provided by rotor rotation to the blade is usually the dominating component of overall magnitude, particularly on the outboard span of long blades like those used on FOWT. To properly predict generated power, stemming from aerodynamic power developed by the rotor, requires an inertial model of the drivetrain to predict rotor acceleration. Most current CFD simulations of FOWT have used prescribed rotor rotational velocities, with or without platform motions. Prescribed rotor rotation velocity and platform pitch oscillations were used with overset grids in [8] and with sliding-mesh in [18]. Both of these studies also produced predictions using BEM and compared, showing differences between the two methods. The rotor velocity of a FOWT was predicted using an inertial drivetrain model, along with a variable-speed generator (VS) control software scheme, and overset CFD in [19] with a fixed platform and single-phase computation. Rigid-body 6 degree of freedom (6-DOF) platform motions and mooring forces were predicted using overset CFD in [20], where rotor power was investigated but improperly compared to generator power. Prediction of rotor rotational velocity is crucial in calculating proper aerodynamic loading and thrust, especially important for FOWT where the platform is free to pitch and requires careful controller calibration. The present study extends upon [20], using the crowfoot mooring system developed

within and predicting, rather than prescribing, rotor rotational velocity for appropriate comparison to generator power. The authors are unaware of any study, to date, where rotor rotational velocity and FOWT platform motions are simultaneously predicted.

The objective of this study is to develop a FOWT simulation tool that combines an inertial drivetrain model and VS controller, a mooring-line model, and overset CFD with advanced turbulent modeling and a high-resolution gridset. The developed tool is applied to the OC3-Hywind, a model developed by NREL and simulated by multiple expert entities in the Offshore Code Comparison Collaboration (OC3) [21, 22]. Simulations of increasing complexity are performed and results are compared with results produced by NREL during the OC3 [23] using the industry recognized wind turbine simulator FAST [7]. Time histories of predicted platform and rotor motions are analyzed along with predictions of developed and generated power. The effects of platform pitching velocity on blade pressure is examined in pressure coefficient plots.

MATHEMATICAL MODELING AND METHODS

Geometry

The OC3-Hywind, shown in Figure 1, is a variable-speed, variable collective-blade-pitch-to-feather controlled spar-buoy FOWT model based on the full-scale Hywind model developed by Statoil of Norway [24]. It utilizes a 3-bladed, 125 m diameter rotor located at a hub height of 90 m. The turbine sits atop a cylindrical, ballast stabilized spar-buoy platform. Detailed geometric specifications are available in [21, 22].

Figure 1 shows the three coordinate systems used. The earth-fixed system (X, Y, Z) originates at the still water line (SWL) and remains fixed at this point throughout the simulation. The earth-fixed system is initially coincident with the turbine frame, (x_T, y_T, z_T) as displayed in Figure 1. The turbine frame, which translates and rotates with the moving system, originates 120 m vertically upward from the draft of the platform along the centerline of the turbine with the z_T axis pointing upward along the centerline of the platform, the x_T axis pointing from front to back in the circular cross-section of the platform, and the y_T axis pointing to the left when the system is viewed from the front, forming a right-handed orthogonal frame. The blade system (x_B, y_B, z_B) originates at the center of the hub, rotates with the rotor, and includes the blade's cone angle such that the $-z_B$ axis points, at all times, along the pitch axis of blade 1—initially at 0° azimuth and shown directly in front of the tower in Figure 1. The y_B axis points from the leading edge to the trailing edge (TE) of blade 1 at 0° blade twist, and the x_B axis forms a right-handed orthogonal coordinate system with the y_B and z_B axes.

Fluid Modeling

The presented simulations utilize the general purpose unsteady Reynolds-Averaged Navier-Stokes (URANS) and delayed detached eddy simulation (DDES) finite-difference solver CFDShip-Iowa v4.5 [25], which features a two-phase solution method for simulations such as FOWT where coupled aerodynamic and hydrodynamic loading predictions must be considered. The fluid solver utilizes a level-set method [26] which enforces free-surface boundary conditions and predicts the position of the unsteady interface between the air and water phases. CFDShip-Iowa predicts pressures and velocities in both air and water in a 'semi-coupled' fashion. The free-surface position is calculated based on pressure and velocity predictions of the denser phase (water), neglecting any coupling effects with the lighter phase (air). The velocity and pressure predictions of air are then solved subject to the free-surface boundary condition. Overset grid capability [27] is facilitated by SUGGAR [28], an overset software library which is called by the CFD code for domain connectivity at each time step. The code also features a rigid-body

6-DOF motion solver [29], utilized to predict platform motions here. CFDShip-Iowa has been applied and validated against both onshore and offshore wind applications [16, 20].

CFDShip-Iowa solves the dimensionless incompressible continuity and momentum equations

$$\nabla \cdot \mathbf{u} = 0 \quad (1)$$

$$\frac{\partial \mathbf{u}}{\partial t} + \mathbf{u} \cdot \nabla \mathbf{u} = -\nabla \hat{p} + \nabla \cdot \left[\frac{1}{\text{Re}_{\text{eff}}} (\nabla \mathbf{u} + \nabla \mathbf{u}^T) \right] \quad (2)$$

where $\mathbf{u}$ is the fluid velocity vector. The pressure $\hat{p}$ is the piezometric pressure and Re_{eff} is the effective Reynolds number, which are defined as

$$\hat{p} = \frac{p + \gamma Z}{\rho U_0^2} \quad (3)$$

$$\text{Re}_{\text{eff}} = \frac{U_0 L_0}{\nu + \nu_t} \quad (4)$$

Here p is the static pressure, ρ is the fluid density, γ is the specific gravity of the fluid, Z is the depth below the surface (negative for positions above surface in air), U_0 is the free-stream velocity, L_0 is the characteristic length (chosen to be the length of the blade for this study), ν is the fluid's kinematic viscosity, and ν_t is turbulent viscosity.

Delayed detached eddy simulation (DDES) [30], implemented into CFDShip and validated in [31], is used for all simulations for its ability to predict unsteady separated flows. In regions where the turbulent length scale is sufficiently large relative to the local grid size DDES uses large eddy simulation (LES) to directly solve for turbulent viscosity. In remaining regions DDES uses URANS with turbulent modeling to calculate turbulent viscosity. The blended $k - \omega / k - \epsilon$ two equation shear stress transport (SST) model [32] is used for modeling turbulent viscosity in these regions.

Drivetrain Modeling

The drivetrain is modeled as described in the OC3-Hywind reference turbine specification [22]. It is a rigid-structure allowing only for rotation about the rotor central axis. It consists of the rotor, low-speed shaft (LSS), gearbox, high-speed shaft (HSS) and generator as shown in the schematic in Figure 2. The rotor, consisting of the hub and blades, is given a rotational moment of inertia about the low-speed shaft (LSS) of 38,759,232 kg-m². This inertia is calculated with FAST and was verified, through private conversation with Jason Jonkman of NREL, to agree with the figure used by the participants of the OC3. The generator is modeled as having a moment of inertia about the LSS of 5,025,500 kg-m² [22], giving a total moment of inertia about the LSS of 43,784,732 kg-m². The gearbox is given a 97:1 ratio with no modeled internal losses. The inertia and torsional losses of both the LSS and HSS are neglected. The generator is modeled with the same characteristics as the variable-speed generator used by participants of the OC3. The generator is rated at 5 MW of electrical power and a speed of 1173.7 RPM, corresponding to a rated rotor velocity of 12.1 RPM. The generator's efficiency is given as 94.4%, such that the rated mechanical power is 5.297 MW and the rated torque is 43,093.55 N-m. The drivetrain properties relevant are presented in Table 1 and more information about the development of these parameters is available in [22].

The generator torque transmits to the HSS and couples with the aerodynamic torque developed by the rotor to accelerate or decelerate the rotor according to a rotational equation of motion applied to the LSS:

$$T_{Aero} - N_{Gear} T_{Gen} = I_{Dr} \dot{\Omega} \quad (5)$$

where T_{Aero} is the aerodynamic torque developed by the rotor and transmitted to the LSS, N_{Gear} is the gearbox ratio between the HSS and the LSS, T_{Gen} is the generator torque transmitted to the HSS, I_{Dr} is the mass moment of inertia of the drivetrain about the LSS, and $\dot{\Omega}$ is the time rate of change of the rotor velocity, Ω . A first-order forward difference approximation of $\dot{\Omega}$ is used in (5) to solve for the rotor velocity:

$$\dot{\Omega} \approx \frac{\Omega^{n+1} - \Omega^n}{\Delta t} \quad (6)$$

where Ω^{n+1} is the rotor velocity of the next time step, Ω^n is the current rotor velocity, and Δt is the elapsed time between calculations—represented by the global time step used in this study. This time step of 0.017 s, chosen based on previous stability tests, was sufficiently small for only a first-order difference. Introducing (6) into (5) and rearranging provides:

$$\Omega^{n+1} = \frac{\Delta t (T_{Aero}^n - N_{Gear} T_{Gen}^n)}{I_{Dr}} + \Omega^n \quad (7)$$

Equation (7) is an explicit expression for Ω^{n+1} requiring the instantaneous aerodynamic and generator torques, T_{Aero}^n and T_{Gen}^n , respectively. CFDSHIP-IOWA integrates pressure and shear stress over the blades and hub to calculate T_{Aero}^n about the LSS. The VS controller module is called to determine T_{Gen}^n , described in the following section. After T_{Gen}^n is determined, equation (7) is solved and the new rotor azimuth angle is linearly extrapolated:

$$\theta^{n+1} = \Omega^{n+1} \Delta t + \theta^n \quad (8)$$

Variable-Speed Generator-Torque Controller

The variable-speed generator-torque controller works to maximize generated power below rated rotational velocity. Details about the internal workings of variable-speed generators can be found in [5]. The VS controller for the OC3-Hywind is developed in [21, 22], with the relevant details described here. The generator speed is first filtered, using a single-pole low-pass filter with exponential smoothing [33] to avoid high-frequency excitation of the control systems. The filter coefficient, α , is defined as:

$$\alpha = e^{-2\pi f_c \Delta t} \quad (9)$$

where f_c is the corner frequency of the filter and Δt is the time step. The filtering equation is then:

$$\omega_F^n = (1 - \alpha) \omega^n + \alpha \omega_F^{n-1} \quad (10)$$

where ω_F^n is the current filtered generator speed, ω^n is the current, unfiltered generator speed, and ω_F^{n-1} is the filtered generator speed of the previous time step. For each time step equation (10) is solved and the current filtered generator speed is then used as the exclusive input for the torque controller. The torque

provided by the generator is calculated from a piecewise function of RPM based on 5 speed control regions: 1, 1-1/2, 2, 2-1/2, and 3, visualized in Figure 3 and tabulated in Table 2. The optimal line in Figure 3 represents the optimum constant tip-speed ratio, defined as blade tip speed divided by incoming wind velocity, that the VS controller is attempting to maintain while in operation below rated rotor rotational velocity. In region 1 the rotational velocity of the rotor is below the cut-in velocity, the generator torque is set to zero (i.e. no power is extracted), and the aerodynamic torque developed by the rotor blades is used to accelerate the rotor toward cut-in. In region 1-1/2 the generator torque ramps linearly with generator speed. This region serves as a transition between the optimal generator torque curve and the cut-in generator speed to provide a lower limit for operational range. In region 2 the VS controller sets the generator torque for optimal power generation. Region 2-1/2 is another linear ramp used to limit tip speed at rated power for noise concerns. In region 3, above rated generator speed, the torque is held constant at rated.

Mooring Model

The mooring lines utilized for Statoil's pilot Hywind are crowfoot structures, where a catenary line is anchored to the seafloor and splits into two separate fairlead connections at the platform (see lines in Figure 1). The crowfoot mooring system helps to reduce platform yaw by increasing the effective moment arm of the mooring line. This moment arm shifts from one fairlead connection of the crowfoot to the other as the platform yaws and one connection line begins to slacken. NREL approximated the crowfoot line as a single line and supplied it with augmented yaw stiffness (AYS) to compensate. The crowfoot mooring model developed in [20] is employed in the present study. This mooring model considers each of the three catenary components of the crowfoot structure and eliminates the need for any additional stiffness. Figure 4 shows a comparison of restoration forces and moments between the AYS and crowfoot configurations utilized by NREL and the present study, respectively, in four single-DOF displacements. The crowfoot model is observed to provide greater restoration for both platform surge and pitch displacements in the intervals of interest. The AYS and crowfoot models agree very well for platform yaw displacements of less than $\pm 2^\circ$, well within the expected range. Vertical forces due to heave displacements are linear in the range shown as additional line weight is being lifted off or placed onto the seabed. The constant shift between the two models is due to the additional weight of the delta connection of the crowfoot, which uses $\sim 10\%$ additional line per mooring structure.

Numerical Methods and Solution Strategy

The momentum and level-set convection terms are discretized using a fourth-order upwind-biased differencing scheme. A hybrid strategy, which switches to second-order accuracy in regions very close to solid surfaces for stability, is applied to the momentum convection term. A second-order backward differencing scheme is used for temporal discretization in the momentum equation. The overall solution strategy is shown in Figure 5.

SIMULATION CONDITIONS AND DESIGN

Load Cases

Four simulations (Cases 1-4) are performed. A summary of simulated cases is presented in Table 3. All cases use 8 m/s steady, unidirectional incoming wind, approximately 70% rated wind velocity. Case 1 and Case 2 are presented as baseline cases without platform motion and use the input conditions from the OC3's load cases 2.1a and 2.1b, respectively, detailed in [34]. In both of these cases no platform motions occur and hydrodynamic loading and waves are both disabled, although all relevant aerodynamic effects—including wind shear—are included. Both OC3 LC 2.1a and 2.1b feature rigid-body substructures, providing excellent comparison to the rigid-body simulations presented herein. In case 1

the rotor rotational velocity is prescribed at 9 RPM. In case 2, however, the rotor is released and the VS controller engaged to predict rotor rotational velocity.

Cases 3 and 4 both use the input conditions from the OC3's LC 5.1 [23], including regular (Airy) incident waves and platform motions. The exact platform motions from NREL's LC 5.1 results are prescribed in Case 3 to provide baseline results including platform motions and the VS controller. Rotor torque and generator power predictions are compared to those of NREL. In Case 4 the system is released and both platform motions and rotor rotational velocity are predicted. The platform is allowed to move downstream and find an equilibrium point where all transient natural frequency driven motions, notably in surge, have decayed to within 2% relative to the mean. A full comparison of the results of the present method and the motion and power predictions of NREL's FAST is presented. Note that, while NREL's LC 5.1 simulation includes flexible structures, all simulations in the present study assume rigid-body structures.

Grid Design

The grid set used consists of 14 grids, shown in Figure 6 and described in Table 4. A refinement grid (Wake Refinement) is applied to capture high velocity and pressure gradients in the near and far wake of the rotor and tip vortices as they convect downstream. This grid extends approximately 25% of a rotor diameter upstream, approximately 1 diameter downstream, and approximately 1.5 diameters in the rotor plane to allow for downstream wake expansion. The platform refinement block is provided mainly for SUGGAR, which can suffer from numeric instability during determining overlap of coarse grid sections. The Rotor Grids, grouped for brevity and consisting of all blade component grids and the hub grid, are displayed in Figure 6 and shaded in Table 4. All solid geometries from [22, 23] are considered, except for the nacelle which was eliminated for numeric stability during overlap calculations. For each geometric component a surface-fitting 2-dimensional grid was first developed and extruded outward, normal to the surface, to create 3-dimensional volume blocks. The maximum expected Reynolds number on the blades is determined using NREL's LC 5.3 maximum predicted rotor velocity with blade section chords for local length scales. The LC 5.3 maximum—43% greater than NREL's LC 5.1 maximum predicted rotor velocity—for conservative calculations and for potential usage of the same grid set for future OC3 LC 5.2 and 5.3 simulations. The maximum Reynolds number corresponds to the maximum y^+ spacing [35] normal to the blade surface. The rotational velocity of the rotor is the most significant component of the local velocity at the maximum y^+ location and it is assumed that any variance in inflow conditions will scale equally along the entire blade, such that the location of the maximum y^+ spacing on the blade is constant. Thus the normal spacing of the first layer of points off solid-surface grids is assumed to keep the maximum y^+ spacing below 5 for the majority of grid points at all times.

RESULTS AND DISCUSSION

Overview of Flow Field

Several features key to FOWT wake modelling and simulation are observed in the flow field of the near and far wake, which is visually depicted in Figure 7 (a) through (d). Here (a), (b), and (c) present 3-dimensional views of the turbine and Figure 7 (d) shows contours of streamwise velocity at the central vertical cross section of the system. Tip vortices are the dominant feature in Figure 7 (a), (b), and (c), visualized with isosurfaces of the Q-criterion [36]. These vortices generate a helical structure as they convect downstream and provide a visual boundary of the rotor wake. Secondary to the tip vortices is the turbulent activity downstream of the tower which combines with vortices shed from the roots of each blade. An abrupt discontinuity in resolution of both Q-isosurfaces and u-velocity contours is seen at the

streamwise end of the wake refinement grid, approximately 1 rotor diameter downstream. Vertical wake skewing due to platform pitching, platform heaving, and wave height can be seen in the tip vortices of Figure 7 (b) and (c). This corresponds with horizontal stretching and compressing seen in the varying streamwise distance between individual tip vortices. This wake stretching is further compounded in the lower half of the wake as the free-surface waveform has an unsteady, time-dependent height. This causes oscillating local wind velocities closer to the wave surface, seen in the velocity gradients in Figure 7 (d). These variations in velocities produce variations of pressure on the order of those seen in the wake, developing a secondary source of rotation. This secondary rotation will cause the wake to interact with itself, blending tip vortices and producing a situation similar to the vortex ring state described in [9]. This stresses the importance of proper wake modeling for FOWT, which requires calibrated empirical models to account for yawed inflow and unsteadiness in BEM but is intrinsic to CFD solutions. Wake counter-rotation can also be seen in the tower and root vortices of Figure 7 (a) and (b), a result of the chord-wise acceleration of the incoming wind. The substantial drop in velocity in the wake due to kinetic energy extraction is shown in Figure 7 (d). Also visible is the faster moving core wake region, immediately behind the hub and cylindrical blade roots, where very little energy is extracted from the freestream and the impinging of the free-surface induced pressure gradients on the lower half of the wake.

Cases 1 and 2

Time-series results of predicted rotor torque in Cases 1 and 2 are shown in Figure 8 along with the corresponding OC3 results from NREL's FAST, referred to simply as "NREL" from here on. Rotor torque, T_{Rot} , is defined as the torque transmitted through the LSS and includes the torque provided by generator acceleration or deceleration. From a 1-DOF free-body diagram it is calculated as:

$$T_{Rot} = T_{Aero} - I_{Rot} \dot{\Omega} \quad (11)$$

where I_{Rot} is the rotational inertia of the rotor, 38,759,232 kg-m². In both figures only the final 20 seconds of simulation are shown and the initial transient period removed. The drops in rotor torque in both cases are due to a blade passing in front of the tower and are referred to as a blade-tower interaction (BTI) event. Case 1 is intended to compare aerodynamic loading calculation differences between the present study and NREL's results and to serve as a baseline to help explain differences in more the more complex Cases 3 and 4. In Case 1 the rotor is rotating at a prescribed 9 RPM and the rotor azimuth is a simple linear function of time, matching across all OC3 simulations. With a fixed rotor rotational velocity the acceleration term, $\dot{\Omega}$, in equation (11) is zero and the rotor torque is equal to the developed aerodynamic torque. A mean rotor torque of 1.963 MN-m is predicted in Case 1, 6.4% less than the 2.019 MN-m mean rotor torque from NREL's results. These mean torques are represented by dashed lines in Figure 8. The average maximum rotor torque in Case 1 of 1.986 MN-m is 6.1% less than that of NREL's average of 2.115 MN-m. The maximum is predicted in Case 1 to occur immediately after a BTI event while NREL's results predict the maximum immediately before a BTI event. The average minimum torque predicted in Case 1 is 1.902 MN-m, 5.8% less than that of NREL's average minimum of 2.019 MN-m. The loss of rotor torque due to BTI is also predicted in Case 1 to be less than that predicted by NREL. The average difference between minimum and maximum predicted in Case 1 is 0.086 MN-m, 10.4% less than the average difference of 0.096 MN-m from NREL's results. The differences in mean rotor torque across the results of the OC3 LC 2.1a are "about 5%" [37], with NREL representing the highest mean torque. The predicted torque results of Case 1 are closer to 6% but are in the same order as the rough difference seen across the OC3. The results of Case 1 are tabulated in Table 5.

In Case 2 the VS controller is engaged to investigate its effect on rotor torque. The controller varies the generator torque such that the rotor speed is a result of the shaft rotation equilibrium [37], calculated in equation (5) above. The phase difference seen between Case 2 and NREL's results is due to slightly different predictions of rotational velocities. Accordingly the BTI events are not experienced at the exact same time as in Case 1. Very consistent differences in rotor torque are seen due to the controller influence. The mean rotor torque predicted in Case 2 of 1.94 MN-m is 4.4% less than the 2.03 MN-m mean predicted by NREL. Identical 4.4% differences are also predicted in the maximum and minimum torque of Case 2 compared to NREL. The predicted difference between minimum and maximum torque (0.011 MN-m) is the same for both Case 2 and NREL's LC 2.1b results. Note the rotor acceleration during the 20 seconds presented is essentially zero. Accordingly the rotor torque and developed aerodynamic torque are practically identical. The torque results of Case 2 are presented in Table 5. Case 2 serves to verify the operation of the VS controller.

Case 3

Time-series results of predicted rotor torque, generator power, generator speed, and generator torque for Case 3 are shown in Figure 9 (a) and (b) along with the OC3 LC 5.1 results from NREL. The torque and power details for Case 3 are also shown in Table 6. In Figure 9, and in all subsequent time-series plots, any flow and motion transient periods have been removed from results and only the final 30 seconds, corresponding to 3 wave periods, of each result is displayed. The instantaneous wave elevation at the platform centerline is at its maximum at 0, 10, 20, and 30 seconds for all simulations, both present and from NREL. Obvious in Figure 9 (a) is the dominant 10 s wave period in both Case 3 and NREL's rotor torque predictions as well as a secondary frequency seen in NREL's results. In OC3 LC 5.1 the blades, drivetrain, and tower are given flexibility and this secondary frequency is a rotor vibrational frequency not seen in the rigid-body simulation of Cases 3 and 4. Case 3 agrees strongly with FAST in frequency and phase of all predictions of torque, generator speed, and power. Both the mean and maximum (averaged) rotor torque predicted by Case 3 are 5.6% less than NREL's LC 5.1 results, likely a result of the aerodynamic load calculation differences seen in Cases 1 and 2. The average minimum rotor torque predicted by Case 3, however, is 7% less than NREL's results as an effect of the VS controller moving between two different control region strategies.

In Figure 9 (b) time-series predictions of the generator torque and generator speed of Case 3 are presented. The line between VS control regions 1-1/2 and 2 is also drawn. A phase shift between generator speed and generator torque, a product of filtering the generator speed in the VS controller, is observed in the present study and NREL's predictions. In Case 3 peak generator torque predicted is 6% less than that of NREL as a result of decreased peak rotor torque discussed previously. The minimum generator torque is predicted in Case 3 to be 8% less than that of NREL's results as the VS controller shifts between control region 1-1/2 and region 2. In region 1-1/2 the generator ramps down to shutoff, linearly dropping torque with RPM. The predicted generator speed of Case 3 spends ~33% of each wave period in region 1-1/2 while the predictions of NREL spend ~16% of each wave period producing more substantial torque losses in Case 3. Analysis of the generator speed of Case 3 shows little effect of the excursion into region 1-1/2 on the generator speed, however. Also observed is the mere 1.8% less predicted minimum generator speed of Case 3 compared to NREL in Figure 9 (b). These two points suggest that lowering the RPM cutoff line between regions 1-1/2 and 2, mitigating the oscillations between the two regions, could potentially deliver more generator torque with minimal deceleration of the rotor, thereby delivering more generator power for the given conditions. A difference in mean generator torque is thus observed with NREL predicting a mean of 21.3 kN-m and Case 3 predicting 20.5 kN-m, a relative decrease of 3.8%. The resultant generator power for Case 3 is compared to NREL's predictions in Figure 9 (a). The maximum generator power predicted in Case 3 is 2.04 MW, an 8.5% decrease relative to

NREL's maximum of 2.23 MW. The minimum generated power in Case 3 is 1.48 MW while NREL predicts a minimum of 1.64 MW, representing a 9.8% relative decrease in Case 3. These losses in generated power are functions of the reduced generator speed in the present study. The mean power generated in Case 2 is 1.76 MW, an 8.8% decrease relative to NREL's mean of 1.93 MW.

Case 4

In Case 4 the platform is released and allowed to move freely under wind and wave excitation as described in Table 3. Time-series plots of the predicted platform motions and velocities in surge, pitch, heave, and yaw from Case 4 are compared to the predictions of NREL in Figure 10 and a summary of minimum, maximum and mean results from both NREL and Case 4 is presented in Table 6. In Case 4 the maximum predicted platform surge is 13.1 m and the minimum is 10.6 m. These are 13.8% less and 10.9% less than NREL, respectively, and produce a 12.5% reduction in mean surge relative to NREL. These lower predictions are likely due to the increased surge and restoration forces and moments provided by the crowfoot mooring lines compare to the AYS lines used by NREL (see Figure 4). A relative difference of 13.2% in maximum pitch is seen in Case 4 while producing almost identical minimum pitch prediction for an 8% reduction in mean pitch. This reduction in pitch is likely the result of both increased pitch restoration provided by the crowfoot mooring model as well as aerodynamic drag of the tower. The aerodynamic drag of the tower is included in the present results and can be seen in Figure 7. This drag was neglected in NREL's OC3 results and may have an effect on platform pitch. A minimal phase difference is seen between the heave results of Case 4 compared to those of NREL, potentially due to viscous effects in heave being predicted in the present study as well as differences in heave restoration between the different mooring models of the two studies. Large percentage differences are seen in the maximum (225%) and minimum (-10.4%) heave predictions, although these differences, in absolute terms, are a negligible 0.09 m and 0.05 m, respectively. The platform yaw of the Case 4 is seen to be 90° different in phase relative to NREL while displaying the same frequency. Significant reduction in system yaw is seen in Case 4 with the maximum yaw 75.7% less and minimum yaw -65.5% less than the maximum and minimums predicted by NREL. Similar to heave, however, these correspond to negligible absolute values, with a maximum yaw displacement in either study of 0.37°.

The mean velocities of all DOFs are observed to be zero as all transient platform motions have sufficiently decayed. Large decreases of -35.3% and -30.4% (averaged) are seen in both the upwind and downwind magnitudes of surge and pitch velocity, respectively, relative to NREL's results. These lower velocity magnitudes are attributed to the increased mooring restoration in the present study as well as non-linearities in drag calculations present in the URANS equations solved in Case 4. A minimal difference of 0.01 m/s in mean heave velocity is observed and the maximum and minimum yaw velocity differences, 66.7% and 71.4% lower, respectively, are negligible in absolute magnitude.

The time-series predictions of torque and power from Case 4 along with NREL's results, which are identical to those from Figure 9 (a) but repeated for comparison, are shown in Figure 11 (a) and (b). Compared with Case 3, which used NREL's exact OC3 LC 5.1 motions and show relatively consistent differences from NREL in rotor torque, the platform motions of Case 4 are controlled by the crowfoot mooring model and are subjected to lower magnitudes in surging and pitching velocities (see Figure 10 (a) and (b) and Table 6). The mean rotor torque predicted in Case 4, 1.949 MN-m, is 5.8% less than the mean rotor torque predicted by NREL in LC 5.1. This is very similar to the 5.6% decrease of Case 3 and is on the order of the mean differences seen in Cases 1 and 2, attributable to reduced calculated aerodynamic loading. However the maximum rotor torque predicted in Case 4 is predicted to be 8.9% less than that of NREL, and is observed to only deviate 0.144 MN-m, 7.4% relative to its mean, while NREL predicts a deviation of 0.228 MN-m, 11.0% relative to their mean. The minimum rotor torque in Case 4 is

predicted to be 3.0% less than that of NREL and to deviate 0.154 MN-m, 7.9% relative to its mean, while NREL predicts a deviation of 0.219 MN-m, 10.6% relative to their mean. In Figure 11 (a) Case 4 clearly shows less deviation from the mean in both maximum and minimum rotor torque. This difference is attributed to the decreased upwind and downwind velocities of Case 4 relative to NREL due to increased restoration forces of the crowfoot mooring model. The difference in maximum developed torque may also be a result of predicted separation caused by the increased effective angle of attack (AoA) experienced during the upwind velocity phase of platform motion. Upwind relative velocity of the platform increases the effective AoA seen by the blade by increasing the magnitude of the incoming wind velocity component, nominally perpendicular to the rotor plane. This in turn generates high magnitude pressure coefficients at the leading edge of the blade but develops strong adverse pressure gradients at the rear, promoting separation. The full span of the suction side of the blade during both maximum downstream and maximum upstream velocities is shown Figure 12 (a) and (b), respectively, contoured by local C_p . The developed suction pressure is shown to be significantly lower in magnitude during maximum downstream velocity (Figure 12 (a)) than during maximum upstream velocity (Figure 12 (b)) along the entire blade span beyond the root transition region. In both situations a large separation region exists at the TE of the cylindrical root and the transition region between cylinder and blade, as well as similar separation regions at the blade tip. The maximum downstream situation remains attached over the remainder of the span of the blade. In the maximum upstream situation, however, the root separation region spans 7% more of the blade and TE separation occurs on the outboard 1/3 of the blade, including a large-scale separation bubble at 80% span. In Figure 13 the outboard 20% of blade 1 is shown during maximum downstream velocity in the left frame (a) and during maximum upstream velocity in the right frame (b). The TE separation of the upstream velocity situation is more clearly visualized in Figure 13 (a), along with the separation bubble detailed in the inset. The increased AoA during maximum upstream velocity can be seen in the plane-section streamlines of Figure 13 (b) compared to those of Figure 13 (a), as well as the increased C_p magnitudes on both the suction and pressure sides of the blade. While producing similar mean values, the smaller deviations of rotor torque produce smaller bending moments on the blades and less torsion in the shafts, potentially reducing overall fatigue. This adds to the importance of the mooring system to limit streamwise velocity fluctuations.

The generator speed predicted in Case 4 is shown in Figure 11 (b). The diminished platform velocities in Case 4 are observed to reduce deviations from the mean generator speed compared to that of Case 2, and the generator spends only 28% of the wave period in VS control region 1-1/2 instead of the 33% observed in Case 2. The maximum generator torque of Case 4, also shown in Figure 11 (b), deviates 1.4 kN-m from a mean of 20.1 kN-m (7.0% relative) and the minimum generator torque deviates 1.8 kN-m from the mean (9.0% relative). These same generator torque deviations are observed in Case 3, perhaps more directly comparable to Case 4 than NREL's results due to solution modeling differences between the present study and NREL's FAST software. In Case 3 is predicted a 1.7 kN-m deviation from the mean in maximum generator torque (8.4% relative) and a substantial 2.9 kN-m deviation in minimum generator torque (14.2% relative). The lesser deviations of generator torque in Case 4 compared to Case 3 help to reduce fatigue along the entire drivetrain. The resultant generator power developed in Case 4 is shown in Figure 11 (a). The maximum power generated in Case 4 is 1.95 MW, which is 12.6% less than the maximum of 2.23 MW generated by NREL and 4.4% less than the 2.04 MW maximum generated in Case 3. The minimum power generated in Case 4 is 1.55 MW, which is 5.5% less than the minimum 1.64 MW predicted by NREL and 4.5% greater than the 1.48 MW minimum predicted in Case 3. The difference between the minimum generator power in Case 4 and Case 3 is largely a function of the smaller amount of time spent in VS control region 1-1/2 in Case 4 compared to Case 3. The mean power generated in Case 4 is 1.77 MW, which is 8.3% less than the predicted mean power by NREL. The mean generated

power of Case 3 is 0.01 MW higher than that of Case 3. While this difference is relatively negligible it suggests that minimizing platform velocities, thereby reducing generator speed deviation, can help with more precise controller design.

CONCLUSIONS

An inertial rotor model with a VS generator-torque controller is coupled with high resolution CFD and a mooring force model to predict motion and generated power of FOWT. The developed code is utilized in four simulations of the OC3-Hywind FOWT using the OC3's LC 2.1a, 2.1b, and 5.1 wind and wave conditions. Results are compared to the publically available OC3 results of NREL using their FAST software. Simulations utilize an incremental approach for verification of the method. OC3 LC 2.1a, featuring a fixed platform and rotor rotational velocity, is first simulated (Case 1) to determine a baseline expectation of rotor torque considering the different aerodynamic solution differences between the present CFD solver and NREL's FAST. The results show about 6% less maximum, minimum, and mean rotor torque than NREL's predictions, within the range of OC3 participants.

A second simulation (Case 2) is performed using the conditions of OC3 LC 2.1b. The platform is still fixed, however the inertial rotor model and VS controller are now activated to investigate the effect of torque control on rotor torque. A very consistent difference of 4.4% is seen between Case 2 and NREL's results in each of mean, maximum, and minimum rotor torque, verifying the operation of the VS controller.

NREL's OC3 LC 5.1 predicted platform motions are prescribed in a simulation (Case 3) while using the inertial rotor model and VS controller. The results of Case 3 serve to identify the effect of the unsteady aerodynamic solution differences between CFD and FAST on generator torque and power predictions. The generator speed results of Case 3 agree within to 3% of NREL's generator speed predictions. However the generator speed of Case 3 is observed to spend 17% more time per wave period in a lower VS control region than NREL's speed, and minimum generator torque predictions of Case 3 are observed to be 8.3% lower than those of NREL as a result. Mean generated power is predicted 8.8% below the mean of NREL's predicted power due to the decreases in generator speed, and corresponding generator torque, experienced during the extra time spent in the lower control region. The results of Case 3 suggest a recalibration of the VS control region cutoffs to help keep generator speed up and increase overall generator power developed.

A final simulation (Case 4) is performed where the platform motions are predicted using semi-coupled 2-phase CFD and a crowfoot mooring model. The inertial rotor model and VS controller are active and both aerodynamic and hydrodynamic loading are considered. Reductions in mean surge translation and mean pitch relative to NREL's predictions are observed due to increased mooring forces. A 32.7% reduction in maximum platform surging velocity and a 31.4% reduction in maximum platform pitching velocity are also observed. These correspond to reduced upstream and downstream velocities and are shown to keep the generator speed in a more favorable VS control region, and generated power is slightly increased (0.01 MW) from Case 3 despite a 0.2% reduction in mean rotor torque. This again demonstrates the importance of VS control region calibration. Separation over the outboard 1/3 of the blade is predicted during maximum upstream pitching velocity, verifying the importance of stabilization of the platform.

FUTURE WORK

The present results suggest a modification of the VS controller scheme. The VS controller is designed to maximize power capture below rated rotor rotational speed. At, or beyond rated velocity, however, requires a method of releasing torque from the system to avoid generator overload. A blade-pitch

controller will be added in the new future to the developed code for analysis of rated conditions and beyond. Combined with the Mann wind model, recently implemented into CFDShip-Iowa [38] OC3 LC 5.2 will be run. The crowfoot mooring lines, seen to add stability in the present study, could potentially be optimized, including clump weights and predicted dynamics. Quantitative verification and validation should be performed to evaluate the numerical and modeling errors and uncertainties using the recent general framework for LES [39] The potential for incorporation of other models into the present system also exist. These potential modifications could include a drivetrain dynamic model or deformable blades and tower, both investigated in [19], or brakes for start-up and shut-down simulations.

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TABLES

Table 1. Drivetrain properties

Drivetrain rotational inertia about LSS	43,784,732 kg-m ²
Rotor rotational inertia about LSS	38,759,232 kg-m ²
Gearbox ratio	97:1
Rated rotor velocity	12.1 RPM
Rated generator velocity	1173.7 RPM
Generator efficiency	94.4%
Rated generator power	5 MW
Rated generator torque	43,093.55 N-m

Table 2. VS controller control regions with corresponding generator and rotor speeds

Control Region	Generator Speed [RPM]	Rotor Speed [RPM]
1	<670	<6.9
1-1/2	670 - 871	6.9 – 9.0
2	871 - 1138	9.0 – 11.7
2-1/2	1138 - 1162	11.7 – 12.0
3	>1162	>12.0

Table 3. Simulation case matrix

Case	Platform Motions	Rotor Rotation	Simulation Length	Wave Conditions	Wind Conditions
1	None	Prescribed (9 RPM)	60 s	None	Steady, unidirectional 8 m/s
2	None	Predicted	125 s	None	
3	Prescribed	Predicted	120 s	Regular (Airy) waves: H = 6 m, T = 10 s	
4	Predicted	Predicted	887 s		

530 Table 4. Grid names and points with directions. Rotor Grids from Figure 6 shaded in gray.

Grid Name	i,j,k Points	Grid Dimension Direction	Total Points
Tower	i – 271	Along length of tower/platform (z_T)	1,750,931
	j – 71	Outward normal to tower surface	
	k – 91	Circumferential around tower/platform	
Platform Refinement	i – 53	Fore-aft of tower/platform (x_T)	199,439
	j – 53	Horizontally transverse to flow (y_T)	
	k – 71	Along length of tower/platform (z_T)	
Wake Refinement	i – 185	Fore-aft of tower/platform (x_T)	6,331,625
	j – 185	Horizontally transverse to flow (y_T)	
	k – 185	Along length of tower/platform (z_T)	
Rotor Hub	i – 56	Along length of hub	198,856
	j – 53	Outward normal to hub surface	
	k – 67	Circumferential around hub	
Blade Main (x3)	i – 313	Along length of blade ($-z_B$)	2,122,140
	j – 60	Outward normal to blade surface	
	k – 113	Circumferential around blade	
Blade Root (x3)	i – 45	Along length of blade root ($-z_B$)	272,700
	j – 60	Outward normal to blade root surface	
	k – 101	Circumferential around blade root	
Blade Tip (x3)	i – 51	Along chord of blade tip	149,328
	j – 48	Outward normal to blade tip surface	
	k – 61	Lengthwise wrapped over blade tip	
Background	i – 223	In streamwise direction (X)	4,074,433
	j – 121	Horizontally transverse to streamwise (Y)	
	k – 151	Vertically upward (Z)	
Total			20,187,788

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532 Table 5 Comparison of rotor torque between Case 1, Case 2, and NREL

	Case 1			Case 2		
	Rotor Torque [MN-m]			Rotor Torque [MN-m]		
	Min	Max	Mean	Min	Max	Mean
NREL	2.019	2.115	2.097	2.025	2.036	2.034
Present Study	1.902	1.986	1.963	1.936	1.947	1.944
Difference	-0.117	-0.129	-0.134	-0.089	-0.089	-0.090
Relative %	-5.8%	-6.1%	-6.4%	-4.4%	-4.4%	-4.4%

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Table 6. Comparison of motions and power characteristics between Case 3, Case 4, and NREL

	Rotor Torque [MN-m]			Generator Speed [RPM]			Generator Torque [kN-m]			Generator Power [MW]		
	Min	Max	Mean	Min	Max	Mean	Min	Max	Mean	Min	Max	Mean
NREL	1.851	2.298	2.070	865	962	913	19.2	23.5	21.3	1.64	2.23	1.93
Case 3	1.721	2.170	1.954	849	934	893	17.6	22.2	20.5	1.48	2.04	1.76
Difference	-0.130	-0.128	-0.115	-16	-28	-20	-1.6	-1.3	-0.8	-0.16	-0.19	-0.17
% Relative to NREL	-7.0%	-5.6%	-5.6%	-1.8%	-2.9%	-2.2%	-8.3%	-5.5%	-3.8%	-9.8%	-8.5%	-8.8%

	Surge [m]			Pitch [°]			Heave [m]			Yaw [°]		
	Min	Max	Mean	Min	Max	Mean	Min	Max	Mean	Min	Max	Mean
NREL	11.9	15.2	13.6	1.93	3.57	2.75	-0.48	0.04	-0.22	-0.37	0.29	-0.03
Case 4	10.6	13.1	11.9	1.95	3.10	2.53	-0.43	0.13	-0.15	-0.09	0.10	0.01
Difference	-1.3	-2.1	-1.7	0.02	-0.47	-0.22	0.05	0.09	0.07	0.28	-0.19	0.04
% Relative to NREL	-10.9%	-13.8%	-12.5%	1.0%	-13.2%	-8.0%	-10.4%	225.0%	-31.8%	-75.7%	-65.5%	-133.3%

	Surge Velocity [m/s]			Pitch Velocity [°/s]			Heave Velocity [m/s]			Yaw Velocity [°/s]		
	Min	Max	Mean	Min	Max	Mean	Min	Max	Mean	Min	Max	Mean
NREL	-0.98	0.98	0.00	-0.51	0.51	0.00	-0.16	0.16	0.00	-0.21	0.21	0.00
Case 4	-0.66	0.66	0.00	-0.36	0.35	0.00	-0.17	0.17	0.00	-0.06	0.07	0.00
Difference	0.32	-0.32	0.00	0.15	-0.16	0.00	-0.01	0.01	0.00	0.15	-0.14	0.00
% Relative to NREL	-32.7%	-32.7%	0.0%	-29.4%	-31.4%	0.0%	6.3%	6.3%	0.0%	-71.4%	-66.7%	0.0%

	Rotor Torque [MN-m]			Generator Speed [RPM]			Generator Torque [kN-m]			Generator Power [MW]		
	Min	Max	Mean	Min	Max	Mean	Min	Max	Mean	Min	Max	Mean
NREL	1.851	2.298	2.070	865	962	913	19.2	23.5	21.3	1.64	2.23	1.93
Case 4	1.795	2.093	1.949	857	919	888	18.3	21.5	20.1	1.55	1.95	1.77
Difference	-0.056	-0.205	-0.121	-8	-43	-25	-0.9	-2.0	-1.2	-0.09	-0.28	-0.16
% Relative to NREL	-3.0%	-8.9%	-5.8%	-0.9%	-4.5%	-2.7%	-4.7%	-8.5%	-5.6%	-5.5%	-12.6%	-8.3%

FIGURES

Figure 1 Three coordinate systems: earth-fixed frame (X, Y, Z), turbine system frame (x_T, y_T, z_T), and blade system for blade 1 (x_B, y_B, z_B)

Figure 2 Drivetrain schematic

Figure 3 Generator torque vs. generator speed response of the variable-speed controller (reproduced from [22] with edits from [23])

Figure 4 Net restoration comparison of crowfoot and AYS mooring lines

Figure 5 Solution strategy including VS controller module

Figure 6 Grid set utilized. Grids points are skipped in all directions for clarity

Figure 7 3D views of turbine and vortical structures using isosurface $Q=1$:
 (a) rear view, (b) top view, (c) side view, and (d) side view of contours of streamwise velocity

Figure 8 Comparison of rotor torque between Case 1, Case 2, and NREL-FAST

Figure 9 Comparisons of aerodynamic torque and generator predictions between Case 3 and NREL-FAST

Figure 10 Comparison of platform motions between Case 4 and NREL-FAST

Figure 11 Comparisons of aerodynamic torque and generator predictions between Case 4 and NREL-FAST

Figure 12 Limiting streamlines on suction side of blade, colored by C_p :
(a) maximum downstream platform velocity; (b) maximum upstream velocity

Figure 13 Outboard 20% of blade showing limiting streamlines and colored by C_p :
 (a) maximum downstream platform velocity; (b) maximum upstream velocity. Inset shows separation zone at 82% blade span.

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