Implementation of potential flow hydrodynamics to time-domain analysis of flexible platforms of floating offshore wind turbines

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Abstract. In the design of supporting platforms of floating offshore wind turbines, global response analysis is essential to predict the response under various loads from wave, wind, moorings and the wind turbines. However, the literature of the global analysis of floating offshore wind turbines combining flexible modelling of the supporting platform and the potential flow theory for hydrodynamic evaluation is limited. In this study, the framework implementing the potential flow hydrodynamics to the time-domain analysis of the three-dimensional frame model was developed using modal decomposition for the hydrodynamic evaluations. The method utilized the assumption that the number of modes can be limited to those with larger contributions, which can lead to the reduction of the calculation cost. A spar-type floating offshore wind turbine was modelled to verify the implementation of the developed formulations. Results showed that the implemented method is effective in predicting the dynamic motion and deformation of floating offshore wind turbines under combined wind and wave loads. For the cases of the irregular waves, the results obtained with potential flow hydrodynamics showed smaller responses than the results from the Morison’s equation in the natural frequencies of rigid body motion, which might be attributed to the steady and low frequency external forces introduced in the Morison’s equation by considering the instantaneous position of the floater.

1. Introduction

Floating offshore wind turbines have been attracting growing interests with their ability to exploit wind resources in deep waters. In the design of supporting platforms of the floating wind turbines, global response analysis is essential to predict the behaviour of the system under the combination of various loads from wave, wind, moorings and wind turbines. Some aero-hydro-servo-elastic tools have been developed for this purpose, adopting various modelling approaches for the evaluation of the aerodynamics, hydrodynamics, mooring dynamics and structural dynamics¹. Among the existing numerical tools, the modelling approach for the structure of the supporting platform can be generally divided into two groups; rigid body modelling and flexible body modelling. The former approach treats the supporting floater as a rigid body with six degrees of freedom. The advantage is the remarkably light calculation cost required to capture the overall motions of the floater and their effects on the system above. Meanwhile, when the target of the evaluation comes to the assessment of the structural strength of the platform, an additional process of Finite Element (FE) modelling is required. The problem with this procedure is that some uncertainties can be introduced by the modelling of the distribution of the
acceleration over the supporting platform which is not evaluated with the global analysis. The other structural modelling approach is flexible body modelling. In this approach, assessment of the structural strength for the primary supporting members, which is of interest in the initial design stage, is possible by directly using the results from the global analysis.

For most of the numerical tools for floating wind turbines, the flexible structural modelling of the supporting platform is often combined with the Morison’s equation for hydrodynamic evaluation. This combination is seen in the options of the numerical codes such as Bladed\(^2\) and HAWC2\(^3\). Morison’s equation is a well-proven engineering formula for slender structures where the viscous and inertia effects from the wave are dominant. Another widely used hydrodynamic modelling method is the potential flow theory, which is usually adopted when the effect of the hydrodynamic scatterings from the floater is not negligible. It is also termed and known as diffraction theory, but the terminology “potential flow theory” is used throughout the paper. When the potential flow theory is adopted, the structure of the floating platform is usually modelled as a rigid body, which may be justified by the fact that a massive structure tends to have large stiffness and to give larger hydrodynamic scatterings. The combination of the potential flow theory and the rigid platform is used in majority of the numerical codes for offshore wind turbines such as Bladed, HAWC2, FAST\(^4\) and other various hydrodynamic tools\(^5,6,7\) coupled with FAST. The choice of the hydrodynamic model is usually indicated by the relationship of the Keulegan-Carpenter number and the ratio of the member diameter to the wave length\(^8\). Considering that various types of floaters have been proposed for offshore wind turbines, the combination of the potential flow theory and the flexible modelling of the supporting platform structure is required to enable the direct structural assessment for wider ranges of floaters.

The combination of the potential flow theory and the flexible structures has been applied previously for very large floating structures (VLFSs). Since most of the external forces on the VLFSs are linear, a large part of the previous studies has been conducted in the frequency domain, where the structural modelling approaches can be generally classed into the modal decomposition method and the direct calculation method. In the former method, the displacement of the floating structure is decomposed into modes, of which hydrodynamic pressures are evaluated individually. This approach is adopted in studies such as Fu et al. (2006)\(^9\), Loukogeorgaki et al. (2012)\(^10\), and Micaelides et al. (2013)\(^11\). In this approach, the number of modes can sometimes become large in order to exactly express the platform displacement. The other approach is the direct calculation method adopted in studies such as Kashiwagi (1998)\(^12\), Yoon et al. (2014)\(^13\), and Kim et al. (2017)\(^14\), where the elastic motion and the pressure distribution of the floater are solved simultaneously. Several applications in the time-domain have also been performed such as Kashiwagi (2000)\(^15\), Liu and Sakai (2002)\(^16\), and Kyong et al. (2006)\(^17\) to evaluate the nonlinear phenomena of the VLFSs. For the floating offshore wind turbines, analysis in the time-domain is usually required, as the loads from the operating wind turbines controlled to maximize or limit the energy production is nonlinear. Studies of the global analysis combining potential flow theory with flexible structural modelling in time-domain for floating offshore wind turbines are limited.

The aim of this study is to implement the potential flow hydrodynamics to the global analysis of floating offshore wind turbines with flexible platforms. First, theoretical backgrounds are explained. The developed method combines the modal decomposition method for the evaluation of the hydrodynamic loadings and the direct time-integration for three-dimensional frame models. The theoretical formulations are implemented into an in-house numerical code for offshore wind turbines. Next, a flexible structural model for a spar-type floating offshore wind turbine is set up with developed code. Two approaches one with the potential flow theory, and the other with the Morison’s equation, are compared, using which the implementation of the theory is verified.

2. Theoretical backgrounds

2.1. Equation of motion

Theoretical backgrounds for the implementation of the potential flow theory to the three-dimensional frame model for floating offshore wind turbines are presented in this section. The supporting structure of a floating offshore wind turbine is discretized with three-dimensional FE frame model with \(N\) number
of nodes with six degrees of freedom (DOF). The equation of motion for the system is written as Eq. (1).

\[ \{M\}_{6N,6N}\{\ddot{x}\}_{6N,1} + \{C\}_{6N,6N}\{\dot{x}\}_{6N,1} + \{K\}_{6N,6N}\{x\}_{6N,1} = \{F^{\text{hydro}} + F^{\text{lines}} + F^{\text{buoyancy}} + F^{\text{aero}}\}_{6N,1} \]

where \{M\} is the mass matrix, \{K\} is the stiffness matrix, and \{C\} is the Rayleigh damping matrix. \(F^{\text{hydro}}\) is the hydrodynamic forces, \(F^{\text{lines}}\) is the forces from the mooring lines, \(F^{\text{buoyancy}}\) is the restoring force, and \(F^{\text{aero}}\) is the force from wind turbine. When the potential flow theory is used with viscous drag force for evaluation of \(F^{\text{hydro}}\), the term is divided into three components as shown in Eq. (2).

\[ \{F^{\text{hydro}}\} = \{F^{\text{radiation}}\} + \{F^{\text{diffraction}}\} + \{F^{\text{viscos}}\} \]

where \(F^{\text{radiation}}\) is the radiation force, \(F^{\text{diffraction}}\) is the diffraction force including the Froude-Krylov force, and \(F^{\text{viscos}}\) is the additional viscous forces. In this study, the radiation force \(F^{\text{radiation}}\) is evaluated by the modal decomposition. The displacement of the system \{x\} can be expressed by the modal shapes \{\phi\} and modal displacements \{u\} as shown in Eq. (3). Here, \(\phi_{ij}\) is the modal shape at \(i\)-th DOF for the \(j\)-th mode and \(u_j\) is the modal displacement for the \(i\)-th mode. \(M\) is the number of modes required to exactly express the structural displacement.

\[ \{x\}_{6N,1} = \{\phi\}_{6N,M}\{u\}_{M,1} \]

When the added mass coefficient \(A_{li}\) and radiation damping coefficient \(B_{li}\) for the \(j\)-th mode at the \(i\)-th DOF are evaluated, the hydrodynamic radiation force \(F^{\text{radiation}}\) for the system can be written as Eq. (4). \(L_{ij}\) is the memory effect function for the \(j\)-th mode at the \(i\)-th DOF, and is calculated using Eq. (5).

\[ L_{ij}(t) = \frac{1}{\pi} \int_{0}^{\infty} B_{ij}(\omega) \cos \omega t \, d\omega \]  

Using \(M\) for the estimation of the radiation forces is sometimes not realistic since the calculation cost increases drastically with the increase of the number of modes included. Considering that the contribution of some of the modes to the radiation forces is small, the calculation cost can be reduced by taking up only the modes with dominant contributions. Here, it is assumed that only \(m\) number of modes contributes to the radiation forces. Using this assumption, the number of terms that require integration calculation can be reduced from \(M \times 6N\) to \(m \times 6N\), where usually \(m \ll M\). Considering the orthogonality of the modal vector matrix, the modal displacements of \(m\) number of modes can be written as Eq.(6), where \(\phi^T\) is transposed matrix of \(\phi\), and Eq.(4) can be re-written as Eq.(7).

\[ \{u\}_{m,1} = \phi^T_{m,6N}\{M\}_{6N,6N}\{x\}_{6N,1} \]  

\[ - \left[ \begin{array}{cccc} A_{1,1}(\omega) & \cdots & A_{1,M}(\omega) \\ \vdots & \ddots & \vdots \\ A_{6N,1}(\omega) & \cdots & A_{6N,M}(\omega) \end{array} \right] \phi^T_{m,6N}\{M\}_{6N,6N}\{\ddot{x}\}_{6N,1} \]

\[ - \int_{-\infty}^{\infty} \left[ \begin{array}{cccc} L_{1,1}(t-\tau) & \cdots & L_{1,M}(t-\tau) \\ \vdots & \ddots & \vdots \\ L_{6N,1}(t-\tau) & \cdots & L_{6N,M}(t-\tau) \end{array} \right] \phi^T_{m,6N}\{M\}_{6N,6N}\{\ddot{x}\}_{6N,1} \]  

The added mass term in Eq. (7) is moved to the left-hand side of the equations of motion for time integration, while the radiation damping term is treated as external force in the right-hand side. It is to be noted that the rigid body motions are treated as the first six modes in Eqs. (3), (4), (6) and (7). The hydrodynamic diffraction force \(F^{\text{diffraction}}\) is evaluated directly for each DOF. The components of the diffraction force is shown in Eq.(8), where \(F^d_i\) is the diffraction force acting on the \(i\)-th DOF.
\[
\{F_{diffraction}\}_i = \left\{ F_i^d \right\}
\]

For irregular waves expressed with Eq. (9), the diffraction force acting on the i-th DOF \( F_i^d \) can be evaluated with Eq. (10).

\[
\eta(t) = \sum_{k=1}^{K_\omega} \sqrt{2S(\omega_k)\Delta\omega} \cos(\omega_k t + \alpha_k)
\]

\[
F_i^d(t) = \sum_{k=1}^{K_\omega} \sqrt{2S(\omega_k)\Delta\omega} \left| \text{Re}(P_i^d(\omega_k)) \cos(\omega_k t + \alpha_k) - \text{Im}(P_i^d(\omega_k)) \sin(\omega_k t + \alpha_k) \right|
\]

where \( \eta(t) \) is the wave elevation of the irregular wave, \( S(\omega_k) \) is the wave spectrum of the k-th circular wave frequency \( \omega_k \), \( P_i^d \) is the diffraction pressure at i-th DOF, and \( \alpha_k \) is the phase angle at \( \omega_k \).

In the potential flow theory, the viscosity is not considered, and an additional term is required to include its effect. In this study, the term shown in Eq. (11) is used, where \( \rho \) is water density, \( D \) is the diameter of the cross-section, \( C_D \) is the drag coefficient, and \( v \) is the position of the wave particle. The inclusion of the viscous term to the potential flow theory is simpler in the time-domain analysis than the frequency domain analysis.

\[
P^{visc} = C_D \frac{1}{2} \rho D (\dot{v} - \dot{x}) |\dot{v} - \dot{x}|
\]

The equations described above are implemented to an in-house code NK-UTWind\(^1\), which is a time-domain tool for FE analysis of floating offshore wind turbines. Originally, the hydrodynamic loadings in NK-UTWind are based on the Morison’s equation, where the \( F^{hydro} \) term in Eq. (1) is evaluated with Eq. (12).

\[
F^{hydro} = \rho \frac{\pi D^2}{4} \dot{v} + C_m \rho \frac{\pi D^2}{4} (\dot{v} - \dot{x}) + C_D \frac{1}{2} \rho D (\dot{v} - \dot{x}) |\dot{v} - \dot{x}|
\]

where \( C_m \) is the added mass coefficient. The mooring force \( F^{lines} \) can be calculated using either the quasi-static catenary equation or the lumped-mass method. The aerodynamic and the inertia forces from the wind turbine is calculated with FAST and passed to NK-UTWind as \( F^{aero} \) at the tower-top node. From NK-UTWind, the displacement and velocity of the tower-top node is passed at each time step.

### 2.2. Evaluation of the hydrodynamic coefficients for individual nodes

For Eqs. (7) and (9), the hydrodynamic coefficients at each node are required. To estimate this, the floater is first modelled with panels for potential flow solvers, and the calculated hydrodynamic pressures are outputted for each panel. For each node, a group of panels are assigned, and by summing the obtained pressure over the panel group, the hydrodynamic coefficients for the associated node are obtained. For the accuracy of the estimation of the hydrodynamic forces, the size and arrangement of the panels are determined so that the centre of the panel group is aligned with the associated node as shown in Figure 1. The added mass coefficient, radiation damping coefficient, and the diffraction pressure are evaluated as per Eq. (13), (14), and (15) respectively. \( n = 1,2,3 \) indicates the translation motions while \( n = 4,5,6 \) indicates the angular motions.

\[
\omega^2 A_{i,j} = \left\{ \begin{array}{ll}
\sum_{s_i} \text{Re}(p_{s_i}^d) (\phi_j \cdot n_s) ds & (n = 1,2,3) \\
\sum_{s_i} \text{Re}(p_{s_i}^d) (\phi_j \cdot n_s) \times r_s ds & (n = 4,5,6)
\end{array} \right.
\]

\[
\omega^2 B_{i,j} = \left\{ \begin{array}{ll}
\sum_{s_i} \text{Im}(p_{s_i}^d) (\phi_j \cdot n_s) ds & (n = 1,2,3) \\
\sum_{s_i} \text{Im}(p_{s_i}^d) (\phi_j \cdot n_s) \times r_s ds & (n = 4,5,6)
\end{array} \right.
\]

\[
p_i^d = \left\{ \begin{array}{ll}
\sum_{s_i} p_{s_i}^d (\phi_j \cdot n_s) ds & (n = 1,2,3) \\
\sum_{s_i} p_{s_i}^d (\phi_j \cdot n_s) \times r_s ds & (n = 4,5,6)
\end{array} \right.
\]

Here, \( p_{s_i}^d \) is the radiation pressure obtained at the s-th panel, \( p_i^d \) is the diffraction pressure at the s-th panel. \( n_s \) is the unit vector normal to the surface of the s-th panel, \( r_s \) is the distance from the origin of the coordinate to the centre of the s-th panel, and \( s_i \) is the panels associated with the i-th DOF of the floater. Eqs.(13), (14) and (15) are calculated within NK-UTWind using the information of panel-node relationship.
3. Calculation settings

To verify the developed code, a spar-type floating offshore wind turbine model were set up. The geometries of the floater and the mooring system were based on the Hywind model used in the OC3 project\textsuperscript{19} in the IEA Wind Task 30. The draught of the floater was 120 m and the diameter of the platform below the tapered part was 9.5 m. As only the rigid-body properties are specified in [19], the elastic properties and the distribution of the mass were estimated assuming that the plate thickness is 50 mm, the Young’s modulus of the steel material is $2.05 \times 10^{11}$ N/m$^2$, and all the cross-sectional shape is circle. The height of the ballast tank was determined so that the centre of gravity and the rotational inertia around the centre of gravity were in accordance with [19]. The principal particulars of the floating platform are shown in Table 1. The floater is moored with three catenary moorings. The main properties of the moorings are shown in Table 2. The wind turbine mounted on the floater was the 5 MW reference wind turbine defined in [20]. The rotor diameter was 126 m and the hub height was 90 m above the sea level. All the definitions of the tower, blade, drivetrain and the control parameters were set as the same values defined in [20]. The principal particulars of the wind turbine are shown in Table 3. A schematic of the whole floating offshore wind turbine system is shown in Figure 2 (a).

For the evaluation of the hydrodynamic pressures on the floater for the potential flow theory, the software WAMIT\textsuperscript{21} was used. Figure 2 (b) shows the hydrodynamic mesh used in the analysis. Sensitivity study of the hydrodynamic coefficients to the number of panels were conducted using the panel numbers of 9760, 4960, and 2480. The results converged well with 2480 panels, where the floater was divided into 120 segments, 20 segments, and 4 segments in the vertical, circumferential, and radial directions respectively. The hydrodynamic added mass and radiation damping coefficients integrated over the floater is shown in Figure 3 for surge, heave, pitch and yaw directions using Eqs.(13), (14), and (15). The tower and the floater were modelled as a flexible body using three-dimensional frame model. The total number of nodes was 35 for the spar and 13 for the tower and the nacelle. A schematic of the frame model is shown in Figure 2 (c). In this study, it was assumed that only the component of rigid body motions contribute to the hydrodynamic radiation forces, which means the number of modes $m$ in Eq.(7) is set as six.
Table 1 Principal particulars of the spar-type floating platform

| Platform mass       | 7466.3 ton |
|---------------------|------------|
| Platform CoG height | -89.9 m    |
| System CoG height   | -75.5 m    |
| Platform diameter   | 9.5 m      |
| Platform CoG height | 89.9 m     |
| Platform diameter   | 6.5 m      |
| Platform bending    | 3290GNm²   |
| Platform roll / pitch inertia | 4.23Mtonm² |
| Platform axial stiffness | 301GN     |

Table 2 Principal particulars of the mooring system

| Horizontal distance of anchor to Platform Centerline | 853.87 m |
| Water depth                                           | 320 m    |
| Fairlead height                                      | -75 m    |
| Mooring line diameter in water                       | 0.09 m   |
| Mooring line weight in water                         | 698.094 N/m |
| Mooring line axial stiffness                         | 384,243,000 N |

Table 3 Principal particulars of the 5 MW wind turbine model

| Hub height | 90 m |
| Nacelle mass | 240 ton |
| Rotor diameter | 126 m |
| Rotor mass    | 110 ton |
| Nacelle CM location downwind of tower center | 1.9 m |
| Hub mass      | 56.8 ton |
| Tower base height | 10 m |
| Rated wind speed | 11.4 m/s |

Figure 3 Hydrodynamic added mass and radiation damping coefficients of the floater in (a) surge, (b) heave and (c) pitch direction

For evaluation using Morison’s equation, the added mass coefficient $C_m$ in Eq.(12) was set at $C_m = 0.9$ for horizontal directions and $C_m = 0.5$ for vertical direction. The drag coefficient $C_D$ was set as 0.6 for all elements of the three-dimensional frame model. The same drag coefficients were also used for the calculation of the viscous terms when the potential flow theory was used. The free-decay results of the floater motion are shown in Figure 4. It is seen that the calculated free-decay process agreed well between the two hydrodynamic models. This can be justified by the fact the radiation damping coefficients at the natural period are small for this floater. The natural periods of the floater motion
estimated from the free-decay are shown in Table 4. The natural periods also agreed well between the two hydrodynamic models.

![Figure 4](image-url) Comparison of the free-decay response between the potential flow theory and Morison’s equation in (a) surge, (b) heave, and (c) pitch direction

| Table 4 Natural periods of the Hywind-OC3 model |
|-----------------------------------------------|
|                   | Potential flow theory | Morison’s equation |
| Surge              | 116.0 s               | 116.5 s            |
| Heave              | 21.8 s                | 21.7 s             |
| Pitch              | 26.4 s                | 26.7 s             |
| Yaw                | 12.1 s                | 12.1 s             |

Three cases of external conditions were used for the verification. In the first load case, only regular waves were applied to the system to study its linear frequency responses. In the second load case, irregular wave was applied to verify the condition where the memory effect in the radiation force can be pronounced. In the third load case the wind turbine was set in the operating condition with a turbulent wind field. For each load case, two set of calculations were performed using the potential flow theory and the Morison’s equation. Details of the external conditions are shown in Table 5. Here \(U\) is the mean wind speed at hub-height, \(I_u\) is the turbulence intensity, \(H_s\) is the significant wave height and \(T_s\) is the significant wave period. Mann model was used to generate the three-dimensional turbulent wind field with the length scales recommended in IEC61400-1. Airy waves were used for the modelling of the wave kinematics. The directions of the wave, wind and the wind turbine nacelle were aligned for all cases. The time-step for the numerical integration was 0.01 second, and the calculation time lengths were 600 seconds for LC.1 and 3600 seconds for LC.2 and LC.3. For all cases the first 300 seconds of the results were deleted to eliminate the transient responses.

| Table 5 External conditions for each load case |
|-----------------------------------------------|
| Wind | Wave                  | Wind Turbine |
|------|-----------------------|--------------|
| LC.1 | No wind               | Regular airy | Parking     |
|      | Regular, \(H_s=1\) m, \(T_s=5-30\) sec |              |
| LC.2 | No wind               | Irregular airy | Parking |
|      | JONSWAP, \(H_s=3.25\), \(T_s=10\) sec |              |
| LC.3 | \(U = 11.3\) m/s, \(I_u = 7\) % Mann model | Irregular airy | Operating |
|      | JONSWAP, \(H_s=3.25\), \(T_s=10\) sec |              |

4. Results

4.1. Regular waves only case

A comparison of the Response Amplitude Operators (RAOs) obtained from the potential flow theory and the Morison’s equation is made in Figure 5. It is seen from the figure that the RAOs calculated with the potential flow theory agreed well with those calculated with the Morison’s equation for all directions.
Small differences could be observed in the wave-less period for the heave direction. This can be explained by a fact that the diffraction force for the tapered part is obtained using the smoothly arranged panels in the potential flow theory, while in the Morison’s equation it is obtained with the discretized beam elements.

![Figure 5](image)

**Figure 5** Comparison of the Response Amplitude Operators calculated using the potential flow theory and the Morison’s equation in (a) Surge, (b) Heave, and (c) Pitch direction

4.2. **Parked wind turbine with irregular waves case**

The time-series of the motions of the floater for LC.2 is compared between the potential flow theory and the Morison’s equation. The results are shown in Figure 6, where the node at z = 0 m is selected for the examination of the floater motion. It is seen that the mean value and the phases of the floater motion were similar between the two hydrodynamic models in all directions. The responses in frequency domain were obtained with Welch’s method and are shown in Figure 7. For surge direction, peaks were seen at 0.0085 Hz, 0.0354 Hz, and 0.096 Hz, which corresponds to surge natural frequency, pitch natural frequency, and wave excitation frequency, respectively. For the motion in heave direction, a peak apart from the wave excitation frequencies could be seen at around 0.096 Hz, which corresponds to the heave natural frequency. For the region of the wave excitation frequencies, the floater responses agreed well between the two hydrodynamic models. However, the potential flow theory predicted considerably smaller response than the Morison’s equation model around the natural frequencies of the floater. This can be explained by a fact that in the Morison’s equation model, the load is evaluated over the instantaneously wetted area by integrating the segment-wise load up to the instantaneous position of the free-surface. This yields steady and the higher order external forces to excite mooring tensions, while in the potential flow model, only the linear components of the hydrodynamic loadings are evaluated.

The distribution of the mean and maximum values of the internal force for LC.2 is shown in Figure 8. A comparison was made for force in x direction $F_x$, force in y direction $F_y$, bending moment in x direction $M_x$ and bending moment in y direction $M_y$. It is seen from the figure that the mean values predicted with the potential flow theory agreed well with those obtained with the Morison’s equation in all components of the sectional forces. For the maximum values, obtained distributions were similar between the two hydrodynamic models. The maximum values predicted with the potential flow theory were slightly lower than those obtained with the Morison’s equation for all components. The difference in the floater response shown in Figure 7 can be attributed to the difference in the higher order forces as discussed above.

![Figure 6](image)

**Figure 6** Comparison of the time-series of the calculated using proposed method and the Morison’s equation in (a) Surge, (b) Heave, and (c) Pitch direction
4.3. Operating wind turbine with irregular waves case

A comparison of the floater responses is made for LC.3 where the system is subjected to the irregular waves while the wind turbine is in operational condition, as shown in Figure 9. Here the motion of the floater on the node at the \( z = 0 \) is selected. It is seen from the figure that common peaks were observed for the two models, in surge and pitch direction around 0.0085 Hz, 0.0354 Hz, and 0.096 Hz, which corresponds to surge natural frequency, pitch natural frequency, and wave excitation frequency, respectively. For the motion in heave direction, peaks were observed at 0.0317 Hz and 0.096 Hz which corresponds to the heave natural frequency and the wave excitation frequency, respectively. For LC.3, the responses of the floater were similar between the two hydrodynamic models in all directions, which is different from what was observed for the parked condition case discussed in the previous section. The Morison’s equation gave much larger responses around the peaks of natural frequencies. One explanation can be given as the following; in the operational condition case, the thrust force of the wind turbine is dominant in the steady external force that causes mooring tension whereas the effect of the steady wave drifting force is limited.

The distribution of the mean and maximum values of the internal force for LC.3 is shown in Figure 10 for \( F_x \), \( F_y \), \( M_x \), and \( M_y \). It is seen from the figure that the mean and maximum of \( F_x \) were positive in the upper part and negative in the lower part of the structure. The maximum positive value occurred at the tower base, and the maximum negative value in magnitude was found around – 60 m, which is the top of the ballasted part. The mean values agreed well between the two hydrodynamic models, while for the maximum value, the results for the Morison’s equation were larger along the whole height of the structure. The maximum values for \( F_y \) and \( M_x \) were small compared to \( F_x \) and \( M_y \) respectively, and the mean and maximum values were similar between the two hydrodynamic models. For \( M_y \), the maximum value was found around – 10 m, and it degreased towards the edges of the structure. The distribution of...
the mean values of $M_y$ was similar between the two hydrodynamic models, while the Morison’s equation gave larger values for the maximum $M_y$ along the whole height of the structure.

![Figure 9](image-url) Comparison of the amplitude spectra calculated using proposed method and the Morison’s equation in (a) Surge, (b) Heave, and (c) Pitch direction

![Figure 10](image-url) Distribution of the sectional loads under the operating condition for (a) $F_x$, (b) $F_y$, (c) $M_x$, and (d) $M_y$

4.4. Calculation cost

The CPU times used for the calculations performed in this study are presented in Tables 6. The CPU used was Intel (R) Core (TM) i7-2640 operating in 2.80GHz. RAM was 8.00 GB and the OS was Windows 7 64 bit. It is seen from the table that generally the calculation cases with an operating wind turbine required about 3 to 15 times the time compared to the case without wind. Comparing the calculation time between the two hydrodynamic models, it is seen that the proposed method combining the potential flow theory and the flexible structural model required 4 times the calculation time of the Morison’s equation model for cases with only waves, and 1.2 times the calculation time of the Morison’s equation for cases with the wind turbine in operational condition.

| Table 6 CPU times required for 1 hour simulation | Potential Flow Theory (Rigid mode only) | Morison’s Equation |
|-----------------------------------------------|----------------------------------------|--------------------|
| Irregular wave without wind                   | 179.4 min                              | 43.95 min          |
| Irregular wave with operational wind turbine  | 875.4 min                              | 739.5 min          |
5. Conclusions
In this study, the potential flow theory was implemented to the global analysis of the flexible platforms for floating offshore wind turbines. Following conclusions were drawn.

a. The framework implementing the potential flow hydrodynamics to the time-domain analysis of the three-dimensional frame model for offshore wind turbines was developed using modal decomposition for the hydrodynamic evaluations. The method utilized the assumption that the number of modes can be limited to those with larger contributions, which can lead to the reduction of the calculation cost.

b. A spar-type floating offshore wind turbine was modelled to verify the implementation of the developed formulations. Results showed that the implemented method had shown to be effective in predicting the dynamic motion and deformation of floating offshore wind turbines under combined wind and wave loads.

c. For the cases of the irregular waves, the results obtained with potential flow hydrodynamics showed smaller responses than the results from the Morison’s equation in the natural frequencies of rigid body motion. This may be attributed to the steady and low frequency external forces introduced in the Morison’s equation by considering the instantaneous position of the floater.

The developed tool can be particularly effective when the response is not represented by a sum of linear responses to the respective loads in such cases as extreme floater behaviour moored by non-linear mooring, transient response in start-up and shut-down process, accidental loads, etc.

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