Short-crestedness effect on the dynamic response of offshore floating wind turbines

Liang Li, Yuanchuan Liu

Previous investigations on the performance of offshore floating wind turbines mostly adopted the long-crested wave model, which propagates along one single direction. In practice, the ocean waves are short-crested and three-dimensional. Unfortunately, the performance of offshore floating wind turbines in short-crested waves is not fully understood. The primary objective of the present research is to form a better understanding of the dynamics of offshore floating wind turbines in multi-directional waves. The short-crested waves are modelled using a spreading exponent function. The wave directionality effect on the dynamic performance of a semisubmersible offshore floating wind turbine is studied. In short-crested wave, the longitudinal response is reduced whereas the lateral response is amplified. The tower base fore-aft bending moment and the mooring line tension force are reduced. In the meanwhile, the fatigue damages of critical points at tower base are also reduced. It is concluded that short-crestedness is beneficial to the structural integrity. The present research reveals the importance of wave short-crestedness effect on the performance of offshore floating wind turbines.

Keywords: offshore floating wind turbine; wave directionality; short-crested wave; spreading component; dynamic response

1. Introduction

Offshore wind is currently promoting the development of sustainable energy. Compared with other ocean renewable energy recourses, the utilization of offshore wind is the most efficient given the present technology. To further exploit the offshore wind resource and minimize the pollution to the local community, it is necessary to go to the deepwater zone and the floating wind turbine is thus proposed.

Since the proposal of the world’s first floating wind turbine – Hywind (Equinor 2017),
an increasing number of researchers are dedicated to the investigation on the offshore floating wind turbines. Although numerical simulations have been conducted intensively for offshore floating wind turbines, most numerical models adopt the long-crested wave assumption (Li et al. 2018, Li et al. 2018, Wan et al. 2017). A long-crested wave is two-dimensional and propagates along one single direction. In most cases, real ocean waves are three-dimensional, known as short-crested waves. The effect of wave directionality has been considered for various forms of offshore structures. Sørum et al. (2019) compared the structural loads of a bottom-fixed offshore wind turbine with varying model properties in long-crested and short-crested waves. They found that wave directionality effects closely related to the properties of the support structures and observed an 80% increase in fatigue damage under the short-crested wave condition but only for the hydrodynamically sensitive design. Viuff et al. (2019) investigated the wave directionality on the extreme responses of a floating bridge consisting of a set of pontoons. Naess (1990) conducted a statistical analysis of the non-linear, second-order hydrodynamic forces and motions of a tension leg platform (TLP) subjected to short-crested waves. Jiao et al. (2019) carried out towing tank tests as well as sea trials for a ship model in various wave conditions and concluded that the pitch motion and sectional loads of the ship could be over-estimated in long-crested waves compared with those in short-crested waves. Jian et al. (2008) analysed the hydrodynamic forces and water run-up on a fixed vertical cylinder in short-crested waves with influences of water current. Beels et al. (2011) considered the wave directionality effect in the power assessment of a wave energy converter farm. Results from the above studies showed that the directionality in short-crested waves can greatly influence the hydrodynamic forces exerted upon offshore structures and consequently their motions if they are afloat. However, to the best of the authors’ knowledge, the wave directionality effect on
the dynamic responses of offshore floating wind turbines has been rarely investigated. Although there have been some studies devoted to the effects of wind-wave misalignment on offshore floating wind turbines (Bachynski et al. 2014, Barj et al. 2014, Han et al. 2017, Karimirad and Michailides 2016, Li et al. 2020), only one misalignment angle could be imposed between wind and long-crested waves, which differs from practical short-crested wave conditions with waves concurrently propagating in a number of directions different from wind.

The primary objective of this paper is to illustrate the wave directionality effect on the dynamic response of offshore floating wind turbines. To take the wave directionality into account, the typical 2-D wave spectrum is modified with a spreading function, which ensures that the overall wave energy is not varied. The dynamic responses of a semisubmersible offshore floating wind turbine in long-crested and short-crested waves are simulated, respectively. Through the comparison between the two types of waves, the wave directionality effect is demonstrated.

2. Methodology

The OC4 DeepCwind semisubmersible concept (Robertson et al. 2014) is considered in this work. The wind turbine is the NREL 5 MW baseline wind turbine (Jonkman et al. 2009). The diameter of the rotor is 126 m, and the hub height is 90 m.

A three-column submersible platform is used to carry the wind turbine (see Fig. 1). The platform is made up of three main offset columns inducing buoyancy and restoring force, one central column supporting the wind turbine, as well as a series of diagonal cross and horizontal bracing components. To gain a good hydrostatic stability performance, a ballast tank is installed at the bottom of each main offset column. The main scantlings of the platform are listed in Table 1. The base columns work in a
similar way with heaving plates to produce additional viscosity to damp the platform. Such phenomenon was modelled by the designers (Robertson et al. 2014) with an additional quadratic damping matrix. This approach is adopted in Section 2.1.1.

![Platform geometry](image)

**Table 1 Main scantlings of the platform.**

<table>
<thead>
<tr>
<th>Term</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Draft</td>
<td>20m</td>
</tr>
<tr>
<td>Elevation of main column top above SWL</td>
<td>10 m</td>
</tr>
<tr>
<td>Elevation of offset columns above SWL</td>
<td>12 m</td>
</tr>
<tr>
<td>Spacing between offset columns</td>
<td>50 m</td>
</tr>
<tr>
<td>Length of upper columns</td>
<td>26 m</td>
</tr>
<tr>
<td>Length of base columns</td>
<td>6 m</td>
</tr>
<tr>
<td>Diameter of main column</td>
<td>6.5 m</td>
</tr>
<tr>
<td>Diameter of offset (upper) columns</td>
<td>12 m</td>
</tr>
<tr>
<td>Diameter of base columns</td>
<td>24 m</td>
</tr>
<tr>
<td>Platform mass</td>
<td>13,473,000 kg</td>
</tr>
<tr>
<td>Displacement</td>
<td>13,986.8 m$^3$</td>
</tr>
<tr>
<td>Centre of mass</td>
<td>(0 m, 0 m, -13.5 m)</td>
</tr>
<tr>
<td>Platform roll inertia</td>
<td>6.827 ×10$^8$ kg·m$^2$</td>
</tr>
<tr>
<td>Platform pitch inertia</td>
<td>6.827 ×10$^9$ kg·m$^2$</td>
</tr>
<tr>
<td>Platform yaw inertia</td>
<td>1.226 ×10$^{10}$ kg·m$^2$</td>
</tr>
</tbody>
</table>

The floating wind turbine is displaced at sea site with a water depth of 200m. The mooring system is composed of three catenary lines. The three mooring lines are oriented symmetrically at 60°, 180°, and 300° about the vertical axis. Fairleads are connected to the tops of ballast tanks. The relevant properties of mooring lines are outlined in Table 2. Please note that a multi-segments catenary mooring line is typically employed in practices whilst this paper models an equivalent mooring line in
accordance with the design proposed by the designers (Robertson et al. 2014).

Table 2 Properties of the mooring line.

<table>
<thead>
<tr>
<th>Term</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Depth to anchor</td>
<td>200 m</td>
</tr>
<tr>
<td>Depth to fairlead</td>
<td>14 m</td>
</tr>
<tr>
<td>Radius to anchor</td>
<td>853.7 m</td>
</tr>
<tr>
<td>Radius to fairlead</td>
<td>40.868 m</td>
</tr>
<tr>
<td>Unstretched mooring line length</td>
<td>835.5 m</td>
</tr>
<tr>
<td>Mooring line diameter</td>
<td>0.0766 m</td>
</tr>
<tr>
<td>Equivalent line mass density</td>
<td>113.35 kg/m</td>
</tr>
<tr>
<td>Equivalent mooring line extensional stiffness</td>
<td>753.6 MN</td>
</tr>
</tbody>
</table>

2.1. Numerical model

A numerical model of the reference floating wind turbine is developed in marine operation analysis software Sima (version 3.7.0), which calls Simo and Riflex to conduct coupled aero-hydro-elastic-servo analysis in time domain (see Fig. 2). The platform is regarded as a rigid hydrodynamic body; the blades and the tower are all modelled as Euler-Bernoulli flexible beams; a rigid nacelle is fixed at the top end of the tower; the mooring line is simulated using the bar element in Sima.

2.1.1. Wave load

The linear theory is used to simulate random wave, in which the instantaneous wave elevation \( \zeta(t) \) is written as the sum of a large number of regular wave components (Faltinsen 1993)
\[ \tilde{\xi}(t) = \text{Re}\left\{ \sum_{j=1}^{N} \sqrt{2S(\omega_j)}\Delta \omega \cdot e^{i(\omega_j t + \epsilon_j)} \right\} \]  
(1)

where \( \omega \) is the angular wave frequency, \( \epsilon \) is the random phase difference, \( S(\omega) \) is the JONSWAP wave spectrum (International Electrotechnical Commission 2009).

Both numerical and experimental studies prove that 2nd order difference-frequency wave force is essential in the hydrodynamic analysis of the OC4 DeepCwind wind turbine (Coulling et al. 2013, Li et al. 2018). Since the natural period of translational mode coincides with the dominating frequencies of the difference-frequency force, significant low-frequency resonant motions will be induced. Therefore, the 1st order linear wave force and the 2nd order difference-frequency force are both modelled in the present simulation.

The linear wave force \( F^{(1)}(t) \) is represented with the linear transfer function \( H^{(1)} \)

\[ F^{(1)}(t) = \text{Re}\left\{ \sum_{j=1}^{N} H^{(1)}_j \cdot \sqrt{2S(\omega_j)}\Delta \omega \cdot e^{i(\omega_j t + \epsilon_j)} \right\} \]  
(2)

The Newman approximation is used to model the 2nd order difference-frequency wave force \( F^{(2)}(t) \)

\[ F^{(2)}(t) = \text{Re}\left\{ u^2(t) + v^2(t) \right\} \]

\[ u(t) = \sum_{j=1}^{N} \sqrt{H^{(2)}_{u_j} \cdot \text{Re}\left( \sqrt{2S(\omega_j)}\Delta \omega \cdot e^{i(\omega_j t + \epsilon_j)} \right)} \]  
(3)

\[ v(t) = \sum_{j=1}^{N} \sqrt{H^{(2)}_{v_j} \cdot \text{Im}\left( \sqrt{2S(\omega_j)}\Delta \omega \cdot e^{i(\omega_j t + \epsilon_j)} \right)} \]

here \( H^{(2)}_{u_j} \) is the second-order quadratic transfer function. Both \( H^{(1)} \) and \( H^{(2)} \) are calculated with boundary element analysis code Wadam (DNV 1994).

The application of potential flow theory is unable to take the viscous effect into account, which exists in the real ocean waves. A quadratic damping matrix is thus added to
represent the viscous drag force $F^{\text{drag}}(t)$

$$F^{\text{drag}}(t) = -B|\dot{x}(t)|\dot{x}(t) \quad (4)$$

$\dot{x}(t)$ is the platform velocity. The damping matrix $B$ take the values recommended by Robertson et al. (2014) (see Table 3).

<table>
<thead>
<tr>
<th>$B_{11}$</th>
<th>$B_{12}$</th>
<th>$B_{13}$</th>
<th>$B_{14}$</th>
<th>$B_{55}$</th>
<th>$B_{66}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>(Ns$^2$/m$^2$)</td>
<td>(Ns$^2$/m$^2$)</td>
<td>(Ns$^2$/m$^2$)</td>
<td>(Nms$^2$/rad$^2$)</td>
<td>(Nms$^2$/rad$^2$)</td>
<td>(Nms$^2$/rad$^2$)</td>
</tr>
<tr>
<td>3.95E+5</td>
<td>3.95E+5</td>
<td>3.88E+6</td>
<td>3.70E+10</td>
<td>3.70E+10</td>
<td>4.08E+9</td>
</tr>
</tbody>
</table>

### 2.1.2. Wind load

The aerodynamic load is calculated based on a modified blade element momentum (BEM) method (SINTEF Ocean 2019), in which the blade is divided into a set of elements, and the elements are assumed independent from each other. The dynamic model proposed by Øye (1991) is incorporated to model the unsteady effect. In addition, both the Glauert correction factor (to account for large induction factor effect) and the Prandtl correction factor (to account for tip and hub loss due to a finite number of blades) are applied. A variable-speed torque strategy and a blade pitch strategy are incorporated into the wind turbine controller. The two control strategies are designed to work independently in the below-rated and above-rated wind speed range, respectively.

The goal of the variable-speed torque controller is to maximize the power capture below the rated operation point. The blade-pitch controller is designed to regulate the generator power above the rated operation point.

### 2.2. Spreading function

The real ocean waves are short-crested in most cases, propagating along multiple directions. In this circumstance, a 2-D wave spectrum must be utilized to describe the waves. In the present research, a simple but effective 2-D wave spectrum is utilized,
which is a modification of the 1-D wave spectrum using the spreading function $D(\theta)$

$$S(\omega, \theta) = D(\theta) \cdot S(\omega)$$  \hspace{1cm} (5)

The 2-D wave spectrum should ensure that the total energy is equal to that of the 1-D wave spectrum

$$S(\omega) = \int_{-\pi}^{\pi} D(\theta)S(\omega)d\theta$$  \hspace{1cm} (6)

The spreading function $D(\theta)$ recommended by Standard DNV RP-C205-2010 is adopted here

$$D(\theta) = \begin{cases} 
\frac{1}{\sqrt{\pi}} \cdot \frac{\Gamma(n/2+1)}{\Gamma(n/2+1/2)} \cos^n(\theta - \theta_0), & |\theta - \theta_0| < \frac{\pi}{2} \\
0, & \frac{\pi}{2} \leq |\theta - \theta_0| \leq \pi
\end{cases}$$  \hspace{1cm} (7)

where $\Gamma$ is the Gamma function and $\theta_0$ is the main wave direction. $n = 2$ is the spreading exponent. A small spreading exponent indicates that less energy is focused around the dominant direction and more around the peripheral directions. Fig. 3 gives an example of the 2-D wave spectrum, where it can be found that the wave energy spreads over multiple directions.

![Fig. 3. 2-D wave spectra (Hs = 6 m, Tp = 10 s).](image)
3. Convergence

3.1. Mesh convergence

The hydrodynamic coefficients used to generate the wave load is calculated with the boundary element approach. An accurate estimation of the hydrodynamic coefficients is the premise of the coupled numerical simulation. Therefore, a convergence investigation on the element size is first conducted. Two sets of elements are generated: the fine element has a size of 0.8 m × 0.8 m whereas the coarse element has a size of 1.0 m × 1.0 m (see Fig. 4). Please note that due to the meshing algorithm and the platform shape, the actual size of the element may vary slightly.

![Fig. 4. Mesh discretization of the platform. (a) coarse mesh; (b) fine mesh](image)

The wave force transfer function $H_f^{(1)}$ obtained with the two mesh configurations is compared in Fig. 5. The two meshes nearly give identical results, indicating that the mesh convergence is satisfied.
3.2. **Direction discretization convergence**

In the numerical implementation, the 2-D wave spectrum is not continuous and should be divided into a set of orientations. To ensure that enough numbers of directions are divided, a convergence study on direction discretization $\Delta \theta$ is conducted here. Table 4 lists the standard deviations of lateral wave forces with the usage of different direction discretization. As $\Delta \theta$ approaching to 2.0 deg, the standard deviations converge gradually and hereinafter $\Delta \theta = 2.0 \text{ deg}$ will be adopted.

**Table 4 Standard deviations of lateral wave force**

<table>
<thead>
<tr>
<th></th>
<th>$\Delta \theta = 2.5 \text{ deg}$</th>
<th>$\Delta \theta = 2.25 \text{ deg}$</th>
<th>$\Delta \theta = 2.0 \text{ deg}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>LC1</td>
<td>Sway 2028 kN (+1%)</td>
<td>2065 kN (+2%)</td>
<td>2019 kN (0%)</td>
</tr>
<tr>
<td>Roll</td>
<td>43250 kN-m (-5%)</td>
<td>44760 kN-m (-2%)</td>
<td>45530 kN-m (0%)</td>
</tr>
<tr>
<td>LC2</td>
<td>Sway 2821 kN (+3%)</td>
<td>2828 kN (+3%)</td>
<td>2730 kN (0%)</td>
</tr>
<tr>
<td>Roll</td>
<td>56520 kN-m (+2%)</td>
<td>56800 kN-m (+4%)</td>
<td>55400 kN-m (0%)</td>
</tr>
<tr>
<td>LC3</td>
<td>Sway 3428 kN (+7%)</td>
<td>3235 kN (+1%)</td>
<td>3198 kN (0%)</td>
</tr>
<tr>
<td>Roll</td>
<td>61850 kN-m (+5%)</td>
<td>58950 kN-m (+1%)</td>
<td>58740 kN-m (0%)</td>
</tr>
</tbody>
</table>

4. **Validation**

Prior to investigation on the short-crestedness effect, the developed numerical model is firstly validated against model test data measured by the OC4 consortium (Coulling et
al. 2013). The 1/50th scaled model test was carried out at MARIN wave basin. Froude scaling was employed in the test to ensure geometry similarity. As shown in Table 5, the nacelle mass and platform moment inertia measured in the model test are larger than the prototype. Please note that the augmented nacelle mass and platform moment inertia are employed in and only in this section.

Table 5 Mass properties of nacelle and platform (full scale)

<table>
<thead>
<tr>
<th></th>
<th>Prototype value</th>
<th>Model test value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nacelle mass (kg)</td>
<td>240,000</td>
<td>274,940</td>
</tr>
<tr>
<td>CM below still water level (m)</td>
<td>13.4</td>
<td>14.4</td>
</tr>
<tr>
<td>I_{xx} (kg∙m^2)</td>
<td>6.8E+09</td>
<td>8.0E+09</td>
</tr>
<tr>
<td>I_{yy} (kg∙m^2)</td>
<td>6.8E+09</td>
<td>8.0E+09</td>
</tr>
</tbody>
</table>

Fig. 6 compares the response amplitude operator (RAO) of response recorded in the test and simulated with Sima, respectively. Despite the discrepancy on tower base bending moment at 0.13 Hz, the simulation results match the experimental measurement well. The hydrodynamic modelling in Sima is based on linear potential flow theory, which integrates hydrodynamic pressure over the mean wetted surface to obtain the overall hydrodynamic load. Obviously, it is not completely consistent with the model test in which the instantaneous wetted surface was changing all the time. It is the primary source of discrepancies in Fig. 6. As recommended by Robertson et al. (2014), the quadratic damping model is used to simulate viscous load. However, it should be noted such model may not fully represent the viscous effect and thereby will lead to difference between numerical simulation and model test measurement.

Fig. 6. RAOs of platform surge motion and tower base fore-aft bending moment.
5. Results

The joint wind-wave condition considered in the present numerical simulation is given in Table 6. Turbulent wind field is considered in the numerical simulation and the API wind spectrum is employed. The JONSWAP wave spectrum is used to generate random wave oceans. The total simulation length is 4000 s, with the time step being 0.01 s. Only the last 1-hour data are selected for the analysis in order to get rid of the transient effect. Six independent simulations with different wave seeds are carried out for each load case to reduce stochastic variations. The results presented hereinafter are based on the mean value of six simulations. Investigating the directionality effect is the primary objective of the numerical simulation.

Table 6 Environmental condition

<table>
<thead>
<tr>
<th>Wind speed</th>
<th>$H_s$</th>
<th>$T_p$</th>
<th>$\theta_0$</th>
</tr>
</thead>
<tbody>
<tr>
<td>LC1</td>
<td>8 m/s</td>
<td>5 m</td>
<td>8 s</td>
</tr>
<tr>
<td>LC2</td>
<td>11.4 m/s</td>
<td>6 m</td>
<td>10 s</td>
</tr>
<tr>
<td>LC3</td>
<td>14 m/s</td>
<td>7 m</td>
<td>12 s</td>
</tr>
</tbody>
</table>

5.1. Effect on platform motions

Fig. 7 summaries the statistics of surge and pitch motions, which shows that the platform motions are reduced when modelling waves as short-crested. According to the discussion in section 2.2, the wave energy distributes over multiple orientations so that the main direction component carries less energy (see Fig. 3). It explains the reduction of pitch and surge motions in short-crested waves. Since the majority of wave energy still propagate along the main direction, the longitudinal motions are not reduced substantially.
Fig. 7. Standard deviations of platform surge and pitch motions.

Fig. 8 presents the power spectrum density (PSD) of platform surge and pitch motions. Two peaks are observed in the PSD of surge motion. The minor peak represents the wave energy response, which is induced by the dominating frequency components of the wave spectrum. The major peak is stimulated at 0.01 Hz, namely the natural period of the surge motion. It is worth noting that such low frequency response is outside the wave spectrum. As presented by Coulling et al. (2013), the low-frequency surge motion is attributed to the 2nd order wave difference-frequency force.

Fig. 8. PSD of platform surge and pitch motions. Black curve: short-crested wave; red curve: long-crested wave.
Comparatively, the lateral motions are induced to a large extent in the short-crested wave. Fig. 9 plots the time histories of platform sway and roll motions in short-crested and long-crested waves, respectively. In the long-crested wave, the lateral motions are very limited since all wave energy purely induces longitudinal motions. Nevertheless, one can still observe very small sway and roll motions, which are induced by the rotor torque. In short-crested wave, the lateral motions are amplified substantially even if the platform geometry is symmetric. Obviously, this is caused by the wave energy that propagates along other orientations.

![Time series of platform sway and roll motions. Black curve: short-crested wave; red curve: long-crested wave.](image)

**5.2. Effect on mooring tension force**

The directionality effect on mooring tension is illustrated in Fig. 10. The average mooring tension is independent on the wave type. Regardless of short-crested wave or long-crested wave, the linear wave load is the sum of multiple sinusoidal excitations so that the average value is exactly zero. Although the 2nd order difference-frequency pushes the platform from the initial equilibrium position, the magnitude of mean drift is
small. Actually, the mean mooring tension is dominated by the pre-tension and thrust force acting on the rotor. It explains why the mean mooring tension is the largest in the rated load case LC2. However, the directionality effect on the oscillation of mooring tension is considerable. Taken LC3 for instance, the standard deviation in the short-crested wave is approximately 99 kN, 18% lower than that in long-crested wave. The same conclusion can be also drawn from other load cases.

![Fig. 10. Directionality effect on mooring tension, mooring line 1. Height bar indicate mean value and error bar indicates standard deviation.](image)

To assess the safety of mooring line, the extreme tension force should be estimated based on a large amount of samples, namely independent numerical realizations. Extrapolation method is typically used to reduce the sample size and thus to reduce the numerical of numerical realizations. In the present research, extrapolation method proposed by Naess and Gaidai (2008) is employed. According to their method, the up-crossing rate \( \nu(x) \) of variable \( x \) can be extrapolated as

\[
\nu(x) = q \cdot \exp\left\{ -a(x - b)^c \right\}
\]

(8)

Parameters \( q, a, b, c \) are estimated through least-square method based on the 6 independent numerical simulations. Fig. 11 compares the sampled based and the extrapolated up-crossing rate of extreme mooring tension force. The extreme tension force is less stable around the trail region, justifying the usage of extrapolation method.
Hereinafter, the extreme tension force corresponding to $1/3600$ Hz suggested by the extrapolation will be adopted as the extreme response.

![Graph](image)

Fig. 11. Extrapolation of the up-crossing rate (LC1, long-crested wave).

Table 7 compares the extreme mooring tension force. It shows that the mooring line is safer in short-crested waves,

<table>
<thead>
<tr>
<th></th>
<th>LC1</th>
<th>LC2</th>
<th>LC3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Long-crested</td>
<td>2750</td>
<td>2428</td>
<td>1970</td>
</tr>
<tr>
<td>Short-crested</td>
<td>2303</td>
<td>2215</td>
<td>1828</td>
</tr>
</tbody>
</table>

**5.3. Effect on tower base structural integrity**

Structural failure occurs if the maximum structural load exceeds a certain level. Fig. 12 gives the maximum bending moment at tower base. The short-crestedness leads to the lower level of maximum fore-aft bending moment. However, a notable increase in the side-to-side bending moment is seen under short-crested wave. The power spectral density of tower base side-to-side bending moment is plotted in Fig. 13. In the long-crested wave, as a result of low side-to-side damping, two resonant response peaks are observed. The primary response peak is induced around $0.04$ Hz, namely the natural frequency of platform roll motion. In addition, a second peak is seen at $0.44$ Hz, which is around the tower’s 1st vibration natural frequency. No wave-frequency response is
observed as the wave purely propagate along surge direction in the long-crested wave case. When the wave shortness is simulated, the wave propagates along multi-directions, leading to a sharp increasement on wave-frequency response whilst resonant response is nearly negligible. It is thus concluded that the resonant response dominates in the long-crested wave while wave-frequency dominates in the short-crested wave.

Fig. 12. Extreme tower base bending moment

Fig. 13. PSD of tower base side-to-side bending moment, LC2. Left: long-crested wave, right: short-crested wave.

It is not straightforward to investigate the directionality effect on the bending moment directly from Fig. 12 as the fore-aft bending moment is reduced whereas the side-to-side bending moment is increased. Here, we investigate the magnitude of bending moment

$$M = \sqrt{M_x^2 + M_y^2} \quad (9)$$

Table 8 summaries extreme bending moment magnitude in the two types of ocean waves. In short-crested wave, the maximum bending moment magnitude is reduced, indicating that tower base is less likely to exceed its ultimate limit state.

Table 8 Extreme bending moment magnitude $M$

<table>
<thead>
<tr>
<th></th>
<th>LC1</th>
<th>LC2</th>
<th>LC3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Long-crested wave (kN)</td>
<td>1.16×10^5</td>
<td>1.29×10^5</td>
<td>1.06×10^5</td>
</tr>
</tbody>
</table>
Wind, wave and inertial loads applied at tower base cause stress fluctuation, which produces fatigue damage. The SN-based fatigue analysis software Mlife (NWTC 2017) developed by NREL is used to assess the fatigue damage at tower base. The fluctuating stress are broken down into individual hysteresis cycles by matching local minima with local maxima in the time series, which are characterized by a load-mean and range. The stress cycles are counted by the rainflow counting algorithm. It is assumed that the damage accumulates linearly with each of these cycles according to Miner’s Rule. In this case, the overall equivalent damage load \( DL \) produced by all the cycles is given by

\[
DL = \left[ \sum \left( \frac{n_i (L_i)^m}{n_{STeq}} \right) \right] \frac{1}{m}
\]

(10)

where \( n_{STeq} \) total equivalent fatigue counts. \( n_i \) is the cycle count and \( L_i \) is the cycle's load range. \( m \) is the Whöler exponent. \( m = 3 \) is used in this study.

Although the tower is subject to both axial stress and shear stress, the shear stress has a much smaller influence on the fatigue as suggested by Kvittem and Moan (2015). Thus, only the axial stress is considered in the fatigue assessment. As shown in Fig. 14, the axial stress across the tower base section is calculated as

\[
\sigma = \frac{N_z}{A} + \frac{M_y}{I_y} \cdot r \cos(\theta) - \frac{M_z}{I_z} \cdot r \sin(\theta)
\]

(11)

where \( N_z \) is the axial force; \( A \) is the nominal cross section area; \( M_y \) and \( M_z \) are fore-aft and side-side bending moment respectively; \( I_y \) and \( I_z \) are the sectional moments of the area.
Fig. 14. Axial stress at tower base.

The assessed fatigue damage loads are presented in Table 9. For Point 1 and Point 3, the short-crestedness has a positive effect on the structure safety. A significant reduction on the fatigue damage load is observed under all loading conditions. This is consistent with the simplified fatigue reduction factor proposed by Vugts (2005), which is a quick approach to assess the fatigue reduction of fixed offshore structure owing to wave shortness effect. Assuming that the fatigue damage of a fixed offshore structure is purely caused by wave load, Vugts (2005) assessed the reduction of fatigue damage caused by short-crested wave by

$$RD = \frac{\pi^2}{2} D(\theta) \cdot \cos^m \theta \cdot d\theta$$  \hspace{1cm} (12)

Considering that $m = 3$ is used in the present simulation and the spreading function $D(\theta)$ given by Eq. (7), the estimated fatigue reduction factor is 0.68, which is lower than the simulation results presented in Table 9. It is because we consider a floating structure and the tower base fatigue damage is caused by both wave load and wind load.

Table 9 Fatigue damage load at tower base

<table>
<thead>
<tr>
<th></th>
<th>Point 1 (MPa)</th>
<th>Point 2 (MPa)</th>
<th>Point 3 (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>LC1</td>
<td>Long-crested wave</td>
<td>28.6</td>
<td>1.6</td>
</tr>
<tr>
<td></td>
<td>Short-crested wave</td>
<td>21.7</td>
<td>15.2</td>
</tr>
<tr>
<td>LC2</td>
<td>Long-crested wave</td>
<td>31.9</td>
<td>2.8</td>
</tr>
<tr>
<td></td>
<td>Short-crested wave</td>
<td>27.2</td>
<td>13.5</td>
</tr>
<tr>
<td>LC3</td>
<td>Long-crested wave</td>
<td>28.3</td>
<td>2.4</td>
</tr>
<tr>
<td></td>
<td>Short-crested wave</td>
<td>24.4</td>
<td>11.5</td>
</tr>
</tbody>
</table>
However, Point 2 is subjected to much more fatigue damage load in short-crested waves. By comparing the fatigue damage at the three points, we can find Point 1 and Point 3 are more critical than Point 2, and thus can conclude that short-crestedness is beneficial to the structural integrity. Please note that this paper only presents a short-term fatigue damage analysis whilst a long-term fatigue damage calculation must be carried out to assess the overall fatigue damage during the lifetime.

1. Conclusions
The effect of short-crestedness on the dynamic response of a semisubmersible offshore floating wind turbine is investigated in the present research. On condition that the total wave energy is identical, the longitudinal platform motions are reduced whereas the lateral motions are amplified in the presence of short-crested wave. It is due to the fact that the wave energy propagates along multiple orientations in the short-crested wave. The structural loads at critical connection points are also reduced in the short-crested wave. When wave propagates along multiple orientations, both the tower base bending moment and the mooring tension decrease. In the meanwhile, the fatigue damage load at tower base is also reduced. It indicates that the short-crestedness effect is beneficial to the structural integrity.

The present research manifests that offshore floating wind turbines behave differently in long-crested and short-crested waves. Therefore, the wave directionality should be considered carefully during the design stage of offshore floating wind turbines.

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Table 1 Main scantlings of the platform.
Table 2 Properties of the mooring line.
Table 3 Quadratic drag coefficient of the platform
Table 4 Standard deviations of lateral wave force
Table 5 Mass properties of nacelle and platform
Table 6 Environmental condition
Table 7 Extreme mooring tension force
Table 8 Extreme bending moment magnitude M
Table 9 Fatigue damage load at tower base

Fig. 1. Platform geometry
Fig. 2. Numerical modelling in Sima.
Fig. 3. 2-D wave spectrums (Hs = 6 m, Tp = 10 s).
Fig. 4. Mesh discretization of the platform. (a) coarse mesh; (b) fine mesh
Fig. 5. Wave force transfer function H(1) of the platform.
Fig. 6. RAOs of platform surge motion and tower base fore-aft bending moment.
Fig. 7. Standard deviations of platform surge and pitch motions.
Fig. 8. PSD of platform surge and pitch motions. Black curve: short-crested wave; red curve: long-crested wave.
Fig. 9. Time series of platform sway and roll motions. Black curve: short-crested wave; red curve: long-crested wave.
Fig. 10. Directionality effect on mooring tension, mooring line 1.
Fig. 11. Extrapolation of the up-crossing rate (LC1, long-crested wave).
Fig. 12. Extreme tower base bending moment
Fig. 13. PSD of tower base side-to-side bending moment, LC2. Left: long-crested wave, right: short-crested wave.
Fig. 14. Axial stress at tower base.