Numerical Modelling of the MIT/NREL TLP Wind Turbine and Comparison with the Experimental Results

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Abstract. In this study, numerical analysis of a tension leg platform wind turbine is conducted and the responses with focus on surge motions and tendon tension are compared with available experimental test data. The main scope of the study is to establish the numerical model for which the damping coefficients for rigid-body motions are tuned based on the comparison of the sway free decay test results (natural periods and damping ratios) between the numerical and the experimental studies. The differences between the test model properties and the numerical model information have been discussed. Numerical model tuning with available test data resulted with relatively good accordance but also slight to moderate differences in the responses. These differences are credited for the uncertainties in the model testing and the solution methodology of the numerical model. Numerical study is under development with regular and irregular wave analyses and analyses including wind excitation.

1. Introduction
Coupled nonlinear hydrodynamic and aerodynamic responses make the design of floating wind turbines challenging. Design of the floating platforms originates from the offshore oil & gas industry. Several platform concepts have been continuously studied in the last two decades. Tension Leg Platform (TLP) achieves stability by the mooring line tension. Tracy [1] proposed the MIT TLP design and pointed out that the TLP is an attractive choice because of low accelerations and negligible heave and pitch motions. Matha [2] enhanced the design by Tracy [1] and proposed the MIT / NREL TLP design for offshore wind applications.

The complicated responses of floating wind turbines can be observed either by monitoring the prototype in the ocean by performing laboratory model tests or by conducting simulations with numerical methods. Jonkman and Buhl Jr. [3] constructed the “FAST” code (Fatigue, Aerodynamics, Structures, and Turbulence) for the numerical analysis of wind turbines. Jonkman et al. [4] developed and proposed a 5MW Horizontal Axis Wind Turbine (HAWT) at National Renewable Energy Laboratory (NREL) as a standard and a base project for future studies. With the increasing demand for offshore wind harvesting, Bak et al [5] introduced the reference 10 MW turbine design. Bachynski [6] developed a parametrical study on the design of TLP wind turbines and contributed to the development of the numerical analysis tools, SIMO-RIFLEX-AeroDyn.

Numerical analysis tools need to be calibrated and verified by laboratory testing or prototype data. In 2012, an experimental test campaign was conducted on the 5MW NREL standard turbine within the
Hydralab IV Integrated Infrastructure Initiative [7]. Tests were conducted with both a spar buoy floater and a TLP floater. Different load cases under wind and wave loading were considered. Natural frequencies of the structure were measured by free vibration tests. Regular and irregular wave tests were performed under cut-in, rated and cut-out wind speed conditions. Experimental and numerical data from spar buoy tests have been discussed by Tomasicchio et al. [8][9].

This paper gives insight to the tuning of a TLP wind turbine numerical model with the available experimental data. Hydrodynamic damping coefficients are utilized to tune the numerical model. Experimental model, test procedure, theoretical background, numerical model, numerical free decay analyses are discussed respectively. Free decay responses are processed and compared in terms of natural periods and damping ratios.

2. Experimental Data

Experimental data are gathered from the results of an experimental test campaign, conducted within the Hydralab IV Integrated Infrastructure Initiative [7]. Test campaign was carried out in the offshore wave basin facility of the Danish Hydraulic Institute (DHI) in Hørsholm, Denmark in 2012.

Main purposes of the tests was to understand the responses of two floating offshore wind turbine models under different ocean conditions and to create a database for numerical model calibration and verification purposes [7]. Tension Leg Platform (TLP) and spar buoy floaters were selected for this purpose. Musci [10] and Tomasicchio et al. [8][9] reported and discussed the behavior of the spar buoy model. TLP model test results have been reported and discussed by Armenio [11], Riefolo et al.[12]. This study focuses on understanding the behavior of TLP wind turbines with numerical simulations. Experimental test data is used to tune the numerical model.

2.1. TLP Model

The TLP model platform is a 1:40 Froude scaled model of the MIT/NREL TLP standard design [2]. Likewise, the spar buoy model platform is the 1:40 Froude scaled model of the OC3-Hywind spar buoy standard design [13]. Both floaters were supporting the NREL 5-MW baseline wind turbine[4]. Rotor blades and the tower were also 1:40 Froude scaled. The TLP model is shown in Figure 1.

![TLP model with instrumentation and mooring layout](image)

Figure 1. TLP model with instrumentation and mooring layout

Tower and the blades were designed as rigid. The tower was made of plastic material. The blades were constructed from fiber glass material. The TLP model platform was constructed out of plastic material, but the platform at the model scale is considered rigid so that the flexibility in the platform is neglected. For ballasting, lead bars and small lead spheres were placed at the bottom of the structure. Four mooring
lines connected the structure to the basin base. A steel plate connected the four mooring legs (spokes, pontoons) to the structure. The spokes were made of aluminum. An internal mast was used to connect the four legs to the base of the floater. At the end of each leg, springs with spring coefficients ranging from 10.3 N/mm to 13.08 N/mm (model scale) were attached. The mooring lines used were 8 mm wires composed of an impregnated Vectran fiber core with a Polyester coating.

| Table 1. Properties of the TLP Model [7]. |
|------------------------------------------|
| Property                                | Full Scale | Scaled Model (1/40) | Unit |
| Platform diameter                       | 18         | 0.450               | m    |
| Platform Draft                          | 47.89      | 1.197               | m    |
| Radius to fairleads                     | 27         | 0.675               | m    |
| Depth to fairleads                      | 47.89      | 1.197               | m    |
| CM location below still water level     | 40.61      | 1.015               | m    |
| Tower height (hub level)                | 90         | 2.25                | m    |
| Tower mass                              | 347500     | 5.297               | kg   |
| Platform mass (including ballast)       | 8600000    | 131.098             | kg   |
| Water displacement                      | 12180      | 0.190               | m³   |
| Roll mass moment inertia                | 5.72E8     | 5.446               | kg*m²|
| Upstretched line length                 | 151.70     | 3.793               | m    |

2.2. Wave Basin and Instrumentation

Model tests were performed at the DHI Offshore Wave Basin in Denmark. The wave basin is 20 m long, 30 m wide, and has a water depth of 3 m and a pit of 6 m. The wave maker is equipped with 60 individually controlled flaps, able to generate regular and irregular waves. A parabolic wave absorber located opposite to the wave maker has minimized reflection. Six wave gauges around the structure, five wave gauges before the structure measured wave heights.

A 6DoF marine track system followed the rigid body motion of the TLP in terms of displacements (surge, sway, heave) and rotations (roll, pitch, yaw). The system is based on two cameras emitting infrared light. Five passive spherical markers, 40 mm in diameter, reflect the infrared light and were placed on a frame mounted at the tower base. Six DoF displacement data was directly transferred through an analog output to the main data acquisition system. The DHI Wave Synthesizer synchronized all the sensors. Sampling took place at 40 Hz. The test duration for regular and irregular waves is equal to 3 and 10 minutes, respectively.

Instrumentation on the model is depicted in Figure 1. The six component force gauge was placed at the base of the tower, measuring the forces along and moments around the x, y and z-axes. Similarly, the four component force gauge was placed between the tower top and rotor nacelle assembly (RNA) that measured the forces along and moments around x and y-axes. An accelerometer was attached to the six component force gauge at the tower base and two accelerometers were placed underneath the nacelle. S-type load cells, which assembles the serial connection between the springs and the mooring ropes had a maximum capacity of 9810 N.

Rotor blades were geometrically scaled according to Froude scaling and were not capable of representing the accurate thrust force. According to that, tests with wind loading was accomplished by means of a static wind loading technique. No wind flow was generated inside the wave basin. An electric
motor inside the RNA casing provided the rotation of the rotor which accounts also for gyroscopic effects. A potentiometer adjusted the rotational speed and the rotation was kept constant during the tests representing the mean wind speed at 11.4 m/s. Artificial rotation of the rotor resulted with a thrust force of 4 N. To reach the target mean thrust force, an additional static and equivalent horizontal force was applied by a weight attached to the nacelle via a rope.

Some information on the instrumentation used on the scaled model turbine is summarized in Table 2.

Table 2. Instruments on the TLP Model [7].

| Number | Type of Instrument         | Position                                      |
|--------|---------------------------|-----------------------------------------------|
| 1      | Wave gauges               | Around the structure                          |
| 1      | ADV                       | Around the structure                          |
| 1      | 6 DoF force gauge         | Between the tower and the platform            |
| 1      | 4 DoF force gauge         | Between the tower and nacelle                |
| 3      | Accelerometers            | Nacelle, tower top, tower bottom             |
| 1      | 6 DoF Marine track system | -                                             |
| 4      | Load cells at tendons     | 1 per mooring line                            |
| 2      | Pressure gauges           | Submerged part of the structure               |
| 2      | High Speed Camera         | -                                             |

2.3. Test Information

Free decay tests with and without the mooring lines, under regular and irregular wave, with and without wind excitation were performed during the experimental campaign. It should be noted that the only free decay test with tension lines is the sway free decay test, Test #1271. During Test #1271, the complete model, including the stationary rotor was displaced by 7.91 m in sway direction. Sway motion was also accompanied with a maximum 0.194 m sink in heave direction. The measurement was taken for 372 s in full scale. Regular and irregular wave tests were performed in the following conditions: 1 - dynamics of the floating structure was studied first with the stationary rotor (NR); 2 - operational conditions were simulated with combined rotation and wave loading (R); 3 - extreme conditions were simulated by extreme wave conditions (1/50 years) with the rotor being stopped. Further information on the test conditions is given by Armenio and D’Alessandro [7].

3. Numerical Study

Numerical computations are performed in Fatigue, Aerodynamics, Structures and Turbulence (FAST v8) code [3]. Aerodynamic loads, second order wave loads, platform flexibility are not considered in this study. Support platform is modelled as a six DoF rigid body with three small rotational displacements. The numerical model is illustrated in Figure 2 with the Degrees of Freedom and coordinate system.
Numerical analyses have been carried out in full scale dimensions. Properties of the numerical model and comparison with the test model is given in Table 3.

### Table 3. Numerical model and experimental model properties

|                | Numerical model | Test model |
|----------------|-----------------|------------|
| Floater        | Rigid           | Rigid      |
| Tendons        | Dynamic module: MoorDyn | Spring + ropes |
| Tower          | Flexible        | Rigid      |
| Rotor          | Flexible & Parked | Rigid & Parked |

### 3.1. Model & theory

The FAST code composes of sub modules. In particular, HydroDyn is used to solve the Hydrodynamics problem; MoorDyn is for dynamic mooring line solutions. AeroDyn deals with aerodynamics problem. HydroDyn module is responsible for the calculation of the hydrodynamic loads on the platform, using the linear theory [14]. Linearized hydrodynamics assumption implies that the amplitudes of the incident waves are much smaller than the wave lengths. This assumption allows use of the Airy wave theory. Linearized hydrodynamics also states that the translational displacements of the support platform are small relative to the size of the body. This permits the hydrodynamic problem to be split into three separate problems: Radiation, Diffraction, and Hydrostatics. As a result of linearization, superposition principles can be applied [14].

The total external load on the platform is expressed in the time domain such as:

$$F_{i}^{\text{platform}} = -A_{ij}\ddot{y}_j + F_{i}^{\text{hydro}} + F_{i}^{\text{lines}} = M_{ij}\ddot{y}_j$$  \hspace{1cm} (1)

where:

- $A_{ij}$: $i^{th}$ and $j^{th}$ component of the impulsive hydrodynamic-added-mass matrix,
- $F_{i}^{\text{hydro}}$: applied hydrodynamic load on the platform excluding added mass forces,
- $F_{i}^{\text{lines}}$: $i^{th}$ component of the load applied to the platform by the mooring lines
i and j : support platform degrees of freedom. 1 to 6: surge, sway, heave, roll, pitch, yaw.

The total load on the support platform from the contribution of all the mooring lines, which is a nonlinear forcing, is expressed in Equation 2 in a simplified way due to the relatively small motions, where mooring inertia and damping were ignored:

\[ F_{i}^{\text{Lines}} = -C_{ij}^{\text{Lines}} \eta_j \]  
\[ F_{i} \]

where:

\[ C_{ij}^{\text{Lines}} \] : \(i^{th}\) and \(j^{th}\) component of the linearized restoring matrix from all mooring lines.

The hydrodynamic loading on the platform, \(F_{i}^{\text{Hydro}}\), is a combination of inertia (due to added mass effect), linear damping (due to radiation), restoring (due to buoyancy effect), incident-wave excitation (diffraction), sea current and nonlinear effects, such as viscous drag \[ (14) \]. The total excitation from incident waves, total hydrostatic loading, hydrodynamic added mass and damping loads on the platform is given as:

\[ F_{i}^{\text{Hydro}} = F_{i}^{\text{Waves}} - C_{ij}^{\text{Hst}} \eta - \int_{0}^{t} K_{ij}(t - \tau) \eta_j(\tau) d\tau \]  
\[ F_{i} \]

First term in Equation 3, \(F_{i}^{\text{Waves}}\), represents the total excitation load on the support platform from first order incident waves and is related to the incident wave elevation, \(\zeta\). The second term represents the load contribution from hydrostatics, that accounts for the change in the hydrostatic forces and moments resulting from the displacements of the platform. The final term in Equation 3 is the convolution integral representing the load contribution from wave-radiation damping and also an additional contribution from added mass that is not accounted for in \(A_{ij}\). From Equation 1,2 and 3, the wave load on the platform can be expressed in:

\[ (M_{ij} + A_{ij}) \ddot{\eta}_j + \int_{0}^{t} K_{ij}(t - \tau) \dot{\eta}_j(\tau) d\tau + (C_{ij}^{\text{Hst}} + C_{ij}^{\text{Lines}}) \eta_j = F_{i}^{\text{wave}} \]  
\[ F_{i} \]

HydroDyn solves the linearized hydrodynamics problem in the time domain. The summary of the HydroDyn calculation procedure is given in Figure 3. For complex platform geometries, the hydrodynamic coefficients can be obtained in the frequency domain by numerical-panel codes such as WAMIT (Wave Analysis at MIT) \[ (14) \]. WAMIT \[ (15) \] works as a pre-processor and exports the hydrodynamic coefficients to HydroDyn. Nonlinear viscous-drag loading is computed in HydroDyn by using Morison’s representation, in conjunction with strip theory. In hydrodynamic strip theory, the structure is split into a number of elements or strips, where two-dimensional properties, such as added-mass and viscous-drag coefficients are used to determine the overall three-dimensional loading on the structure \[ (14) \]. Jonkman \[ (14) \] expresses the total external load acting on the platform for surge and sway modes of motion \((i=1,2)\) according to Morison’s representation by integrating the load acting on each strip, which is given as:

\[ dF_{i}^{\text{platform}}(t, z) = -C_{A} \rho \left( \frac{\pi D^2}{4} dz \right) \dot{\eta}_{i}(z) + (1 + C_{A}) \rho \left( \frac{\pi D^2}{4} dz \right) a_{i}(t, 0, 0, z) \]
\[ + \frac{1}{2} C_{D} \rho (D dz) [v_{i}(t, 0, 0, z) - \dot{\eta}_{i}(z)] v(t, 0, 0, z) - \ddot{\eta}_{i}(z) \]
\[ F_{i} \]

where \(D\) is the diameter of the platform, \(dz\) is the length of the differential strip of the platform, \(C_{A}\) and \(C_{D}\) are the normalized hydrodynamic -added mass and viscous drag coefficients. Last two terms
constitute \( dF_i^{\text{Viscous}} \), which is the viscous-drag load acting on the strip of each cylinder. \( v_i \) and \( a_i \) are the components of the undisturbed fluid-particle velocity and acceleration in the direction of DoF \( i \). The term \( \pi D^2 dz / 4 \) is the displaced volume of fluid for the strip of the cylinder. \( Ddz \) is the frontal area of the cylinder.

HydroDyn allows for multiple approaches for calculating the hydrodynamic loads on a structure [16]: (a) a potential-flow approach with frequency to time-domain transformations that implements a panel code for hydrodynamic coefficients, (b) a strip-theory approach, where the user defines the dynamic pressure, added-mass, viscous-drag coefficients, and (c) a hybrid approach in which the potential-flow solution is combined with strip theory solution. In this study the hybrid approach (c) is chosen in order to take into account the viscous damping effects. In the potential flow theory solution, added mass, diffraction, Froude – Krylov, radiation damping loads are calculated on the wet surfaces of the structure by using the hydrodynamic coefficients imported from WAMIT. Moreover, hydrodynamic coefficients calculated by Matha [2] are used. Within this approach, pontoons are not considered in the potential flow solution part but they were taken into account in the strip theory computations. For the strip theory approach, the platform is divided into 100 strips in vertical direction. Pontoons are divided into 54 strips in horizontal direction. Viscous drag coefficient in transverse direction, \( C_d \) for the platform and the pontoons is calculated as 0.7 considering the circular cross sections according to [17]. Added mass coefficient, \( C_m \) is not considered in the strip theory solution since it has already been accounted for in the potential flow theory calculations. HydroDyn also allows the application of additional preloading, stiffness and damping to the model. This allows the ability to tune the model to match damping with experimental results.

![Diagram of HydroDyn calculation procedure](image)

Figure 3. Summary of the HydroDyn calculation procedure. [14]

FAST allows quasi-static or dynamic solutions for mooring line computations. In order to improve the accuracy of the overall damping response, which is also dependent on the mooring dynamics, MoorDyn [18] is selected for the computations. MoorDyn is based on a lumped-mass approach and considers the mooring stiffness, inertia, weight, buoyancy and friction between mooring cable and the seabed. In addition to the quasi static module MAP++ [19], MoorDyn considers additionally the line damping and the viscous drag forces on the line.
Four pairs of lines, in total 8 lines are defined in MoorDyn. Unstretched line length is 151.73 m. Line stiffness \((EA)\), which is the product of elasticity modulus and cross-sectional area is 1.12E9 N. Each line is divided into 20 mass-spring-damper elements (Maxwell links). Line internal damping ratio is set to 95% of critical damping in order to avoid the artificial resonance that can be created by the discretization. On the other hand, in order to take into account the damping effects, viscous drag coefficient in transverse direction, \(C_{dn}\) is considered as 1.0 and transverse added mass coefficient, \(C_{am}\), is chosen as 1.0 considering the circular cross sections according to [17].

3.2. Model tuning & free decay analyses
The numerical model is tuned according to the free decay tests. The first set of free decay tests were conducted on the TLP model without the tendons. The only free decay test available with lines under tension is the sway free decay test. Free decay analyses have been conducted in full scale applying an initial displacement to the structure and then releasing it. The analyses duration is 400 s, full scale. The time step for the analyses is constant and equal to 0.01 s. From free decay response oscillation periods and damping ratios were derived. As a reference, the natural period was also calculated as:

\[
T = 2\pi \sqrt{\frac{M + A(\omega)}{C + K}}
\]  

where

- \(M\): platform mass,
- \(A(\omega)\): added mass,
- \(C\): hydrostatic stiffness of the platform (zero for sway degree of freedom),
- \(K\): tendon stiffness.

The damping ratio is calculated from the time histories, using the logarithmic decrement method as:

\[
\zeta = \frac{1}{2\pi j} \ln \frac{u_i}{u_{i+j}}
\]

Viscous drag forces on the platform in transverse direction is considered in the computations with the viscous drag coefficient, \(C_d\). The numerical model is tuned by adjusting the damping in surge, sway, heave, roll and pitch degree of freedoms. Additional linear damping controls in HydroDyn is utilized to match the damping behaviour between the numerical model and the test model. Additional linear damping is accounted for the viscous drag of the platform in surge and sway directions and the structural damping in heave direction that is generated by vibration of the tendons, which is not accounted in the computations.

4. Results
All the numerical analyses have been conducted in full scale. Model test results are scaled up and presented in full scale. Numerical model tuning with free decay comparisons, results of sway sensitivity to initial sway are discussed respectively.

4.1. Free decay comparisons
Test #1207 heave free decay test (without tendons) #3 resulted in a period value of 12.9 s. Eq. 6 resulted in 12.93 s for the un-tensioned turbine heave oscillation period.
Test #1271, sway free decay test with tendons was conducted with an initial sway of 7.9 m and an initial heave of -0.194 m. Sway response comparison of the model and the test is given in Figure 4, where test data is represented with the solid red curve, the analysis with additional damping and the analysis
without additional damping is represented with the solid and dashed blue curves, respectively. Resulting periods are 63.8 s and 56.3 s for the analysis and the test, respectively. Damping in the test is 5.02%. From the analyses without additional damping it resulted in 3.35%. Analysis with additional 2.5% damping in sway direction (8.96E4 N/(m/s)) resulted in 5.07% of damping and accepted for the tuned model.

![Graph](image)

**Figure 4. Sway free decay analysis and test comparison**

In the wave basin sway free decay test, heave resonant motions were also excited. Resulting heave response is coupled with the sway response. One sway oscillation is accompanied by two heave oscillations, where the structure sinks and rises back to mean water level. Accordingly, the response consists of a low-frequency response which is twice the sway natural frequency and a high-frequency response, which is the pure heave frequency. To visualize this phenomenon, sway and heave responses from the sway free decay analysis are given in Figure 5, where sway response is unitized, heave response is scaled up 5 times for a better presentation. Figure 5 compares sway, total heave, low frequency component of heave responses, respectively. The low frequency response is obtained by band-pass filtering between 0.001 Hz and 0.20 Hz, according to the spectrum in Figure 6. Calculation of the heave period of the tensioned turbine with Eq. 6 resulted in 2.64 s. The resulting periods are 2.63 s and 2.94 s respectively for numerical analysis and the test with a 12% of difference. The difference in the tensioned platform heave natural period is credited directly to the mooring line stiffness since the un-tensioned test period agrees with Eq. 6 result. Analysis of heave response with additional 1% of the critical damping in heave (5.16 E5 N/(m/s)) resulted in 1.03 % damping in heave oscillations. Heave damping in model test is obtained as 1.05%. Resulting power spectrum and the time series are given in Figure 6 and Figure 7, respectively. Figure 6 demonstrates Welch’s power spectral density estimate of heave time history from the sway free decay test. Units of the PSD estimate are in squared magnitude units of the heave time series data per unit frequency (m²/Hz). Number of overlapped samples is considered as the default value and focused to obtain 50% overlap between segments. Hamming window is also evaluated with the default value in a way to obtain eight segments of heave time series with the overlapped samples. Resulting high frequency amplitudes for the test and analysis are 6 cm at 0.34 Hz and 5.1 cm at 0.38 Hz respectively.
Figure 5. TLP typical sway and heave responses.

Figure 6. Sway free decay test heave spectrum.

Figure 7. Heave response high frequency component
Tower base moment response around the x-axis is given in Figure 8 and Figure 9. It is useful to remind that the tower was stiff in the model tests but flexible in the numerical analyses. Figure 8 shows the spectrum of the tower base bending moment; the low-frequency component at 0.01625 Hz (61.5 s) and 0.0174 Hz (57.4 s) for the analysis and the model test that are governed by the sway motion; the high-frequency component at 0.206 Hz (4.85 s), 0.180 Hz (5.56 s) for the high frequency part, respectively, is the effect of the roll motion. Since the structure is symmetric around the x and y-axes, same findings are also valid for surge and pitch DoFs. Accompanying damping ratios for the high frequency component are 3.27% and 5.25% for the analyses and the model test, respectively. Matha [2] reports the pitch + 1st tower bending modal period as 4.52 s. Pitch free decay analysis with 2 degrees of initial sway resulted with a pitch natural period of 4.92 s. Due to the lack of pitch free decay test, 1% of the critical pitch damping was added in the analyses, to be consistent with heave damping. This resulted in a total pitch damping of 3.73%.

![Figure 8. Tower base moment (around x-axis) spectrum](image)

![Figure 9. High-frequency component of tower base moment](image)

In addition to the results of the sway free decay test, pitch free decay analysis is performed with the tuned model. Period and damping ratio results are given in Table 4 with the results discussed through time histories and spectra. Pitch response is coupled with tower bending 1st mode and period is obtained of 4.92 s from the pitch free decay analysis. The reference value for pitch + tower 1st bending mode is 4.52 s [2].
4.2. Highlights on the differences

The results given in the previous sections indicate possible uncertainties in the wave basin test setup, which can be accounted for simply an offset of the acquisition system or possible error sources in the model design and numerical modelling. In this section a short summary of the differences and of the possible reasons for the differences are given.

Untensioned heave free decay test results, which are coincident with hand calculation values, reveals that the scaled mass of the test model is correct and directs the problem towards the tension line stiffness. Reference sway period value of 60.6 s [2] is higher than 56.3 s, which is the period obtained from the laboratory test and smaller than 63.8 s, which is the value obtained from the analyses. Sway natural period measured from the free decay test is 11.8% lower than the numerical analyses result. On the other hand, the tension line extensional stiffness of the scaled test model and that of the numerical model are 36% lower than the reference value. Heave period of 2.64 s, which is obtained from the sway free decay analysis is close to the 2.62 s, which is the value obtained by Equation 6. Surge natural period by using the test model tendon stiffness with Equation 6, resulted with 65.2 s which is 2.1% higher than the numerical analyses result.

According to the values above, it is understood that the scaled model test, which has a lower tendon stiffness compared to the reference structure, resulted with a stiffer surge response with respect to the reference values and also to the numerical model. It is useful to remember that the geometry, mass, stiffness, initial conditions of the sway free decay test and the analysis are the same.

Table 4. Natural periods and damping ratios

|                  | Period (s) | Damping (%) |
|------------------|------------|-------------|
|                  | Analysis   | Test        | Analysis | Test    |
| sway             | 63.85      | 56.33       | 5.07%    | 5.02%   |
| heave            | 2.62       | 2.92        | 1.03%    | 1.05%   |
| T2 high          | 2.63       | 2.94        | 1.05%    | 1.07%   |
| T2 low           | 63.18      | 56.44       | 5.69%    | 7.11%   |
| T1 high          | 2.63       | 2.93        | 1.14%    | 1.08%   |
| pitch&           | 4.92       | NA          | 3.73%    | NA      |
| 1st tower        |            |             |          |         |
| Mbots-x          | 4.85       | 5.56        | 3.27%    | 5.25%   |

DeepCwind consortium conducted tests on three different floating platforms in 2011, and a similar cable bundle connection that is visible in Figure 10 was reported [21]. It was also reported that the cable bundle added mass to the system (16%) and affected the dynamics of the system. It was reported that [22] the cables employed during testing deviated up the main parameters of the system up to 12%. A comparative study Error! Reference source not found. on DeepCwind consortium tests with numerical computations considered the stiffness of the cable bundle, and employed 7.39 kN/m in full scale as additional stiffness in surge- direction to the numerical model. It was also reported that the additional mass on the tower was evenly distributed and considered in the analyses.

In conclusion to the discussion, the authors have an impression on the fact that the sensor cables attached to the scaled TLP model in the experimental study might have altered the structural characteristics such as the stiffness and mass distribution, which may lead to the differences in the natural periods.
4.3. Sensitivity of damping to initial sway

The reference sway natural period is 60.6 s. [2]. Relatively high initial sway value in Figure 4 is investigated with a sensitivity study by conducting free decay analyses with changing initial sway values. Resulting time histories from five different analyses are given in Figure 11, which shows that decreasing the initial sway did not affect period but affected damping ratio. This indicates that the restoring stiffness from the tendons for the considered sway motions is linear, while the damping effect is dominated by the viscous drag damping for larger initial sway motions and is linear and dominated by potential damping for smaller sway motions. The total damping is the sum of the actual linear damping and the linearized quadratic damping. It is clear from Figure 11 that amplitude of all five analyses reduced to less than 10% of the initial amplitude with 15 sway oscillations. Accordingly, damping ratios are calculated by using logarithmic decrement method and reported for each cycle of the motion in Table 5. It is calculated for the case with 7.91 m initial offset that the total linear damping decreases from 7.27% to 2.7% cycle by cycle. Results for the case with 0.91 m of initial offset are 3.27% for the first cycle and 2.62% for the last cycle. This can be explained with at the beginning of the sway decay with larger initial offset, the damping is governed by viscous drag damping (quadratic part of the damping) with a higher damping ratio since the motions are large; on the other hand, potential damping (actual linear damping) dominates the behavior for the following stages of the smaller motions. From the decay curve, one may directly estimate the linear and quadratic damping coefficients and compare them. However, this is not done here. Only equivalent linear damping coefficients are obtained and compared here to show the dominance of the damping for different magnitudes of motions.

Table 5. Damping ratio results

| $y_0$ (m) | Cycle | 1   | 2   | 3   | 4   | 5   | 6   | 7   | 8   | 9   | 10  | 11  | 12  | 13  | 14  | 15  |
|----------|-------|-----|-----|-----|-----|-----|-----|-----|-----|-----|-----|-----|-----|-----|-----|-----|
| 7.91     | 7.27% | 5.75% | 4.81% | 4.29% | 3.91% | 3.64% | 3.38% | 3.25% | 3.12% | 3.03% | 2.94% | 2.86% | 2.84% | 2.81% | 2.77% |
| 5.91     | 6.22% | 5.15% | 4.53% | 3.95% | 3.69% | 3.46% | 3.29% | 3.16% | 3.06% | 2.97% | 2.90% | 2.84% | 2.80% | 2.75% | 2.74% |
| 3.91     | 5.09% | 4.45% | 4.07% | 3.59% | 3.46% | 3.28% | 3.14% | 3.04% | 2.97% | 2.91% | 2.84% | 2.79% | 2.76% | 2.71% | 2.72% |
| 1.91     | 3.90% | 3.56% | 3.40% | 3.18% | 3.09% | 3.01% | 2.93% | 2.83% | 2.82% | 2.74% | 2.73% | 2.69% | 2.67% | 2.65% |
| 0.91     | 3.27% | 3.07% | 2.97% | 2.90% | 2.84% | 2.79% | 2.76% | 2.73% | 2.70% | 2.70% | 2.64% | 2.67% | 2.61% | 2.62% | 2.62% |

To visualize the effect of quadratic damping in sway oscillations, sway damping calculated from each pair of successive cycles from Figure 11 is given in Figure 12. **Reference source not found.**
Each case with different initial sway offset in Figure 11 is again represented with the same colour in Figure 12. Even though the logarithmic scale fails to show the linear trend in damping, horizontal axis of Figure 12 is given in logarithmic scale for a better visualization of the results of the cases with lower offsets. It should be noted that all the points lie on one curve, indicating that the initial conditions are not influencing the trend.

Figure 11. Initial sway magnitude sensitivity time history

Figure 12. Total sway damping

5. Conclusions and future work
In this study, MIT / NREL TLP floating wind turbine is numerically modelled and the preliminary responses are compared with the available test data from offshore wave basin tests. Focus for this preliminary stage is on understanding the uncertainties in the test and tuning the numerical model for a better agreement with respect to the basic hydrodynamic behaviour of the model. Numerical model tuning with the available test data resulted in relatively good accordance but also slight to moderate differences in the responses. These differences are credited for the uncertainties and test setup in the model testing data and numerical model assumptions. It is worth mentioning that model tuning is achieved with the only available free decay test, which is the sway free decay test. Relatively stiffer sway response of the test model resulted in a stiffer response in sway but softer response in heave. More free decay tests are needed to have a more accurate numerical model. In addition, a relatively soft pitch natural period is the motivation for a better design in pitch. A numerical study is under development with regular, irregular wave analyses and analyses including wind excitation.

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