Efficient critical design load case identification for floating offshore wind turbines with a reduced nonlinear model

Denis Matha, Frank Sandner, David Schlipf

Endowed Chair of Wind Energy, Universität Stuttgart
Allmandring 5B, 70569 Stuttgart, Germany
E-mail: denis.matha@ifb.uni-stuttgart.de

Abstract. Design verification of wind turbines is performed by simulation of design load cases (DLC) defined in the IEC 61400-1 and -3 standards or equivalent guidelines. Due to the resulting large number of necessary load simulations, here a method is presented to reduce the computational effort for DLC simulations significantly by introducing a reduced nonlinear model and simplified hydro- and aerodynamics. The advantage of the formulation is that the nonlinear ODE system only contains basic mathematic operations and no iterations or internal loops which makes it very computationally efficient. Global turbine extreme and fatigue loads such as rotor thrust, tower base bending moment and mooring line tension, as well as platform motions are outputs of the model. They can be used to identify critical and less critical load situations to be then analysed with a higher fidelity tool and so speed up the design process. Results from these reduced model DLC simulations are presented and compared to higher fidelity models. Results in frequency and time domain as well as extreme and fatigue load predictions demonstrate that good agreement between the reduced and advanced model is achieved, allowing to efficiently exclude less critical DLC simulations, and to identify the most critical subset of cases for a given design. Additionally, the model is applicable for brute force optimization of floater control system parameters.

1. Introduction

Floating offshore wind turbines (FOWT) offer the prospect of utilizing the vast amount of high quality wind resources in water depths beyond 40-60m, where industry-standard fixed bottom designs such as jackets, tripods or large diameter monopiles become less economic. Particularly regions with coastlines lacking shallow water areas, e.g. in Spain, Portugal, Norway or parts of the United States are becoming a primary focus for development of floating wind farms. Currently Statol’s Hywind Spar buoy prototype off the coast of Norway and EDP’s Wind Float Semi-Submersible off Portugal represent the first utility scale floating prototypes. Spain, Portugal, Norway, the US, UK, Japan and South Korea all have announced plans to develop floating offshore wind capacities in the near future. This development has propelled the requirement of common guidelines and standards for design and certification of such systems, with e.g. the International Electrotechnical Commission (IEC) in 2011 establishing the IEC TC88 workgroup PT61400-3-2 with the task to develop a new technical specification for FOWTs with a potentially extended set of design load cases (DLC).

Design verification of wind turbines is performed by simulation of DLCs defined in the IEC 61400-1 and -3 standards or equivalent guidelines by certification bodies. However, a complete set of DLCs for floating offshore wind turbines will likely amount to more than 10,000 single DLC, considering additional environmental and failure conditions for floating turbines [1]. Additionally, the
design codes used to perform the DLC simulations need to model not only the wind turbine including aerodynamics and control, but also the hydrodynamic loads on the platform and mooring system. This added complexity for simulation of FOWTs yields increased simulation times. Together, this increase results in additional computational effort for the designer, who often needs to repeat the DLC simulations multiple times during an iterative design process.

Karimirad and Moan [2] recently presented a simplified coupled model showing satisfactory agreement to more complex design codes in the frequency domain and in terms of loads and platform displacements, yet it has not been applied to a complete set of IEC DLCs. A limitation of this study was that the controller is only modeled by a filter, thus the non-linearity introduced by a complex controller is not represented sufficiently.

In this paper a novel method is presented to reduce the computational effort for DLC simulations significantly by introducing a reduced nonlinear model with simplified effective hydro- and aerodynamics and the capability to use the same controllers as in complex aero-elastic codes and on a real turbine: The presented model has been developed from scratch by Sandner [3] and is based on models developed by Schlipf et. al. [4] at the University Stuttgart for controller applications. The platform and wind turbine structure is modeled as a multibody system consisting of four rigid bodies with five degrees of freedom (DOF).

The resulting 10 state nonlinear system of ODEs is calculated applying the Newton-Euler scheme and given symbolically in state-space. Wind loads are predicted by reducing the 3D-wind field to a one dimensional rotor-effective wind speed at hub height and the aerodynamic thrust and torque is then calculated based on a-priori computed non-dimensional power and thrust coefficients. Based on a-priori calculated Airy wave kinematics, a relative formulation of Morison’s Equation is applied to calculate the platform hydrodynamic load. The advantage of this formulation compared to regular aero-servo-hydro-elastic models, which can usually also be setup to feature the same limited DOF as the presented model by deactivating DOF, is that the nonlinear ODE system only contains basic mathematic operations and no iterations or internal loops. Only two one-dimensional external disturbances, wind and waves, are used as external inputs. It therefore is very computationally efficient and the ODE formulation also potentially allows the application in model-predictive control schemes and design of other model based controllers.

Global turbine extreme and fatigue loads such as rotor thrust, tower base bending moment and mooring line tension, as well as platform motions are outputs of the model, which then are used to identify critical and less critical load situations.

In summary, the presented model reduces computational time by two to three-digit factors (magnitude depending on the degree of optimization of the compiled code and used hardware) compared to a traditional aero-servo-hydro-elastic simulation with e.g FAST. Results from these reduced model DLC simulations support the process of narrowing down on a reduced number of potentially critical DLCs, which then can be analysed using much more advanced and detailed simulation models. It enables the designer to immediately check the implications of design changes of e.g. control parameters or mooring line configuration on the critical subset of load cases, which then only need to be analysed in greater detail.

Results from a subset of IEC-style fatigue and extreme DLCs simulations is presented and compared to results from FAST calculations. Results in frequency and time domain demonstrate that good agreement between the reduced and advanced model is achieved, allowing to correctly exclude a large number of DLCs, and to identify the most critical subset of cases for a given design.

Current limitations of the model arise from the reduced DOF and simplifications of external loads. Particularly results for individual blade pitch errors and skewed wind inflow vectors are limited since the simplified aerodynamic model at the current status does not account for each blade individually and uses simple approximations for yawed inflow; therefore certain DLC conditions cannot accurately be represented and reliable load assumptions can only be derived for a subset of DLCs and a subset of turbine signals.
2. Reduced nonlinear model description

This chapter presents a concise overview of the structural, aerodynamic and hydrodynamic properties of the reduced nonlinear model. A more detailed description of the model is presented by Sandner [3], the developer of the model.

2.1. Structural model

Typical design codes [5] use combined modal or finite element (FE) and multibody system dynamics for the structural representation of the turbine. In some codes, the mooring system is also part of the structural MBS or FE model, while others use a quasi-static approach. The reference simulations to which the results of the reduced model are compared to in this study are obtained with NREL’s FAST code [6]. FAST is one of the computationally most efficient widely used and validated coupled aero-servo-hydro-elastic codes capable of FOWT simulation and therefore is utilized as an unbiased assessment of the advantages of the simple model in this study.

In the reference FAST model the wind turbine blades and tower are modelled using a nonlinear modal representation assuming small deflections, with two flapwise bending modes and one edgewise bending mode per blade and two fore-aft and two side-to-side bending modes for the tower. The mode shapes of the blades and tower are pre-calculated with a FEM based preprocessor. The floating platform has six DOFs and the drivetrain is modelled using an equivalent linear spring and damper. Overall the reference FAST model has 21 DOFs.

For the reduced model, the so-called “SLOW” model, a rigid multibody system approach is chosen. The system consists of four rigid bodies with an associated lumped inertial mass and a mass moment of inertia. The only elastic part is a spring-damper element representing tower deformation. The rotor is represented by a disk-like body attributed with a mass and a mass moment of inertia without any degrees of freedom allowing bending of the blades. These simplifications are acceptable because main dynamic excitations on the FOWT range from wave loads of about 0.1 Hz to rotor-induced 1P oscillations of 0.2 Hz. High aerodynamic damping mostly dissipates high-frequency blade oscillations so that the frequency spectrum of onshore WT shows a characteristic cut-off frequency of 0.2 Hz [4]. In summary the model features 9 DOFs, consisting of all six translatory and rotatory motions of the platform, two tower bending motions and the rotor rotation. Figure 1 shows the topology of the structural model.

![Figure 1. Multibody topology](image)

The mathematical model has been derived using the Newton-Euler formalism which takes all dynamic effects of local rotations like Coriolis and gyroscopic forces into account. This is possible through the momentum and angular momentum equations of each body that are combined in a coupled system of
ordinary differential equations (ODE). For the translatory motions, the momentum equation according to Newton’s 2\textsuperscript{nd} Law for the system in each direction \(i\) can be written as eq. (1):

\[
m \cdot \ddot{a} = m \cdot J_{Ti} \cdot \dot{q} + m \cdot \ddot{\alpha} = f_i^e + f_i^r.
\]  

(1)

Accordingly, for rotational motions the momentum equation becomes:

\[
I \cdot \ddot{\omega} \cdot \dot{I} \cdot \omega = t_i^e + t_i^r.
\]

(2)

Here \(m\) represents the 3x3 mass matrix of body \(i\), \(q_i\) and \(\ddot{\alpha}_i\) are the bodies’ 3x1 total and local translatory accelerations, \(\omega\) the 3x1 angular velocities and \(\ddot{\omega}_i\) its corresponding skew symmetric matrices, while \(I\) is the 3x3 mass moment of inertia and \(J_{Ti}\) and \(J_{Ri}\) are Jacobi-Matrices separating curvilinear motions into translatory and rotatory directions. The force vectors on the right hand side \(f_i^e + f_i^r\) are external forces and reaction forces and \(l_i^e + l_i^r\) are the vectors of applied and reaction torques on body \(i\). The vector of the reduced models’ degrees of freedom is \(q\).

Combining Eq. (1) and (2), neglecting Coriolis forces, eliminating reaction forces pointing into constrained directions and denominating \(k\) the vector of generalized coriolis-, centrifugal and gyroscopic forces and \(p\) the vector of generalized forces, the global nonlinear Newton-Eulerian Equations of the reduced nonlinear model become:

\[
M(q) \cdot \dot{q} + k(q, \dot{q}) = p(q, \dot{q}).
\]  

(3)

Transformed into state-space for convenient numerical integration, the equation yields:

\[
\dot{x} = \frac{\partial x}{\partial t} = \begin{bmatrix} \dot{q} \\ \dot{\dot{q}} \end{bmatrix} = \begin{bmatrix} \dot{q} \\ \dot{\dot{q}} \end{bmatrix} = \begin{bmatrix} \dot{q} \\ \dot{\dot{q}} \end{bmatrix} = (M^{-1}(p - k)).
\]

(4)

The mooring system is modeled by using a force-displacement relationship (look-up table) derived from a quasi-static mooring model [7]. The orientation of the mooring force vectors at the fairleads is approximated trigonometrically using the current position of the platform. For simplification the mooring forces with the determined orientation are applied at the spar’s centerline and do not account for the small lever arm due to the off-centerline location of fairlead connection points. Comparison with the original quasi-static model show good agreement.

2.2. Aerodynamics

In the reference FAST model, aerodynamic forces are calculated using quasi-steady blade-element/momentum (BEM) theory including the effects of axial and tangential induction. Tip and hub losses according to Prandtl, skewed-wake corrections and dynamic stall using the Beddoes-Leishman model are considered.

For the reduced model, the BEM algorithm, although simple and quite efficient, is not considered applicable because it is an iterative algorithm. As stated above the goal is to have a resulting model constituted only of ODEs. This is achieved by implementing a method previously applied and proven by Schlipf [4] for nonlinear model predictive control for onshore turbines using LiDAR measurements and modifying it for oblique inflow.

In the reduced model, the loads from aerodynamics are confined to an aerodynamic torque \(M_{aero}\) and the thrust force \(F_{aero}\) on the shaft, being a function of only tip speed ratio \(\lambda\) and rotor azimuth angle \(\theta\):

\[
\begin{bmatrix} F_{aero} \\ M_{aero} \end{bmatrix} = -\frac{1}{2} \rho \pi \left[ \frac{R^2 c_T(\lambda, \theta)}{R^3 c_p(\lambda, \theta)} \right] \cdot v_{rel}^2 \cdot \hat{r}_{rotor}.
\]

(5)

Here, \(\rho\) denotes air density, \(R\) rotor radius, \(v_{rel}\) the relative horizontal velocity of the rotor-plane \(\hat{r}_{rotor}\) to the incoming wind speed \(v_0\) and \(c_T\) and \(c_p\) the dimensionless thrust and power coefficients. The “effective” hub height wind speed \(v_0\) for turbulent wind is derived by weighting the wind speed vectors of a precalculated turbulent wind field using NREL’s TurbSim on the rotor disc according to a non-dimensional average out-of-plane force distribution along the blade radius accounting for tip- and
root-loss effects. The simulation time for the precalculation of turbulent windfields is disregarded in the comparison for both FAST and the reduced model.

To account for the misalignment of the rotor plane with the incoming wind due to platform motions a correction model is introduced to account for oblique inflow. Since $c_T$ and $c_p$ are originally computed for the onshore turbine featuring a $\theta_{tilt} = 5^\circ$ rotor tilt, instead of using the wind speed $v_0$ regardless of the platform motion, the wind speed $v_{0,proj}$, geometrically projected ($\cos(\ldots)$) on the direction of $v_0$ is used.

\begin{equation}
    v_{rel} = -\dot{r}_{rotor} - v_{0,proj}.
\end{equation}

\begin{equation}
    v_{0,proj} = v_0 \cdot \frac{\cos(\beta_{PtfmP tch})}{\cos(\theta_{tilt})}.
\end{equation}

Also included, but not applied for this load case study, is a 4D table-interpolation based correction model accounting for the varying location where the resultant thrust force acts on the rotor-plane depending on the platform motion. In the next evolution stage of the model, this method will be included, as well as representing shear and effects on single blades.

It is clear that this aerodynamic model represents a considerable simplification of the aerodynamics and does not account for various effects covered by BEM with corrections, not mentioning potential flow and CFD based methods. These advanced aerodynamic analyses have e.g. shown [8], that the inflow conditions on FOWT are considerably complex and even BEM based methods might over- or underpredict loads significantly. Nevertheless, the presented aerodynamic model represents the physics in an acceptable level of detail and in an efficient manner.

2.3. Hydrodynamics.

The reference model in FAST uses HydroDyn to calculate the hydrodynamic forces due to waves on the platform: Airy wave theory with free-surface corrections is used to calculate the wave kinematics. The hydrodynamic loading includes contributions from linear hydrostatic restoring, nonlinear viscous drag contributions from Morison’s equation, added mass and damping contributions from linear wave radiation (including free-surface memory effects), and incident wave excitation from linear diffraction. The linearized radiation and diffraction problems are solved in a pre-processing step in the frequency domain using any panel-based program for computing wave loads.

More sophisticated hydrodynamic models in FOWT design codes exist, accounting also for 2nd order effects, but for the investigated slender spar buoy, Morison equation can be considered as sufficient. Therefore, for the reduced model, the external hydrodynamic force vector on the spar consists of contributions from the relative formulation of Morison equation [9] and hydrostatic restoring [7]:

\begin{equation}
    F_{hydro} = F_{hydrostatic} + F_{Morison}
\end{equation}

For non-hydrodynamically-transparent platform geometries the approach may not be sufficient and may require modification. Typically the wave kinematics are computed using Airy wave theory. To avoid the required numerical integration (IFFT) to obtain wave velocities and accelerations for Morison’s equation, a method based on the deepwater approximation [9] is used to estimate the velocities $v(\eta, z)$ and accelerations dependent on wave elevation $\eta$ and water depth $z$ only. The approximation yields an exponential function with the factors $a$ and $b$, which only depend on the wavenumber $k$ and thereon the peak spectral period $T_p$.

\begin{equation}
    v(\eta, z) = a \cdot \eta \cdot e^{bz}
\end{equation}

For the horizontal velocity e.g. using the deepwater approximation this results in $a=\omega_p$ (wave peak frequency) and $b=k_p$ (peak wavenumber). During the load calculations, $T_p$ is a-priori known for each individual load simulation and provided to the model as an input, as well as the wave elevation $\eta$, which is thus identical to the FAST simulations’ wave elevation. This implies that the pre-calculation of wave elevation is required by an external tool. Since in FAST this wave generation is performed
directly in conjunction with each simulation run, the simulation time comparison is slightly biased in this respect. For control purposes where no real-time measurement of this quantity is assumed, the factors could be calculated for a mean peak spectral period and left constant for each DLC.

For numerical reasons (acceleration is unknown in the current timestep) the added mass is included as part of the mass matrix and is also considered for the platform’s mass moment of inertia. With these simplifications and neglecting of vertical fluid and structure motions, Morison’s equation is formulated in a form allowing for analytical integration. A benefit of this method is also that it enables the possibility for predictive control methods based only on wave elevation measurements in front of the FOWT, e.g. with a wave rider buoy.

3. IEC Load simulations

To assess the applicability of the model for excluding a subset of simulations of particular DLCs or potentially complete sets of DLCs, an IEC-style loads analysis according to Table 1 is performed with the reference FAST model and subsequently with the reduced model. As reference FOWT, the OC3 Hywind spar buoy model is used [10]. The wind and wave combinations, number of seeds, simulation times and applied safety factors on loads is according to a typical approach described in detail by Jonkman [7]. This source is also used to define the reference site conditions in this study— a site north of Scotland with relatively extreme metocean conditions - and the failure conditions.

Table 1. IEC Design Load Cases

| DLC | Wind Conditions | Wave Conditions | Events | PSF |
|-----|----------------|----------------|--------|-----|
| 1.x Power Production | | | | |
| 1.1 | NTM $V_{in} < V_{hub} < V_{out}$ | NSS $H_s = E[H_s | V_{hub}]$ | $\beta = 0^\circ$ | Normal operation | 1.25 : 1.2 |
| 1.3 | E1M $V_{in} < V_{hub} < V_{out}$ | NSS $H_s = E[H_s | V_{hub}]$ | $\beta = 0^\circ$ | Normal operation | 1.35 |
| 1.4 | ECD $V_{hub} = V_r \pm 2 \frac{m}{s}$ | NSS $H_s = E[H_s | V_{hub}]$ | $\beta = 0^\circ$ | Normal operation | 1.35 |
| 1.5 | EWS $V_{in} < V_{hub} < V_{out}$ | NSS $H_s = E[H_s | V_{hub}]$ | $\beta = 0^\circ$ | Normal operation | 1.35 |
| 1.6a | NTM $V_{in} < V_{hub} < V_{out}$ | ESS $H_s = 1.09 \cdot H_{180}$ | $\beta = 0^\circ$ | Normal operation | 1.35 |
| 2.x Power production plus occurrence of fault | | | | |
| 2.1 | NTM $V_{in} < V_{hub} < V_{out}$ | NSS $H_s = E[H_s | V_{hub}]$ | $\beta = 0^\circ$ | Pitch runaway | 1.35 |
| 2.3 | EOG $V_{hub} = V_r \pm 2 \frac{m}{s}$ | NSS $H_s = E[H_s | V_{hub}]$ | $\beta = 0^\circ$ | Loss of load | 1.1 |
| 6.x Parked (standing still or idling) | | | | |
| 6.1a | EWM $V_{hub} - 0.95 \cdot V_1$ | ESS $H_s = 1.09 \cdot H_{180}$ | $\beta = 0^\circ, \pm 30^\circ$ | Yaw = 0°, ± 30° | 1.35 |
| 6.2a | EWM $V_{hub} - 0.95 \cdot V_1$ | ESS $H_s = 1.09 \cdot H_{180}$ | $\beta = 0^\circ, \pm 30^\circ$ | Yaw = 0°, ± 30° | 1.1 |
| 6.3a | EWM $V_{hub} - 0.95 \cdot V_1$ | ESS $H_s = 1.09 \cdot H_{180}$ | $\beta = 0^\circ, \pm 30^\circ$ | Yaw = 0°, ± 20° | 1.35 |
| 7.x Parked and fault condition | | | | |
| 7.1a | EWM $V_{hub} - 0.95 \cdot V_1$ | ESS $H_s = 1.09 \cdot H_{180}$ | $\beta = 0^\circ, \pm 30^\circ$ | Yaw = 0°, ± 30° 1 seized blade | 1.1 |

*not calculated with reduced model, only with FAST

With the reduced model the complete set of DLCs was simulated, but due to the current simplifications of particularly the aerodynamics, certain load cases cannot be represented well. The following list provides the main limitations for certain load cases:

- **DLC 1.4**: Here an extreme gust with direction change ECD is to be calculated. In the reduced model this only can be represented by changing the effective rotor wind speed according to the wind direction; therefore excitation off the wind inflow direction is limited.

- **DLC 1.5**: All cases with extreme wind shear EWS cannot be represented at the current stage using the effective wind speed. This feature will be added in a subsequent version by a method based on the relationship of wind shear and rotor yaw and tilt moments derived from a linearized aeroelastic model with subsequent application of the Coleman transformation [11].
• DLC 2.1: Since the blades are not represented individually, the pitch runaway of one blade was not modeled, but all blades were collectively pitched to feather for shutdown 0.2 s after the pitch runaway event.

• DLC 6.1a: For yaw misalignment, the same limitation as for DLC 1.4 is present.

• DLC 6.2a: Due to the occurrence of yaw misalignments up to 180° and the model currently unable to accurately represent these, this case could not be calculated satisfactorily and led to unpredictable results. Thus 6.2a was not completed.

• 7.1a: Here in FAST one blade is seized and the others at 90° pitch angle – due to the previously mentioned limitations in the reduced model all blades are feathered. For single blade failures a potential solution based on linearized relationships similar to what is applied for shear is conceivable as further extension of the model.

4. Comparison of Results
This section presents results from the above described IEC DLC simulations. Initially, time series from selected single simulation runs are compared in the time-domain to provide a first indication of the quality and the differences in results. Next, selected timeseries are compared in frequency-domain in terms of PSDs; for brevity these results are not shown. Finally, an IEC-style extreme and fatigue analysis comparison is performed, evaluating the suitability of the presented approach for preliminary loads analysis.

Each simulation was run on a regular state-of-the-art personal computer. The authors are aware of the difficulties to compare absolute computational times, but to provide an indication of the speed advantage of the presented model, Table 2 provides an overview of the computational times of characteristic load simulations performed during this work.

| Real-time Ratio $T_{real}/T_{Sim}$ | FAST | Reduced nonlinear model |
|-----------------------------------|------|-------------------------|
| $T[s]$ for 198 DLC1.1 Simulations | 12-17 hours | 20-25 min |
| $T[s]$ for Table 1 IEC subset     | 12-18 days  | 9-11 hours              |

Due to the limitations of the model at the current stage and for conciseness of this paper, the focus of the presented analysis is on production load cases. Only a short summary of findings for the failure and parked conditions is provided. With FAST as well as with the reduced model a few DLC simulations from DLC 6.1a and 7.1a failed to converge with the simulation finally crashing – interestingly, regardless of their physical reasons, the crashes occurred for the same simulations, indicating that the potential resonances leading to extreme FOWT behaviour and final non-convergence of the simulations is represented consistently in both models.

4.1. Time-Series Comparison
Comparisons in the time-domain have been performed for all investigated DLCs. In Figure 2, sample timeseries results for DLC 1.1 at rated 12 m/s mean wind speed from FAST and the reduced nonlinear model for wave elevation, wind speed $v_0$, pitch angle $\theta$, rotor speed $\Omega$ and tower base bending moment $M_{YT}$ are presented. The case at rated wind speed is selected here, because it usually is most demanding for reduced models, because due to the dynamic switching between different control regions the model behaves very nonlinear.

However, Figure 2 demonstrates that the reduced nonlinear model yields results in the time domain with good agreement to the FAST model. A close investigation of the tower bending moment $M_{YT}$ in the lower diagram indicates, that the model is able to accurately represent lower frequency behaviour, but fails to resolve the high frequency oscillations present in the FAST model. Yet, this behaviour was to be expected and is mainly a result of the missing tower dam effect in the reduced model introducing a 3P excitation and also not modelling of rotational sampling.
This conclusion is confirmed when investigating the coherence of the simulation between the FAST and the reduced model, not shown for brevity here: For pitch angle, rotor speed and tower base bending moment at rated wind speed, a high coherence is achieved up to about 0.1 - 0.2 Hz, which is well below the 3P frequency of about 0.6 Hz.

4.2. Extreme and fatigue load comparison
An extreme loads analysis was performed on all simulated load cases. Figure 3 presents a comparison of extreme loads in DLC1.1 for the total tower base bending moment $M_{yT}$, which was selected as representative global structural load. The graphs show the mean load (line), standard deviation (inner error bars) and minimum and maximum load (outer error bars) over the different wind speed bins (4-24m/s, 2m/s bin width). Evidently the agreement of the mean loads is quite well, while the standard deviations also show acceptable differences. The values most interesting for the design engineer in terms of certification, the maximum loads, are also close, with FAST consistently giving slightly higher loadings. More importantly than matching the exact extreme value, which in any case will have to be evaluated with a fully coupled design tool, is that the trend for both models is very similar: Maximum loads occur at the same wind speed range and the load case where the global maximum occurs is also identical. Other DLCs show similar good agreement.

![DLC1.1 timeseries comparison of FAST simulation and reduced model for wave elevation, wind speed $v_0$, pitch angle $\theta$, rotor speed $\Omega$ and tower base bending moment $M_{yT}$ at rated wind speed.](image1)

Figure 2. DLC1.1 timeseries comparison of FAST simulation and reduced model for wave elevation, wind speed $v_0$, pitch angle $\theta$, rotor speed $\Omega$ and tower base bending moment $M_{yT}$ at rated wind speed.

Figure 4 provides an overview of the relative differences between the FAST model and the reduced model for selected extreme loads and displacements from production load cases 1.x. The bars show relative differences of generator power (GenPwr), generator torque (GenTq), tower top fore-aft and side-to-side displacement (TTDspFA-SS), floating platform surge and pitch (PtfmSurge-Pitch), and the tower base bending moments in fore-aft and side-to side direction, as well as the global moment (TwrBsMxt10, TwrBsMyt10, TwrBsMMxy1). The ratios all show values above zero, indicating that the FAST model predicts higher extreme loads. Most of the extreme loads occur for DLC1.6a, which is similarly predicted by both models. With the exception of platform surge and tower side to side...
displacement, the results are within 5% - 7%, which represents a good agreement given the differences between the models and the large amount of stochastic simulations (1362 single DLC 1.x runs) this comparison is based on. Even for loads and displacements in side-to-side direction, the results are close. Signals not shown here, such as platform yaw or tower twist cannot be accurately predicted by the reduced model, as well as the loads on blades which are not calculated individually. Nevertheless the result confirms that the presented model is well suited for preliminary design studies and to optimize controllers, where the designer usually is not interested in detailed component loads.

![Graph of total tower base bending moment over wind bins]

**Figure 3.** DLC1.1 Extreme load of total tower base bending moment over wind bins

The fatigue analysis was performed for DLC1.1, for single simulation runs only and for the complete set of DLC1.1 simulations. For the exemplary timeseries in Figure 2, the fatigue damage equivalent loads and associated relative differences for N=2E6 reference cycles and Wöhler exponent m=4 are presented in Table 3 for $M_{yT}$. Also this table presents the lifetime $M_{yT}$ DEL for DLC1.1 with Weibull (A=12, k=2) weighting of each wind speed bin.

For the single simulation the DEL difference yields 10%, while the full DLC 1.1 DEL depicts a difference of 25%. The difference of 10% and 25% between a single sample and the global DEL over 198 simulations is within expected statistical variations. Comparing the 25% $M_{yT}$ DEL difference with the 5% differences in extreme loads and standard deviations in Figure 3 and Figure 4, it is evident that the already mentioned missing of higher frequency load cycles in the reduced model, e.g. caused by 3P excitations, has larger impact on DELs than extreme loads or standard deviations. The reason is that in the used rainflow counting algorithm to compute cycle counts and ranges for the DELs, the higher frequency and small amplitude cycles contribute significantly to the overall DEL.

![Graph of production load cases extreme value differences]

**Figure 4.** Production load cases extreme value differences ([FAST-RM]/[FAST])

| Signal      | Case                  | FAST   | Reduced model | Rel. difference |
|-------------|-----------------------|--------|---------------|----------------|----------------|
| DEL of $M_{yT}$ | timeseries (Figure 2) | 134.8 MNm | 121.4 MNm     | 9.9%           |
| DEL of $M_{yT}$ | DLC1.1                | 140.0 MNm | 105.2 MNm     | 25%            |

**5. Conclusion and Outlook**

A method was presented to use a reduced nonlinear FOWT model to identify critical and less critical IEC DLC situations. Results from the reduced model have been compared to a higher fidelity model in FAST. Results in frequency and time domain as well as extreme and fatigue load predictions demonstrate that good agreement between the reduced and advanced model is achieved, allowing to
efficiently exclude less critical DLC simulations, and to identify the most critical subset of cases for a given design.

Further development of the reduced model to include skewed inflow, better represent the rotor and other improvements in the simplified hydro- and aerodynamic model are required to enhance the quality of the results. With these improvements, side-to-side loads and deflections can be predicted more accurately, as well as more detailed rotor loads. Yet, based on the presented results, the following efficient 3-step approach for a FOWT DLC analysis is already possible:

- Run reduced nonlinear model simulations during design iterations, controller design and optimization and for initial DLC analysis identifying the significant DLCs for extreme and fatigue loads.
- Run full IEC DLCs of the pre-design with traditional design code to obtain standard-conform extreme and fatigue loads and to identify critical DLCs.
- Run small subset of identified single critical IEC DLC simulations with advanced design tools. Models featuring e.g. free wake free vortex lifting line aerodynamic theory could be used to investigate particular extreme load and deflection predictions for blades, or codes featuring 2nd order hydrodynamic theory and mooring line implementations may be used for detailed mooring line and platform loads and displacement analysis.

While further research is necessary to improve the reduced model results and also study other FOWT concepts, following this approach could potentially significantly reduce the computational cost in the initial design phase, while application of advanced tools in the final design phase should help achieving confidence in the final design and reducing the economic risk associated with a new FOWT design.

Acknowledgements
The content of this paper is part of research effort related to IEC TC88 workgroup PT61400-3-2.

References
[1] D. Matha, A. Cordle, J Jonkman, R. Pereira, and M. Schlipf, "Challenges in Simulation of Aerodynamics, Hydrodynamics, and Mooring-Line Dynamics of Floating Offshore Wind Turbines," Maui, Hawaii, USA, 2011.
[2] M. Karimirad and T Moan, "A simplified method for coupled analysis of floating offshore wind turbines," Journal of Marine Structures, 2012, accepted for publication.
[3] Frank Sandner, David Schlipf, Denis Matha, Robert Seifried, and Po Wen Cheng, "Reduced Nonlinear Model of Spar-Mounted Floating Wind Turbine," DEWEK 2012, Bremen, 2012, accepted for oral presentation.
[4] D. Schlipf, D.J. Schlipf, and M Kühn, "Nonlinear Model Predictive Control of Wind Turbines Using LIDAR," Wind Energy Journal, 2012.
[5] Andrew Cordle and Jason Jonkman, "State-of-the-art in Floating Wind Turbine Design Tools," GL Garrad Hassan, ISOPE, 2011.
[6] Jason Jonkman and Marshall Buhl, "FAST User’s Guide," NREL/TP-500-38230. Golden, US-CO, October 2005.
[7] Jason Jonkman, "Dynamics Modeling and Loads Analysis of an Offshore Floating Wind Turbine," NREL/TP-500-41958. Golden, US-CO, 2007.
[8] Denis Matha, Thorsten Lutz, Fabian Wendt, Manuel Werner, and Po Wen Cheng, "Aerodynamic Inflow Conditions on Floating Offshore Wind Turbine Blades for Airfoil Design Purposes," ISOPE 2012, 2012.
[9] Østergaard and Schellin, "Comparison of experimental and theoretical wave actions on floating compliant offshore structures," in Applied Ocean Research 9, 1987, pp. 192-213.
[10] Jonkman, "Definition of the Floating System for Phase IV of OC3," 2009.
[11] S. Schuler, Schlipf D., M. Kühn, and F. Allgöwer, "II-Optimal Multivariable Pitch Control for Load Reduction on Large Wind Turbines," EWEC, Warsaw, Poland, 2010.