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The influence of different mooring line models on the stochastic dynamic responses of floating wind turbines

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Abstract. Mooring line modelling plays an important role in predicting the dynamic response of the floating offshore wind turbines (FOWTs), especially under extreme load conditions. This paper investigates the influence of different mooring line models on the stochastic dynamic responses of a spar-type FOWT. A 16-degree-of-freedom (16-DOF) aero-hydro-servo-elastic model for the spar-type FOWT is first established using Euler-Lagrangian approach, taking into consideration the full coupling of the blade-drivetrain-tower-spar vibrations, a collective pitch controller and a generator controller. Three different mooring line models have been established and incorporated into the 16-DOF model, namely the linear spring model, the quasi-static model and the lumped-mass model, the last of which include the hydrodynamic loads, inertial force and damping force of the mooring cable. Stochastic dynamic analysis of the coupled FOWT-mooring line system is carried out using 3 different turbulent wind conditions and 4 different sea states and a total of 2160 10-min simulations. The mean value, the standard deviation and the extrapolated extreme value of the structural responses (blades, tower and mooring cable) as well as the fatigue equivalent loads are compared for the three different mooring line models.

1. Introduction
When designing floating offshore wind turbines (FOWTs), the load calculation of the structural components requires time-domain simulations of the fully coupled system, taking into consideration the structural dynamics, aerodynamics, hydrodynamics and controller actions. The mooring lines of a FOWT connect the floating platform with the seabed, holding the device in the desired location and influence the global dynamics of the FOWT system. For calculating the mooring loads on the floating platform due to the platform movements, three different methods can be used, i.e. linear stiffness method, quasi-static method and nonlinear dynamic method. These mooring line models will have different influence on the loads and responses of the FOWT, and the influence will be significantly different under different environmental conditions. Most design codes use the computationally efficient quasi-static mooring model, but this model ignores the dynamic effects of the mooring system. The more computationally-heavy lumped mass model also requires a more sophisticated model and many more details of the mooring system to set up the model. The simplicity of the simple models also enables more easy communication between the foundation designer and turbine designer e.g. through lookup tables with the non-linear force displacement relationship of the mooring system.

A comprehensive assessment of the effect of mooring line models on the stochastic dynamic responses of the FOWT is needed to evaluate the possible benefit of a more detailed mooring system model might bring. Some studies have been carried out on the influence of the mooring dynamics on the loads of floating turbines [1,2], but a comprehensive evaluation on the stochastic responses (in terms
of mean value, standard deviation, fatigue loads and extreme responses) of the FOWT under different wind and wave conditions including extreme waves is sparse. The purpose of the present paper is two-fold: 1) to present a newly developed 16-DOF aero-hydro-servo-elastic model of the FOWT, which is the basis for evaluating different mooring line models when fully-coupled simulation is carried out, and also for investigating structural control of FOWT in the future research. 2) To investigate the influence of different mooring line models on the stochastic dynamic responses of a FOWT. Different combinations of the stochastic wind and wave conditions are to be considered with large number of random seeds. The focus is on how different mooring line models will influence the obtained stochastic responses of the FOWT components and the mooring cables, which are very important for the reliability-based design of FOWTs.

2. 16-DOF aero-hydro-servo-elastic model of the FOWT

As a basis for evaluating different mooring line models using fully-coupled time domain simulations, we developed a 16-DOF reduced-order aero-hydro-servo-elastic model for the FOWT system. This model takes into consideration the full coupling of blade-drivetrain-tower-spar vibration, nonlinear aeroelasticity and nonlinear wave-spar interaction, as well as pitch and generator controllers. Figure 1 shows the degrees of freedom (DOFs) of the 16-DOF aero-hydro-servo-elastic FOWT model, and the three different coordinate systems for describing different DOFs of the system.

The blades and tower are modelled as Euler-Bernoulli beams. Each blade is related with two DOFs $q_j(t)$ and $q_{j+3}(t)$, $j = 1,2,3$, indicating the flap-wise and edgewise tip displacements, respectively. The tower motions are defined by the translational DOFs $q_7(t)$ and $q_8(t)$, indicating the fore-aft and side-side vibrations, respectively. The drivetrain is modelled by DOFs $q_9(t)$ and $q_{10}(t)$ using St. Venant torsional theory. The azimuthal angle of each of the three blades is described as:

![Figure 1: Definition of DOFs of the 16-DOF FOWT model.](image-url)
\[
\Psi_j(t) = \Omega t + q_9(t) + (j - 1) \frac{2\pi}{3}, \quad j = 1, 2, 3
\]  

(1)

The spar is modeled by 6 rigid DOFs, \(q_{11}(t), q_{12}(t), q_{13}(t), q_{14}(t), q_{15}(t)\) and \(q_{16}(t)\), specifying surge, sway, heave, roll, pitch and yaw motions of the spar, respectively. Multi-body based formulation has been carried out for the total kinetic energy and total potential energy of the system, and equations of motion of the FOWT are derived using the Euler-Lagrange equation:

\[
\frac{d}{dt}(\frac{\partial T}{\partial \dot{q}}) - \frac{\partial T}{\partial q} + \frac{\partial U}{\partial \dot{q}} = f_a + f_n + f_g + f_m + f_{gen} - C_s \dot{q}
\]  

(2)

where \(T\) and \(U\) are the kinetic and potential energies of the system, \(\dot{q}\) and \(\ddot{q}\) are the position and velocity vectors of the system DOFs, \(f_a, f_n, f_g, f_m\) and \(f_{gen}\) are the aerodynamic loads, hydrodynamic loads, gravity loads, mooring system loads and generator torque, respectively. \(C_s\) is the structural damping matrix. The kinematic energy of the system is defined as:

\[
T(q(t), \dot{q}(t)) = \frac{1}{2} \sum_{j=1}^{11} \int_0^L \left( \mu_B \dot{\nu}_B^T \dot{\nu}_B + \mu_T \dot{\nu}_T^T \dot{\nu}_T + \frac{1}{2} \mu_3 \dot{\nu}_3^T \dot{\nu}_3 + \frac{1}{2} \mu_3^2 \dot{\psi}_3^T \dot{\psi}_3 + \frac{1}{2} J_3 (\dot{\psi}_3^2 + \dot{\psi}_3 \dot{\psi}_3^* + \frac{1}{2} J_5 (\dot{\psi}_5^2 + \dot{\psi}_5 \dot{\psi}_5^*) + \frac{1}{2} (J_{3Y} + J_{3N}) \dot{\psi}_3^2 + \frac{1}{2} (J_{3Y} + J_{3N}) \dot{\psi}_3 \dot{\psi}_3^*)
\]  

(3)

Table 1: Definitions of distributed mass, component mass and component mass moment of inertia.

| \(J_G\) | Generator mass moment of inertia | \(M_G\) | Hub mass |
| \(J_H\) | Hub mass moment of inertia | \(M_H\) | Nacelle mass |
| \(J_{NY}\) | Nacelle mass moment of inertia about yaw axis | \(M_{NY}\) | Spar mass |
| \(J_S\) | Spar mass moment of inertia about pitch and roll axis | \(\mu_B(x_3)\) | Blade mass per unit length |
| \(J_{SV}\) | Spar mass moment of inertia about yaw axis | \(\mu_T(x_3')\) | Tower mass per unit length |

The tower velocity as a function of the tower height is given by:

\[
\nu_T(x_3', \dot{q}(t), t) = (\varphi_{TF} \varphi_{TF} q_7 + \dot{q}_{11} + (h_T + X_3') q_{15}) i + (\varphi_{TS} \dot{q}_8 + \dot{q}_{12} + (h_T + X_3') \dot{q}_{14}) j + \dot{q}_{13} k
\]  

(4)

where \(\varphi_{TF}(x_3')\) and \(\varphi_{TS}(x_3')\) are the first mode shapes of the tower fore-aft and side-side directions, respectively. \(i, j\) and \(k\) are the unit vectors describing the orientation of the global fixed coordinate system. The nacelle velocity is given by:

\[
\nu_N(\dot{q}(t), t) = (\dot{q}_7 + \dot{q}_{11} + h_R \dot{q}_{15}) i + (\dot{q}_8 + \dot{q}_{12} + h_R \dot{q}_{14}) j + \dot{q}_{13} k
\]  

(5)

The blade velocity as a function of the blade length is given by:

\[
\nu_B(x_3, q(t), \dot{q}(t), t) = \left( \dot{q}_7 + \dot{q}_{11} + h_R \dot{q}_{15} + (\dot{q}_{15} \cos \psi_j + \dot{\psi}_j x_3 + \varphi_{BE} \dot{q}_j) \right) i''(t) + \left( \dot{q}_8 + \dot{q}_{12} + h_R \dot{q}_{14} + (\dot{q}_{13} + \dot{q}_5) \sin \psi_j - (\Omega + \varphi) x_3 \right) j''(t) + \left( \varphi_{BE} \dot{q}_j + \dot{q}_{13} - h_R \dot{q}_{14} \right) k''(t)
\]  

(6)

where \(\varphi_{BE}(x_3)\) are the blade flap-wise and edgewise mode shapes, and \(i''(t), j''(t)\) and \(k''(t)\) are the unit vectors describing the local moving coordinate system of the blades. The total potential energy \(U\) of the system is defined by the structural stiffness matrix \(K_s\):

\[
U(\dot{q}(t)) = \frac{1}{2} q^T(t) K_s q(t)
\]  

(7)

where \(K_s\) includes centrifugal stiffening in the rotating blades.

The aerodynamic loads \(f_a(\dot{q}(t), \beta(t), t)\) are modelled using the modified blade element momentum (BEM) theory, and are a function of the system velocity \(\dot{q}(t)\) as well as the collective
blade pitch angle $\beta(t)$. The hydrodynamic loads $f_h(q(t), \dot{q}(t), \ddot{q}(t), t)$ are modelled using Morrison’s equation. The irregular waves are modelled using linear wave theory and the Joint North Sea Wave Project (JONSWAP) spectrum. Using Eq. (2), the equations of motion of the FOWT become:

$$M(t) \ddot{q}(t) + C(t) \dot{q}(t) + K(t)q(t) = f_a + f_h + f_g + f_m + f_{\text{gen}}$$  \hspace{1cm} (8)

Further, a collective pitch controller is implemented by a gain-scheduled PI controller keeping the rpm the rotor at the rated value, and the delay of the pitch actuator is modelled by a 1st order filter. The generator controller is implemented by a torque-rpm relationship in five different control regions.

Eq. (9) is combined with the filter equation of the pitch controller, and solved by a 4th order Runge-Kutta time integration scheme. The simulated steady state responses of this 16-DOF model are compared with that from FAST [9] and [3], for wind speeds from cut-in (3 m/s) to cut-out (25 m/s). Good agreement has been obtained for all responses, and the results of some DOFs are shown in Figure 2.

![Figure 2: Comparison of steady state responses between FAST and the 16-DOF model at different wind speeds.](image)

Nonlinear time domain simulations subjected to turbulent winds and irregular waves have also been carried out, which in general shows good agreement as well. As an example, the flapwise blade tip response of blade 1 is compared in Figure 3, where the stochastic load inputs to the 16-DOF model and FAST are not identical, but with same intensity levels. The mean and standard deviations are given in the left plot of Figure 3 and the right plot shows the FFT of the response. It should also be noted that the modelling of the rotational sampled turbulence is also different in the 16-DOF model.

![Figure 3: Comparison of dynamic responses between FAST and the 16-DOF model at 15 m/s turbulent wind ($t_{\text{ref}} = 0.10$) and irregular waves ($H_s = 6.0 \text{ m}, T_p = 10 \text{ s}$).](image)
and FAST, where we have used an AR model following the procedure in [4]. Nevertheless, the acceptable agreement of the time domain simulation results further verifies the 16-DOF, which is to be coupled to different mooring line models in the following sections.

3. Different mooring line models

As shown in Figure 4a), the simplest mooring line model used is a linear spring model linearized around a given spar location and orientation using basic catenary equations for solving the line forces. The quasi-static model [5] expands the linearized model to update the line forces for every given position and orientation of the spar, to obtain the non-linear force-displacement relationship. Neither the linear spring nor quasi-static model takes the cable dynamics and hydrodynamics into account.

![Figure 4: Schematic representation of the mooring line models. a) linear spring and quasi-static, b) lumped mass.](image)

The lumped mass model [6] of the mooring cable, sketched in Figure 4b), takes the full cable dynamics and hydrodynamics into account as well as seabed interactions by a vertical spring-damper system. The cable is split into \( N_e \) elements and \( N_e + 1 \) nodes from the anchor to the floating platform. The hydrodynamics on the cable is calculated using Morison’s equation as well.

![Figure 5: Free body diagram of the mooring line lumped masses and the forces on them.](image)

As illustrated in Figure 5, the equations of motion for one lumped mass are written as:

\[
[M_i + M_{a,i}] \ddot{r}_i = t_i - t_{i-1} + c_i - c_{i-1} + w_i + b_i + d_{t,i} + d_{n,i}
\]

where \( M_i \) and \( M_{a,i} \) are the mass and hydrodynamic added mass of the cable. \( t_i, t_{i-1}, c_i \) and \( c_{i-1} \) are the structural tension and damping in the cable on either side of the lumped mass. \( w_i \) is the gravitational force on the cable minus the buoyancy of the cable in water. \( b_i \) is the seabed interaction with both vertical stiffness and damping as well as horizontal friction. \( d_{t,i} \) and \( d_{n,i} \) are the tangential and normal hydrodynamic drag on the cable. The position, velocity and acceleration vectors of the lumped masses are defined as \( r_i, \dot{r}_i \) and \( \ddot{r}_i \) respectively. The system is solved by a 2\textsuperscript{nd} order Runge-
The Kutta scheme where the time step in the FOWT model has been divided by 5 in order to ensure numerical stability of the RK2 solver. The forces acting on the floating platform is solved by the equations of motion of the last lumped mass and transformed from the local cable coordinate system to the global coordinate system and finally forming the loads on the platform $f_m$.

To validate the quasi-static and lumped mass models, a comparison with the experimental results in [7] has been made. Figure 6 illustrates the fairlead tensions due to harmonic fairlead motions with different periods, as well as the quasi-static results. All results from the present study agree quite well with that from [7], and deviations of the maximum tensions are within 3%. The largest deviation is observed at the left side of the hysteresis loop for high frequency fairlead excitation (1.58s), where snap loads occur.

![Figure 6: Dynamic fairlead tension due to harmonic fairlead displacements with time periods of 1.58 s, 3.16 s, 4.74 s. a) the lumped mass model presented in this work, b) experimental results from [5].](image)

4. **Stochastic dynamic responses from the fully-coupled analysis**

Coupling the 16-DOF FOWT model with the mooring line models, extensive simulations have been carried out in order to evaluate the influence of the different mooring line models on the stochastic responses of structural components of the FOWT.

4.1. **Response time-series**

First, the free decay (no external wind and wave loads) of the spar surge and pitch motions are compared using the quasi-static and lumped mass models, as shown in Figure 7. It is seen that the free decay of the surge motion has a higher damping when using the lumped mass model because this model takes the hydrodynamic drag on the mooring cables into account. On the other hand, the free decay of the spar pitch is virtually unchanged because the mooring system is attached close to the rotation centre of the FOWT.

![Figure 7: Comparison of free decay of the FOWT in still water and with aerodynamics turned off, using the quasi-static (QS) and lumped mass (LM) mooring line models. a) spar surge motion, b) spar pitch motion.](image)
Next, the impact of the different mooring line models on the stochastic dynamic response of the FOWT and mooring cables is investigated by simulations with different wind and wave conditions. The wind and wave conditions used in the present study are shown in Table 2.

Table 2: Wind and wave conditions used for stochastic dynamic analysis of the fully-coupled system

| External load | Abbreviation | Description                  |
|---------------|--------------|------------------------------|
| Wind          | -            | \( V_0 = 9 \text{ m/s} \)   |
|               | -            | \( I_{ref} = 0.12 \)        |
|               | -            | \( V_0 = 12 \text{ m/s} \)  |
|               | -            | \( I_{ref} = 0.12 \)        |
|               | -            | \( V_0 = 15 \text{ m/s} \)  |
|               | -            | \( I_{ref} = 0.12 \)        |
| Wave          | Low          | \( H_s = 2 \text{ m} \)     |
|               | Med          | \( H_s = 6 \text{ m} \)     |
|               | High         | \( H_s = 10 \text{ m} \)    |
|               | Ext          | \( H_s = 15.24 \text{ m} \) |
|               |              | \( T_p = 5.5 \text{ s} \)   |
|               |              | \( \beta = -30^\circ, 0^\circ, 30^\circ \) |
|               |              | \( T_p = 10 \text{ s} \)    |
|               |              | \( \beta = -30^\circ, 0^\circ, 30^\circ \) |
|               |              | \( T_p = 12.5 \text{ s} \)  |
|               |              | \( \beta = -30^\circ, 0^\circ, 30^\circ \) |
|               |              | \( T_p = 17 \text{ s} \)    |
|               |              | \( \beta = -30^\circ, 0^\circ, 30^\circ \) |

where \( V_0 \) is the mean wind speed, \( I_{ref} \) is the turbulence intensity, \( H_s \) is the significant wave height, \( T_p \) is the peak wave period, and \( \beta \) is the angle between the wind and wave directions.

It has been observed that the mooring line models have insignificant influence on the blades, tower and spar responses (time-series not shown here), and this observation will be elaborated further in terms of fatigue loads and extrapolated extreme responses. The reason for the insignificant influence is that the mooring system is attached very close to the rotation centre of this specific spar-type FOWT, and the thus cable dynamics hardly influences structural responses of the wind turbine components.

However, cable tensions are highly influenced by the cable dynamics. One simulation result (under extreme wave conditions) of the fairlead cable tension is presented in Figure 8, for both mooring line 2 (waves acting perpendicularly) and mooring line 3. Figure 8a) shows small deviations in the fairlead tensions of mooring line 2 when using the three mooring line models. This is because the cable is not dynamically excited when the wave loads are acting perpendicular to the mooring line. On the other hand, Figure 8b) shows a significant difference in the fairlead tensions of mooring line 3 when using the lumped mass model which takes into consideration of cable dynamics, compared to the linear spring and quasi-static models. In this case, using the linear spring or quasi-static models will lead to non-conservative results for the mooring line design. It should also be mentioned that the simulation time when using the lumped mass model is about 6-7 times higher than the quasi-static model.

![Figure 8: Cable tensions at the fairleads compared for the three mooring line models: Linear spring (Lin), quasi-static (QS) and lumped mass (LM). The fully-coupled models are subjected to the same 15 m/s turbulent wind (\( I_{ref} = 0.12 \)) and irregular waves (\( H_s = 15.24 \text{ m}, T_p = 17 \text{ s}, \beta = 30^\circ \)). a) Mooring line 2 (waves acting perpendicularly), b) Mooring line 3.](image)
4.2. Fatigue equivalent loads

To evaluate the influence of mooring dynamics on the fatigue loads of the turbine components and mooring cables, the fatigue equivalent loads are calculated according to [8], for the blade root flap-wise moment, tower base fore-aft moment and fairlead tension of mooring cable 3. The fatigue equivalent loads are based on $10^7$ cycles and for the steel components (tower and mooring cables) a S-N slope of 4 is used whereas a slope of 9 is used for the blades. All load combinations in Table 2 have been considered, and only the comparisons for the cases of aligned wind and waves ($\beta = 0^\circ$) are shown here. Figure 9 shows the fatigue equivalent loads from the 20 seeds of each wind-wave combination ($\beta = 0^\circ$). The turbine components (blades, tower) loads are well within 1% when using the linear spring, quasi-static and lumped mass mooring line models, as illustrated in Figure 9a) and b). This again shows that the responses of the turbine components are hardly influenced by different mooring line models for this specific spar-type FOWT, even under the extreme wave conditions. On the other hand, the fatigue loads of mooring cables (Figure 9c) are highly impacted by the dynamics from the lumped mass model, where the lumped mass model predicts up to 68% higher fatigue equivalent loads comparing with the other two models, under the extreme wave condition.

![Fatigue equivalent loads](image)

Figure 9: Fatigue equivalent loads, a) blade root flap-wise moment, b) tower base fore-aft moment, c) cable tension at fairlead 3.

4.3. Extrapolated extreme responses

The extreme loads with a 50-year return period of the blades, tower and mooring cables are estimated by extreme value extrapolations, for the different load combinations in Table 2 according the IEC 61400-1 guideline. For the extrapolations, a Gumbel distribution is used to fit the data. The data used for extrapolations are selected based on 1-minute block maxima, so as to have more data points for fitting and extrapolating from a limited number of time series (20 for each load combination). Figures 10, 11 and 12 show the extrapolated extreme responses of the blade root flap-wise moment, tower base fore-aft moment and mooring cable 3 tension, respectively.

The differences in the extreme responses of the blades and tower are generally within 1% when comparing the linear spring model with the quasi-static model. The differences in the extreme
responses of the blade and tower are within 2% when comparing the lumped mass model with the quasi-static model, except for one extrapolation of the blade root flap-wise moment at 15 m/s and low waves. This deviation can be due to the statistical uncertainty and is not considered a general tendency. Therefore, it once again shows that whether nonlinear cable dynamics is considered or not in the mooring line model does not impact the responses of the turbine components for this FOWT, even in terms of the extrapolated extreme responses. On the other hand, when using the lumped mass model, the extrapolated extreme responses of cable tensions are more than 9% higher compared to that from the quasi-static model. Moreover, the extreme cable tensions are up to 13% lower when using the linear spring model compared to the quasi-static model. Therefore, mooring line models do significantly impact the extreme responses of the mooring cable.

Figure 10: Extreme value extrapolation of blade root flap-wise moment, a) extrapolation of responses for 15 m/s and extreme waves, b) overview of all extrapolations

Figure 11: Extreme value extrapolation of tower base fore-aft moment, a) extrapolation of responses for 15 m/s and extreme waves, b) overview of all extrapolations

Figure 12: Extreme value extrapolation of cable tension at fairlead 3, a) extrapolation of responses for 15 m/s and extreme waves, b) overview of all extrapolations
5. Conclusions

A 16-DOF aero-hydro-servo-elastic model is established for the spar-type FOWT, taking into account the full coupling of the blade-drivetrain-tower-spar vibrations, a collective pitch controller and a generator controller, and nonlinear aerelasticity and hydrodynamics. The simulation results from this 16-DOF model agree very well with that from FAST, and the model is considered a good basis for evaluating different mooring line models when fully-coupled simulations are carried out. Three different mooring line models (namely the linear spring, quasi-static and lumped mass models) are presented and validated, and are then coupled to the 16-DOF model.

Fully-coupled simulations have been carried out to evaluate the influence of different mooring line models on the stochastic responses of the FOWT components and the mooring cables, under different turbulent wind and irregular wave conditions. In total 2160 numbers of 10-min simulations have been carried out. It is found that the blades and tower responses are little affected by the mooring cable dynamics for this specific spar-type FOWT. The differences are within 2% when using the linear spring model or quasi-static model compared to the dynamic lumped mass model, in terms of the mean values, standard deviations, fatigue equivalent loads and the extreme responses. However, the mooring cable tensions are seen to be increased significantly when using the lumped mass model compared with the quasi-static model, by up to 68% for the fatigue loads and 9% for the extreme loads. The quasi-static model is therefore considered acceptable for estimating the blades and tower loads, but highly unconservative for the cable tensions especially under extreme wave conditions. A dynamic mooring line model, such as the lumped mass model, is needed for carrying stochastic dynamic analysis and reliability-based design of mooring cables.

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