

Article

Effects of Floater Equivalent Stiffness and Damping on the Dynamic Responses of a 22 MW Two-Body Floating Wind Turbine

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Abstract

The upscaling of floating wind turbines and the lightweight design of floating support structures make floater flexibility increasingly important for the dynamic response and load transfer of two-body floating platforms. This study investigates the influence of upper-floater flexibility on Tsemi, a previously proposed suspended-ballast two-body floating platform supporting the IEA 22 MW reference wind turbine, using a fully coupled multi-body flexible-joint model. The floater is discretized into multiple rigid bodies connected by damped six-degree-of-freedom joints, which represent equivalent flexibility and local energy dissipation. The model is evaluated through member-level response comparisons, global modal analysis, and nearly rigid limiting-case simulations. Results show that equivalent floater stiffness strongly affects wave-frequency responses. Reducing the stiffness shifts the platform pitch period from 25.6 s to 30.6 s and significantly amplifies structural and tendon loads under extreme environmental conditions. The standard deviations of tower-top acceleration and tower-base bending moment increase by 75.8% and 72.1%, respectively, and the maximum effective tension in the upwave tendon increases from 19.93 MN to 32.50 MN, exceeding the tendon minimum breaking load. For the considered low-stiffness case, equivalent damping reduces this tension to 21.77 MN. These results suggest that neglecting floater flexibility may underestimate tower responses and extreme tendon loads, while equivalent damping can mitigate wave-frequency amplification under low-stiffness conditions.

Keywords: IEA 22 MW wind turbine; two-body floating platform; multi-body flexible-joint model; fully coupled analysis



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1. Introduction

Deep-water and far-offshore regions provide abundant and relatively steady wind resources. Their development has therefore become an important direction for the large-scale deployment of offshore wind energy. When the water depth exceeds about 50–60 m, floating platforms generally offer better engineering feasibility and economic performance than fixed-bottom foundations [1,2]. To reduce the levelized cost of energy, the rated power of offshore wind turbines has continued to increase [3,4]. This trend leads to higher nacelle-rotor assembly mass and larger environmental loads. It also makes the design of floating platforms and the prediction of their dynamic responses more challenging. The suspended two-body floating platform, hereafter referred to as the two-body floating platform, has

been considered a potential concept for supporting large-capacity floating wind turbines because of its advantages in wet towing and operational stability [5–7].

This type of two-body floating platform typically consists of an upper floater, a lower ballast, and a suspension system. The global stability of the system can be improved by adjusting the mass and submerged depth of the lower ballast. In recent years, considerable attention has been given to the conceptual design, multi-body coupled response, and parameter sensitivity of the suspension system for two-body floating platforms [8,9]. Dankelmann et al. [10] proposed the TELWIND two-body floating platform concept. Armesto et al. [6] developed a fully coupled time-domain model of TELWIND and validated its hydrodynamic response characteristics through model tests. For the TetraSpar platform, Borg et al. [7] provided the technical definition of the demonstration foundation. Pereyra et al. [8] investigated the effects of lower-ballast mass, submerged depth, and tendon tension on platform responses and fatigue loads. Similarly, Ward et al. [5,11] showed, through model tests and nonlinear multi-body dynamic analyses, that the lower-ballast mass and tendon stiffness can modify the multi-body modes of a suspended counterweight platform and the tension fluctuations in the tendons. However, most existing studies treat the upper floater as a rigid body. The effects of its structural flexibility and energy dissipation characteristics on system responses and load transfer have received limited attention.

As floating platforms become larger and more lightweight, their flexibility effects become more pronounced [12,13]. Zhang and Ishihara [14] showed that platform flexibility can change the natural frequencies of the tower and affect tower-base fatigue loads. Jonkman et al. [15,16] extended the capability of OpenFAST for platform flexibility modeling and member-level load analysis. Borg et al. [3,17], Luan et al. [18–20], and Ran et al. [21] further developed flexible modeling methods for large-volume floating structures based on hydroelastic modes, multi-body discretization, and potential-flow coupling. For large floating wind turbines, Zhao et al. [22] found that platform flexibility can enhance the resonance of low-frequency (LF) platform motions. Li et al. [4] indicated that platform flexibility can increase the amplitude and standard deviation of tower-base bending moment by more than 20% under certain operating conditions. More recently, Dike et al. [23,24] investigated the effects of member and local-joint flexibility on the dynamic and fatigue responses of conventional semi-submersible platforms, while Li et al. [25] developed an in-loop shell-element flexible-platform model for the IEA 15 MW VoltturnUS-S system. Collectively, these studies demonstrate that structural flexibility can influence natural characteristics, global motions, member-level loads, and fatigue responses. Nevertheless, most of these studies have focused on conventional single-integrated-body floating platforms.

Flexibility effects observed for single-body platforms may not be directly applicable to two-body floating platforms because of their more complex loading paths and coupling mechanisms [26,27]. In a two-body configuration, the upper floater is subjected to aerodynamic and wave-induced hydrodynamic loads while being coupled with the lower ballast through relative motion, tendon, and potential-flow-based hydrodynamic interactions. Moreover, equivalent damping from connecting members, joints, and local structural energy dissipation may alter response amplitudes by dissipating vibration energy [12,28]. The equivalent damping of the upper floater should therefore also be evaluated.

At present, studies on the flexibility effects of the upper floater in two-body floating platforms remain limited. Thomsen et al. [29] compared rigid-body and flexible models of TetraSpar using an elastic-beam model based on the Morison equation. They found that flexible modeling changes the natural periods of the platform and the bending moments of structural members. However, their study focused on a 3.6 MW floating platform mainly composed of slender members. It may not fully represent the scale effects and loading characteristics of large two-body floating platforms. In addition, the Morison equation has

limitations in describing wave diffraction, radiation effects, and potential-flow coupling among multiple floating bodies. A systematic investigation is therefore needed to clarify how the equivalent stiffness and equivalent damping of the upper floater affect multi-body coupled responses and load transfer in large two-body floating platforms.

To address this issue, this study applies a multi-body flexible-joint approach to a two-body floating platform supporting the IEA 22-MW reference wind turbine. The upper floater is represented by rigid modules connected through damped six-degree-of-freedom (6-DOF) flexible joints, whose stiffness and damping are specified using Timoshenko beam theory and a stiffness-proportional damping model, respectively. Potential-flow hydrodynamic coupling among the modules is incorporated through frequency-domain boundary element analysis, and the model is verified using static, modal, and nearly rigid-limit responses. The verified model is then used in a fully coupled time-domain framework to conduct a parametric study on the influence of upper-floater equivalent stiffness and damping on platform motions, inter-body relative motion, tower responses, mooring-line tensions, and tendon tensions.

2. Model Description

2.1. Two-Body Floating Platform and 22 MW Wind Turbine

The two-body floating platform considered in this study, hereafter referred to as Tsemi, is developed from the NewSemi concept previously proposed by Wu et al. [30]. The previous study focused on the hydrodynamic stability of this two-body configuration. Tsemi combines a large upper floater supporting the IEA 22 MW reference wind turbine with a suspended lower ballast. At this scale, the global flexibility of the upper floater may affect the coupled platform response, while the suspended ballast introduces relative motion and load transfer between the two bodies. These characteristics make Tsemi particularly relevant for investigating how upper-floater stiffness and damping affect the coupled platform response. To match the target site conditions, slight modifications are made to the suspension system and lower ballast. Following the UMaine VoltturnUS-S semi-submersible configuration, the upper floater consists of one central column, three outer columns, and three horizontal pontoons arranged at 120° intervals [31]. A toroidal lower ballast filled with high-density magnetite ore is connected to the upper floater by six tendons. Figure 1 shows the platform layout, principal dimensions, and global coordinate system. The coordinate origin is located at the intersection of the still water level and the centerline of the central column. This platform supports the IEA 22 MW reference wind turbine, a three-bladed, upwind, horizontal-axis turbine. Its main design parameters are listed in Table 1, while the remaining data are taken from the official report [32].

Table 1. Main properties of the IEA 22 MW reference wind turbine.

Item	Value
Rated power [MW]	22
Number of blades	3
Rotor diameter [m]	284
Hub height [m]	170
RNA mass [t]	1217.2
Tower mass [t]	1574
Cut-in/rated/cut-out wind speed [m/s]	3/12/25
Rated 1P frequency [Hz]	0.11
First fore-aft frequency [Hz]	0.20

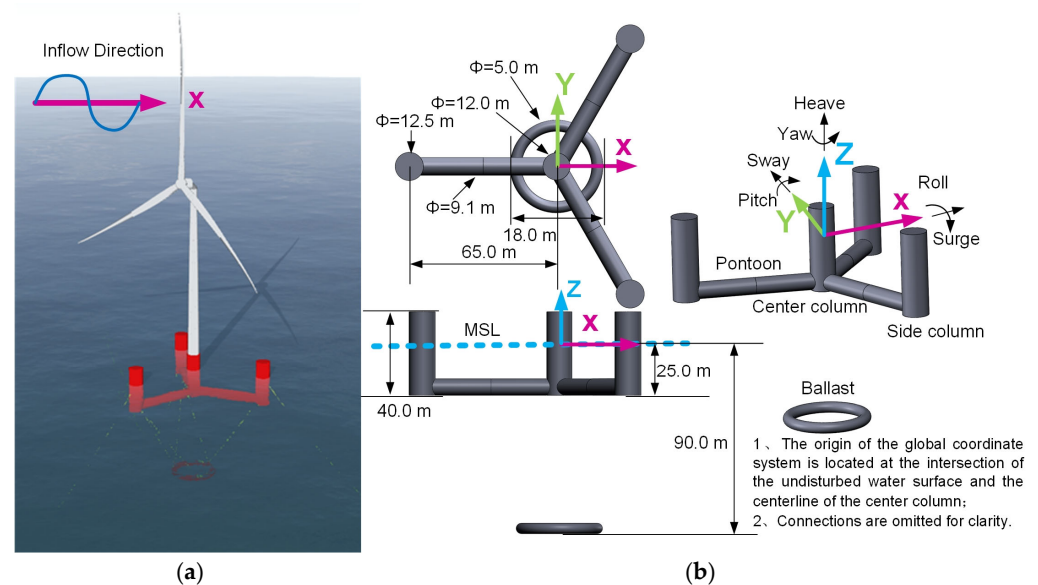


Figure 1. Schematic of the Tsemi two-body floating platform for the IEA 22 MW wind turbine: (a) overall configuration of the floating wind turbine system; (b) structural layout, principal dimensions, and global coordinate system of the two-body platform.

2.2. Multi-Body Flexible-Joint Model Configuration

Figure 2 shows the baseline rigid-body model, the multi-body flexible-joint model, and their body-fixed coordinate systems for the Tsemi floating platform. The platform position is maintained by the tendons and mooring lines, as shown in Figure 2a. In the baseline rigid-body model, both the upper floater and the lower ballast are treated as rigid bodies. In the multi-body flexible-joint model, the upper floater is discretized into four rigid modules, namely the Body Center, Body After, Body Port, and Body Stbd. Following the findings of Lu et al. [33], the number of modules was selected according to the highest structural mode of the floater expected to be excited by wave loading. Since the wave-induced response in this study is mainly governed by the first few global flexible modes, four modules were adopted to approximately capture the overall vibration characteristics of the platform. Adjacent modules are connected by damped 6-DOF flexible joints, which provide equivalent stiffness and energy dissipation characteristics for the horizontal pontoons, as shown in Figure 2b.

The center-of-gravity (CoG) coordinates of all rigid bodies and modules are specified in the global coordinate system. The local coordinate systems of the flexible joints are illustrated in Figure 2b. Both the global and joint-local coordinate systems follow the right-hand rule. To ensure a consistent comparison, the multi-body discretization preserves the total mass and assembled mass moments of inertia of the baseline rigid-body model. The component masses, CoG coordinates, and mass moments of inertia about the corresponding CoGs are listed in Tables 2 and 3.

Table 2. Basic physical parameters of the platform components.

Type	Component	Mass [t]	Draft [m]	CoG (x,y,z) [m]
Baseline rigid-body model	Floater	13,251.69	25	(0.00, 0.00, −16.93)
	Body Center	3289.01	25	(0.00, 0.00, −18.74)
	Body After	3320.89	25	(−59.96, 0.00, −16.33)
	Body Port	3320.90	25	(29.98, 51.93, −16.33)
	Body Stbd	3320.89	25	(29.98, −51.93, −16.33)
Lower ballast	Ballast	7914.06	90	(0.00, 0.00, −87.50)

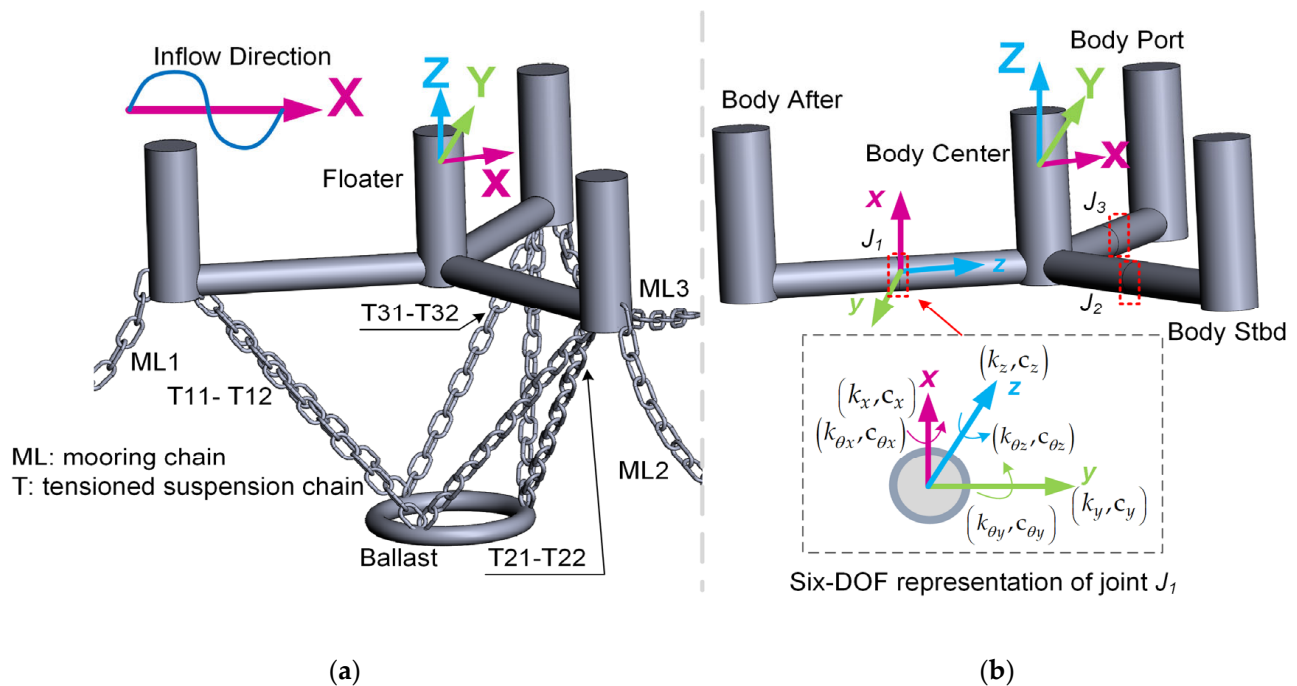


Figure 2. Schematic of the Tsemi platform: (a) baseline rigid-body model and chain arrangement; (b) multi-body flexible-joint model with the six-DOF representation of J_1 . $J_1 - J_3$ denote the flexible joints. Here, k_i and c_i denote the translational stiffness and damping components, while $k_{\theta i}$ and $c_{\theta i}$ denote the rotational stiffness and damping components, respectively ($i = x, y, z$). Chain links are enlarged for clarity and are not to scale.

Table 3. Moment of inertia parameters of the two models.

Type	Component	I_{xx} [t·m ²]	I_{yy} [t·m ²]	I_{zz} [t·m ²]	I_{xy} [t·m ²]	I_{xz} [t·m ²]	I_{yz} [t·m ²]
Baseline rigid-body model	Floater	2.01×10^7	2.01×10^7	3.80×10^7	0.00	0.00	0.00
Multi-body flexible-joint model	Body Center	8.48×10^5	8.48×10^5	1.13×10^6	0.00	0.00	0.00
	Body After	3.12×10^5	5.97×10^5	3.53×10^5	0.00	-9.99×10^4	0.00
	Body Port	5.26×10^5	3.83×10^5	3.53×10^5	1.24×10^5	5.00×10^4	8.65×10^4
	Body Stbd	5.26×10^5	3.83×10^5	3.53×10^5	-1.24×10^5	5.00×10^4	-8.65×10^4
Lower ballast	Ballast	1.31×10^6	1.31×10^6	2.60×10^6	0.00	0.00	0.00

Note: All moments of inertia are calculated about the center of gravity (CoG) of each rigid body.

2.3. Mooring and Suspension Systems

The layout of the mooring and suspension systems is shown in Figure 2a. The mooring system comprises three studless R4 catenary lines, ML1–ML3, uniformly spaced at 120° intervals. For ML1, the fairlead and anchor coordinates are (−71.3, 0, −8.1) m and (−844.0, 0, −200.0) m, respectively; those of ML2 and ML3 are obtained by rotating the ML1 coordinates by 120° and 240° about the global z-axis. The suspension system comprises six studless R4 chains, hereafter referred to as tendons, arranged in three pairs: T11–T12, T21–T22, and T31–T32. Their upper ends are connected to the aft, starboard, and port outer columns, respectively, while their lower ends are attached to the lower ballast. The corresponding geometric, physical, and mechanical properties are summarized in Table 4. To assess tendon slack risk, the instantaneous effective tension is required to remain positive, i.e., $T > 0$, for all load cases [8].

Table 4. Layout, physical, and mechanical properties of the mooring and suspension systems.

Property	Mooring System	Suspension System
Number of lines	3	6
Angle between adjacent lines	120°	-
Fairlead/Anchor depth [m]	8.1/200	25/85
Fairlead/Anchor radius [m]	71.3/844.0	58.7/18
Unstretched length [m]	837	79.21
Chain type	Studless R4	Studless R4
Nominal diameter [mm]	225	185
Dry density [kg/m]	1007.4	749.5
Minimum breaking load [kN]	36.1×10^3	27.3×10^3
Axial stiffness [kN]	4.3×10^6	3.5×10^6

Note: The fairlead and anchor depths are measured downward from the still-water level, and their radii are measured horizontally from the platform center. For the suspension system, the two entries correspond to the upper-floater and lower-ballast attachment points, respectively.

3. Theoretical Method

This study develops a rigid–flexible coupled dynamic model for the IEA 22 MW two-body floating wind turbine under combined wind, wave, and current loading. In the model, the blades, tower, tendons, and mooring lines are represented as flexible components. The lower ballast is modeled as a rigid body. The upper floater is treated as a single rigid body in the baseline rigid-body model. In the multi-body flexible-joint model, the upper floater is discretized into several rigid bodies connected by damped 6-DOF flexible joints.

3.1. Fully Coupled Time-Domain Equations of Motion

The coupled motion of the two-body floating wind turbine is solved in the time domain using OrcaFlex. For each rigid body, the equation of motion is written as

$$(\mathbf{M} + \mathbf{A}_\infty)\ddot{\mathbf{q}}(t) + \int_0^t \mathbf{K}_r(t - \tau)\dot{\mathbf{q}}(\tau)d\tau + \mathbf{C}\dot{\mathbf{q}}(t) = \mathbf{F}_{hydro}(t) + \mathbf{F}_{aero}(t) + \mathbf{F}_{moor}(t) + \mathbf{F}_{susp}(t) + \mathbf{F}_{joint}(t) \quad (1)$$

where $\mathbf{M}$, $\mathbf{A}_\infty$, $\mathbf{K}_r$, and $\mathbf{C}$ denote the structural mass matrix, infinite-frequency added-mass matrix, radiation retardation-function matrix, and hydrostatic restoring stiffness matrix, respectively. The radiation-memory effect is represented by the Cummins convolution term [34]. The hydrodynamic load $\mathbf{F}_{hydro}(t)$ is evaluated using a hybrid approach [35,36]. Potential-flow theory is used to describe wave-radiation and diffraction effects. The viscous drag contribution is included through the drag term in Morison’s equation. The drag coefficient C_d is selected according to DNV-RP-C205 [37]. As this work is a preliminary study, only first-order wave loads are considered, while second-order wave-drift forces are neglected. The aerodynamic load $\mathbf{F}_{aero}(t)$ is calculated using the blade-element momentum method. The blade-pitch and generator-torque controllers are coupled through a Bladed-style external controller interface [38]. The main control parameters are taken from the floating configuration of the IEA 22 MW reference wind turbine [32]. The mooring force $\mathbf{F}_{moor}(t)$ and suspension force $\mathbf{F}_{susp}(t)$ are obtained from the lumped-mass line models in OrcaFlex [39]. The flexible-joint force $\mathbf{F}_{joint}(t)$ is described in Section 3.2.

3.2. Multi-Body Flexible-Joint Model Formulation

To account for the flexibility of the upper floater, a lumped-parameter multi-body flexible-joint model is adopted. Following the multi-rigid-body hydroelastic concept, the upper floater is idealized as rigid modules connected by virtual flexible joints [33]. The joints provide equivalent stiffness and damping to represent the overall flexible behavior

and energy dissipation of the structure, rather than physical connections for recovering continuous deformation fields or local internal-force distributions.

Under the assumptions of small deformation and small rotation, the contribution of the flexible joints to the generalized restoring force of the system is

$$\mathbf{F}_{joint}(t) = \sum_{k=1}^{N_j} \mathbf{J}_{joint,k}^T \mathbf{f}_{joint,k}^l \quad \mathbf{f}_{joint,k}^l = -\mathbf{K}_{joint,k} \Delta \mathbf{u}_{joint,k} - \mathbf{C}_{joint,k} \Delta \dot{\mathbf{u}}_{joint,k} \quad (2)$$

where N_j is the number of flexible joints. $\mathbf{J}_{joint,k}$ is the mapping matrix from the local relative degrees of freedom of the k -th flexible joint to the system generalized coordinates. $\mathbf{f}_{joint,k}^l$ is the generalized restoring force in the local coordinate system of the flexible joint. $\mathbf{K}_{joint,k}$ and $\mathbf{C}_{joint,k}$ are the local equivalent stiffness and equivalent damping matrices of the flexible joint, respectively. $\Delta \mathbf{u}_{j,k}$ and $\Delta \dot{\mathbf{u}}_{j,k}$ are the relative displacement and velocity at the flexible joint.

The equivalent stiffness and damping matrices of the flexible joint are defined as

$$\mathbf{K}_{joint,k} = \beta \mathbf{K}_{joint,k}^{\text{ref}} \quad (3)$$

where $\mathbf{K}_{joint,k}^{\text{ref}}$ is the reference stiffness matrix of the k -th flexible joint. β is the stiffness scaling factor. It is used to parameterize changes in the equivalent stiffness of the upper floater.

The $\mathbf{K}_{joint,k}^{\text{ref}}$ is expressed as

$$\mathbf{K}_{joint,k}^{\text{ref}} = \text{diag} \left(\frac{12E_k I_{y,k}}{L_k^3(1 + \phi_{x,k})}, \frac{12E_k I_{x,k}}{L_k^3(1 + \phi_{y,k})}, \frac{E_k A_k}{L_k}, \frac{E_k I_{x,k}}{L_k}, \frac{E_k I_{y,k}}{L_k}, \frac{G_k J_k}{L_k} \right) \quad (4)$$

The first two terms are the equivalent transverse shear-bending stiffnesses in the local x - and y -directions. The third term is the axial stiffness. The last three terms are the bending rotational stiffnesses about the local x - and y -axes and the torsional stiffness about the local z -axis. A detailed derivation is provided in Appendix A.

$$\mathbf{C}_{joint,k} = \frac{2\zeta}{\omega_r} \mathbf{K}_{joint,k} \quad (5)$$

The damping coefficient matrix $\mathbf{C}_{joint,k}$ is defined using a stiffness-proportional Rayleigh damping formulation [40,41]. The equivalent damping parameter ζ is specified at the reference circular frequency ω_r , resulting in the coefficient $2\zeta/\omega_r$ in Equation (5). The reference circular frequency is taken as $\omega_r = 2\pi/T_p$, where T_p is the spectral peak period of the corresponding irregular sea state, following the response-period definition for stiffness-proportional Rayleigh damping [42]. Here, ζ denotes an equivalent local damping parameter of the flexible joint, rather than a modal damping ratio of the full coupled system. The stiffness-proportional form is used to represent local structural energy dissipation, while the mass-proportional term is excluded to avoid artificial damping of the large rigid-body motions of the compliant floating system [43,44].

3.3. Fully Coupled Simulation Procedure

A frequency-domain to time-domain coupled numerical framework is established to evaluate the coupled response of the two-body floating wind turbine. The framework is based on OrcaWave 11.6b and OrcaFlex 11.6b. The overall procedure is shown in Figure 3. For each floating body, the wetted-surface panel model is generated from the three-dimensional geometry, and the frequency-dependent added mass, radiation damping, first-order wave excitation forces, and hydrostatic restoring coefficients are calculated. The resulting hydrodynamic database is then imported into OrcaFlex 11.6b and coupled with

the aerodynamic–servo-control model, turbulent wind field, mooring system, suspension system, and multi-body flexible-joint model. Fully coupled time-domain simulations are performed under combined wind, wave, and current loading, accounting for aerodynamic, hydrodynamic, servo-control, mooring, suspension, and structural coupling effects. The MATLAB API (R2025b) is used for automated load-case execution, batch simulations, and post-processing of platform motions, tower responses, and key structural loads.

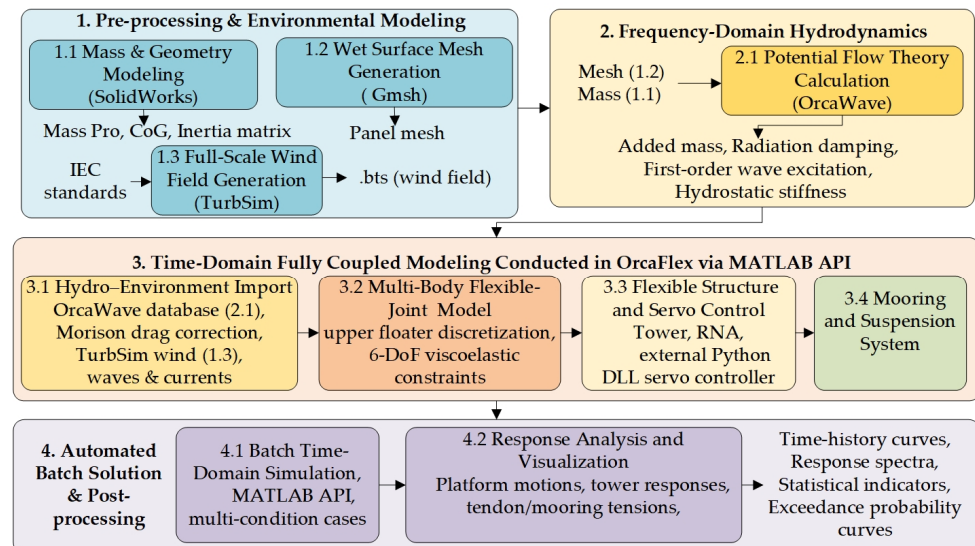


Figure 3. Fully coupled time-domain analysis framework for the two-body floating wind turbine.

4. Rationality Assessment of the Equivalent Model

In this study, the upper floater is discretized into four rigid bodies connected by virtual 6-DOF flexible joints, allowing the dominant flexible modes to be represented with reasonable computational efficiency. This section assesses the stiffness representation and multi-body discretization, with the joint reference stiffness derived from pontoon properties and the equivalent damping parameter treated only as a sensitivity variable.

4.1. Determination of the Reference Stiffness

Each pontoon is idealized as a uniform circular steel tube. Its reference stiffness is determined by the axial stiffness, transverse shear–bending stiffness, bending rotational stiffness, and torsional stiffness. According to Equation (4) in Section 3.2., the reference stiffness of the flexible joint, $\mathbf{K}_{j,k}^{\text{ref}}$ is calculated. The corresponding equivalent parameters and stiffness values are listed in Table 5.

Table 5. Equivalent parameters of the pontoon and reference stiffness of the flexible joint.

Geometrical and Material Parameter	Value	Unit	Flexible-Joint Stiffness	Value	Unit
Outer diameter, D	9.08	[m]	k_x	9.08×10^4	kN/m
Wall thickness, t	0.04	[m]	k_y	9.08×10^4	kN/m
Equivalent length, L_k	65	[m]	k_z	3.60×10^6	kN/m
Young's modulus, E	206	GPa	$k_{\theta x}$	3.68×10^7	kN·m/rad
Poisson's ratio ν	0.30	—	$k_{\theta y}$	3.68×10^7	kN·m/rad
Shear correction factor κ	0.50	—	$k_{\theta z}$	2.83×10^7	kN·m/rad

4.2. Verification of a Single Pontoon

Flexible joints are introduced to represent the flexibility of the pontoon in a lumped manner, consistent with previous simplified pontoon studies where a rotational spring stiffness was used and checked against static deflection and natural-frequency characteristics [45]. A single pontoon is modeled using two approaches: the OrcaFlex continuous-beam model and the OrcaFlex multi-body flexible-joint model. The boundary condition is fixed at one end and free at the other end.

For the static verification, unit transverse forces, an axial force, bending moments, and a torque are applied separately. The responses in the corresponding dominant degrees of freedom are then compared. The results are given in Table 6. The multi-body flexible-joint model agrees well with the continuous-beam model in terms of the static responses. The errors in the two transverse displacements are both 3.93%. The errors in the remaining static load cases are less than 1%. This indicates that the reference stiffness can reasonably represent the dominant flexibility of the pontoon.

Table 6. Member-level response comparison for a single pontoon.

Verification Type	Load Case/Mode	Response	Beam Model	Flexible-Joint Model	Difference [%]
Static	$F_x = 1\text{MN}$	$u_x [\text{mm}]$	38.2	39.7	3.93
	$F_y = 1\text{MN}$	$u_y [\text{mm}]$	38.2	39.7	3.93
	$F_z = 1\text{MN}$	$u_z [\text{mm}]$	0.28	0.28	0.00
	$M_x = 1\text{MN}\cdot\text{m}$	$\theta_x [^\circ]$	1.55×10^{-3}	1.56×10^{-3}	0.65
	$M_y = 1\text{MN}\cdot\text{m}$	$\theta_y [^\circ]$	1.55×10^{-3}	1.56×10^{-3}	0.65
	$M_z = 1\text{MN}\cdot\text{m}$	$\theta_z [^\circ]$	2.02×10^{-3}	2.02×10^{-3}	0.00
Natural frequency	First bending mode	$f [\text{Hz}]$	2.16	2.12	−1.85
	First axial mode	$f [\text{Hz}]$	19.7	17.7	−10.15
	First torsional mode	$f [\text{Hz}]$	12.2	11.0	−9.84

For the natural frequencies, the multi-body flexible-joint model reproduces the first transverse bending frequency with good accuracy. The error is −1.85%. The errors in the first axial and torsional frequencies are approximately 10%. These differences are mainly caused by the lumped representation of the distributed mass and distributed flexibility in the flexible-joint model. Overall, the two models give consistent results in terms of member-level static flexibility and the order of magnitude of the low-order natural frequencies.

4.3. Modal Verification of the Upper Floater

Rigid-body modules connected by elastic joints or flexible connectors have been widely used as an efficient approximation for the hydroelastic modeling of large floating structures. Riggs et al. [46] showed that a rigid-module flexible-connector (RMFC) model can reproduce the wave-induced response of a mobile offshore base when its dominant natural frequencies and mode shapes agree with those from a finite-element model. Fu et al. [47] further demonstrated that translational and rotational connector stiffnesses have significant effects on the natural frequencies, mode shapes, and hydroelastic response of flexible floating interconnected structures. These studies provide the basis for using a multibody flexible-joint model to represent the dominant global flexibility of the upper floater.

To verify the proposed multibody flexible-joint model, a modal comparison is conducted using a finite-element beam model of the upper floater as the reference. In the finite-element model, the pontoons are modeled by OrcaFlex Line objects, while the central and outer columns are treated as rigid bodies, with fixed pontoon-column connections. Since the upper floater is a floating structure, very weak elastic constraints are applied at the fairlead locations to suppress rigid-body drift and stabilize the eigenvalue solution.

The constraint stiffness is selected to be sufficiently small so that its influence on the elastic modes is negligible.

The multibody model adopts the same global topology, where the columns and pontoon segments are modeled as rigid bodies, and the pontoon flexibility is represented by midpoint flexible joints. After removing the six rigid-body motion modes, the first six global elastic modes of the two models are compared. As shown in Figure 4, the first four mode shapes agree well, demonstrating that the multibody flexible-joint model can reproduce the dominant low-order deformation of the upper floater. The discrepancies in higher-order modes are mainly attributed to the different flexibility descriptions, namely distributed beam flexibility in the finite-element model and concentrated hinge flexibility in the multibody model.

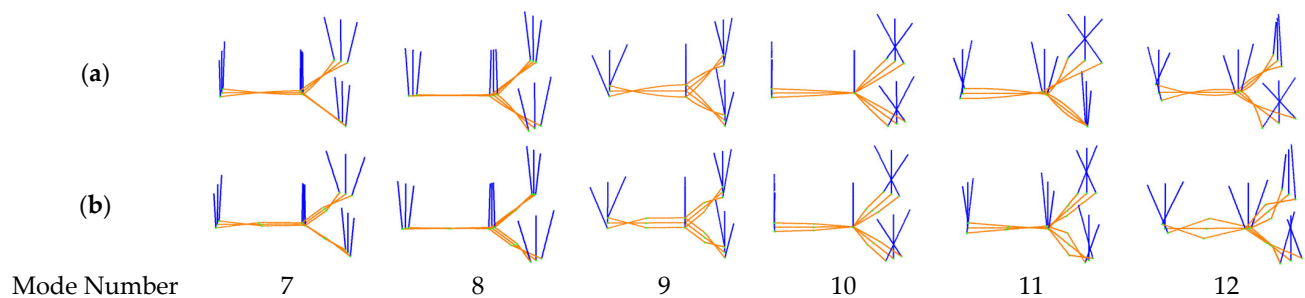


Figure 4. Comparison of the first six global elastic mode shapes of the upper floater: (a) finite-element beam model; (b) multibody flexible-joint model. The undeformed and symmetric deformed configurations are shown together to highlight the global deformation patterns. Blue and orange denote columns and pontoons, respectively, while green dots indicate connection nodes.

Natural frequencies and the Modal Assurance Criterion (MAC) are further used to quantitatively assess the modal consistency between the finite-element beam model and the proposed multibody flexible-joint model. For the r -th finite-element mode vector, ϕ_r^{FE} , and the s -th flexible-joint mode vector, ϕ_s^{MB} , the MAC is defined as

$$MAC = \left| (\phi_r^{FE})^T \phi_s^{MB} \right|^2 / [(\phi_r^{FE})^T \phi_r^{FE}] [(\phi_s^{MB})^T \phi_s^{MB}]. \quad (6)$$

A MAC value close to 1 indicates a strong correlation between the corresponding mode shapes. As shown in Table 7, the proposed multibody flexible-joint model reproduces the first four global elastic modes of the upper floater with good accuracy. The absolute frequency differences are below 7%, and the MAC values are above 0.97, indicating good consistency between the two models for the first four low-order global modes.

Table 7. Comparison of global modes of the upper floater.

Mode	Beam Model [Hz]	Flexible-Joint Mode [Hz]	Frequency Difference [%]	MAC
7	1.10	1.17	6.36	0.97
8	1.10	1.17	6.36	0.97
9	1.35	1.33	−1.48	0.99
10	2.20	2.14	−2.73	1.00
11	2.38	2.37	−0.42	0.67
12	2.38	2.37	−0.42	0.67

4.4. Hydrodynamic Consistency and Nearly Rigid Limit

A high-stiffness flexible-joint case with ($\beta = 100$) was included as a limiting-case consistency check. At this stiffness level, the relative motions between the rigid modules

are sufficiently suppressed within the investigated wave-frequency range, and the multi-body model exhibits quasi-rigid behavior. As shown in Figure 5, its added mass, radiation damping, and first-order wave-excitation force are nearly indistinguishable from those of the single-rigid-body model. This agreement confirms the consistency of the coordinate transformation and assembly of the multi-body hydrodynamic coefficients.

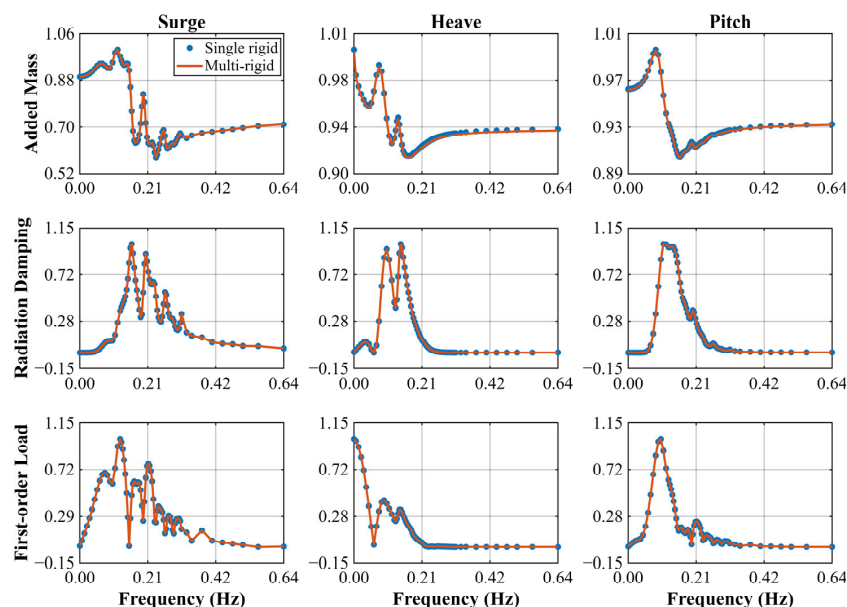


Figure 5. Comparison of first-order hydrodynamic coefficients between the single-rigid-body model and the multi-body flexible-joint model in the quasi-rigid limiting case ($\beta = 100$).

In this limiting condition, the global response of the multi-body system is expected to approach that of the rigid-body model. As shown in Table 8, the two models give nearly identical static equilibrium offsets and main free-decay periods. The surge and pitch offset differences are 3.44% and 1.37%, respectively, while the differences in the main free-decay periods are negligible. Further comparisons, including the surge, heave, and pitch dynamic responses, tower responses, and tendon tension and mooring-line tensions, also show close agreement between the two models, as discussed in Section 6. These results demonstrate that the flexible-joint model correctly recovers the nearly rigid limit.

Table 8. Comparison of static equilibrium offsets and motion periods between the single-rigid-body model and the flexible-joint model with $\beta = 100$.

Response	Baseline Model	Flexible Model with $\beta = 100$	Difference
Surge [m]	0.29	0.30	3.44%
Heave [m]	0.00	0.00	0.00%
Pitch [°]	−0.73	−0.74	1.37%
Surge Period [s]	107.7	107.7	0.00%
Heave Period [s]	18.5	18.5	0.00%
Pitch Period [s]	25.6	25.6	0.00%

5. Load Cases and Simulation Setup

5.1. Environmental Conditions and Set up

The target site is located in the northern South China Sea at a water depth of 200 m. The load cases are defined in accordance with IEC 61400-3 [48] and the CCS environmental condition guidelines [49], and the corresponding environmental conditions are summarized in Table 9.

Table 9. Summary of free-decay tests and selected design load cases.

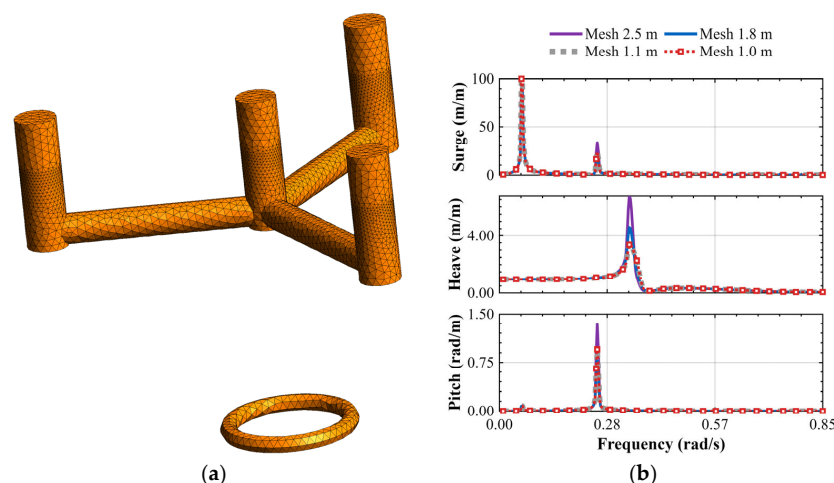
Test/DLC	Condition	Wind	Wave	Current
Surge = +10 m	Free-decay test	Calm	Calm	Not considered
Heave = +5 m		Calm	Calm	Not considered
Pitch = +5°		Calm	Calm	Not considered
DLC 1.2	Operational	$V_{hub} = 12$ m/s (NTM)	$H_s = 1.5$ m, $T_p = 7.5$ s (NSS)	Not considered
DLC 6.1	Parked	$V_{hub} = 53.5$ m/s (EWM)	$H_s = 13.1$ m, $T_p = 14.2$ s (ESS)	$V_c = 1.89$ m/s (ECM)

Notes: NTM, normal turbulence model; NSS, normal sea state; EWM, extreme wind speed model; ESS, extreme sea state; ECM, extreme current model. Wind, waves, and current are assumed to be collinear, with a heading angle of 0°.

DLC 1.2 is used to represent the normal power-production condition. DLC 6.1 is used to represent the parked extreme condition. For both DLC 1.2 and DLC 6.1, wind, waves, and current are assumed to be collinear for the examined environmental conditions. In DLC 6.1, the extreme wind speed, significant wave height, and current speed correspond to a 50-year return period. The turbulent wind field is generated using the Kaimal spectrum. The irregular wave field is described by the Pierson–Moskowitz spectrum [50,51]. For each environmental condition, six independent simulations are performed, in which the turbulent wind field and irregular wave field realizations are generated using paired random seeds to account for the stochastic variability of the environmental loading.

The numerical setup consists of time-domain simulations in OrcaFlex and frequency-domain hydrodynamic calculations in OrcaWave. For each environmental load case, an 11,000 s time-domain simulation was conducted in OrcaFlex, with the first 200 s discarded to eliminate initial transient effects; the remaining data were used for statistical analysis. Free-decay simulations were also performed to identify the natural periods and damping characteristics of the floating platform.

Figure 6 presents the OrcaWave panel model of the floating structure and its mesh convergence verification. The hydrodynamic coefficients were calculated using a three-dimensional surface panel model, in which the mesh was refined near the mean waterline and gradually coarsened with depth to balance numerical accuracy and computational cost. The mesh convergence results show that the surge, heave, and pitch response amplitude operators (RAOs) obtained from OrcaWave using the 1.1 m and 1.0 m meshes are nearly identical, whereas coarser meshes exhibit visible deviations near the resonance peaks. Therefore, the 1.1 m panel size was adopted in the final OrcaWave model to balance numerical accuracy and computational efficiency.

**Figure 6.** OrcaWave panel model and mesh convergence study: (a) surface panel model of the floating structure (b) comparison of surge, heave, and pitch RAOs under different representative panel sizes.

5.2. Flexible-Joint Parameter Combinations

The flexible-joint parameters are defined by the stiffness scaling factor β and the damping parameter ζ , as introduced in Section 3.2. The case ($\beta = 1$) corresponds to the reference stiffness derived from the equivalent Timoshenko beam model, while reduced values of β are used to examine the sensitivity to lower upper-floater stiffness. A high-stiffness case is also included to approximate the nearly rigid limit. These stiffness levels are not intended to represent specific structural design alternatives, but rather to cover a broad range for sensitivity assessment.

The damping parameter ζ is introduced as a joint-level equivalent damping parameter representing lumped local energy dissipation of the flexible joint. It may account for inherent material and structural damping, connection-level dissipation due to minor interfacial slip or hysteresis, and possible supplemental viscous damping devices installed near the joint [52,53]. Therefore, the selected values of ζ are used to assess the influence of equivalent local damping, rather than calibrated damping ratios of a specific physical connection. In total, 15 parameter models are considered in this study, as listed in Table 10.

Table 10. Parameter combinations of the flexible-joint models.

Group	β	ζ
Rigid-body model	—	—
Stiffness sensitivity group	0.1, 0.2, 0.25, 1.0, 100	0
Baseline-stiffness damping group	1.0	0.01, 0.02, 0.05, 0.08, 0.20
Low-stiffness damping group	0.1	0.01, 0.02, 0.05, 0.08, 0.20

6. Results and Discussion

This section discusses the natural characteristics, platform motions, tower loads, mooring-line and tendon responses, and the modeling effects of the flexible-joint parameters. To ensure a consistent comparison among different models, the origin of the body-fixed coordinate system at the central column is used as the motion reference point for both the baseline rigid-body model and the multi-body flexible-joint model.

6.1. Free-Decay Results and Motion RAOs

Figure 7 presents the surge, heave, and pitch periods identified from the free-decay simulations. The bars show the absolute natural periods, while the lines show the periods normalized by the corresponding single-rigid baseline values. The percentage changes relative to the baseline are indicated above the bars, and the horizontal black dashed lines represent the natural periods of the single-rigid baseline model.

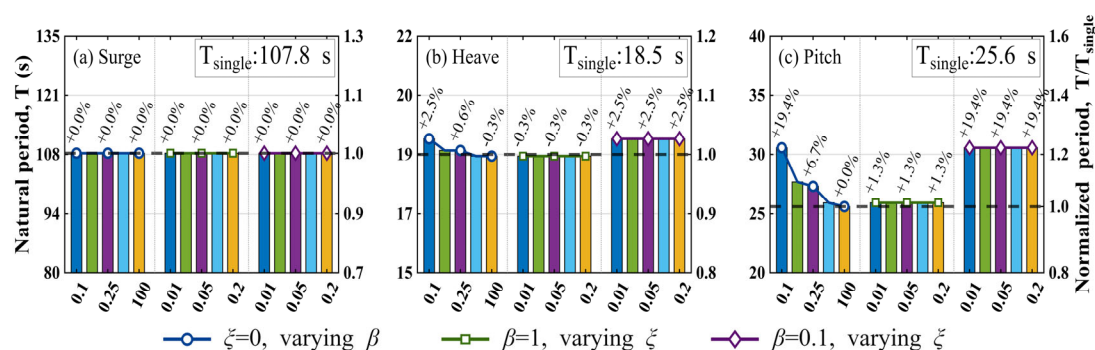


Figure 7. Variation in platform motion periods with flexible-joint parameters. The bar colors distinguish the parameter cases indicated on the x-axis.

The surge period remains approximately 107.8 s in all cases, while the maximum heave period increases only from 18.5 to 18.96 s, corresponding to an increase of 2.50%. These results indicate that the surge and heave periods are only weakly affected by the flexible-joint parameters within the investigated range.

In contrast, the pitch period is considerably more sensitive to the joint stiffness. For the low-stiffness case of ($\beta = 0.1$) and ($\xi = 0$), it increases from the single-rigid value of 25.6 s to 30.6 s, corresponding to an increase of 19.4%. As β increases, the pitch period gradually approaches that of the single-rigid model. This result indicates that reducing the joint stiffness weakens the effective rotational constraint of the upper floater and consequently increases the global pitch period. At a fixed value of β , varying ξ from 0.01 to 0.20 produces no discernible change in the identified periods within the numerical resolution. Therefore, the natural-period variation is governed primarily by the joint stiffness, whereas the joint damping has only a negligible influence on the identified motion periods.

The motion RAOs are obtained using a white-noise wave approach [54,55]. A truncated white-noise spectrum is prescribed over ($0.005 \leq f \leq 0.30$ Hz) and discretized into 150 non-uniformly spaced frequency components. Finer spacing is used in the low-frequency region and in the WF range of ($0.04 \leq f \leq 0.20$ Hz). After the time-domain simulation, the incident wave elevation and platform responses are transformed into the frequency domain. The RAOs are evaluated from the ratio of the corresponding amplitude spectra. Figure 8 shows the motion RAOs of the platform. The surge RAO is not sensitive to the flexible-joint parameters. However, the heave and pitch RAOs show local peaks within or above the WF range when the flexible-joint stiffness is reduced. This suggests that the flexibility of the upper floater may introduce additional coupled modes.

The increase in pitch period and the local response peaks observed in heave and pitch are qualitatively consistent with previous studies showing that platform flexibility can modify the natural characteristics and resonant responses of floating wind turbines [22,29]. However, unlike conventional single-body platforms, the present suspended-ballast system also transfers these flexibility effects to the relative motion and load transmission between the upper floater and the lower ballast.

Increasing the damping parameter clearly reduces the local peaks in the low-stiffness model. However, it does not significantly change the peak frequencies. To facilitate the interpretation of the power spectral density (PSD) results, the frequency range is divided into three parts: the LF range, $f < 0.04$ Hz; the WF range $0.04 \leq f \leq 0.20$ Hz; and the high-frequency range, $f > 0.20$ Hz.

To improve the readability of the Figures, the cases $(\beta, \xi) = (0.1, 0)$, $(0.1, 0.2)$ and $(100, 0)$ are selected to represent the low-stiffness undamped model, the low-stiffness damped model, and the high-stiffness undamped model, respectively. These cases are compared with the baseline model. The present results show that the high-stiffness model is generally close to the baseline model. Therefore, the following discussion focuses mainly on the low-stiffness model and the effect of damping.

6.2. Platform Motions and Relative Motion

The total platform motion consists of the static offset and the dynamic response. The static offset represents the equilibrium displacement caused by steady wind loads, current loads, and hydrostatic restoring forces. The dynamic response is the time-varying motion after removing this static equilibrium offset. Figures 9 and 10 show the time histories and PSDs of the platform dynamic motions and the relative motion between the two bodies under the operating and extreme conditions, respectively. The three representative flexible-joint configurations and the baseline rigid-body model are compared. The corresponding statistical results are summarized in Table 11.

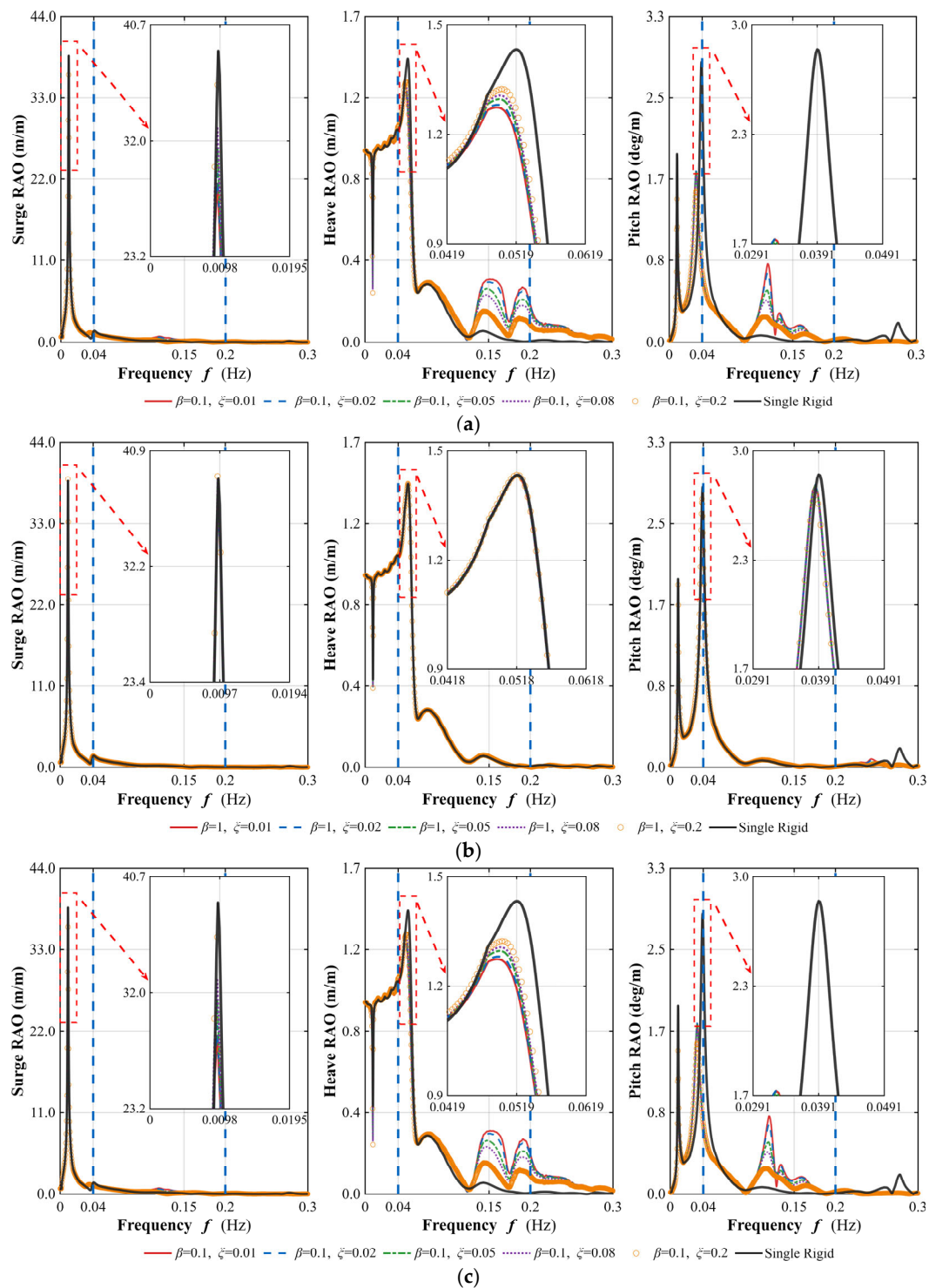


Figure 8. Effects of flexible-joint parameters on motion RAOs and resonant responses in the WF range: (a) effect of stiffness under undamped conditions ($\zeta = 0$); (b) effect of damping at the reference stiffness ($\beta = 1$); (c) effect of damping at low stiffness ($\beta = 0.1$).

Under the operating condition, the main peak of the surge PSD occurs at about 0.009 Hz. This indicates that the surge response is mainly governed by the surge natural mode. Compared with the baseline rigid-body model, the low-stiffness undamped model increases the surge standard deviation by only about 9.5%. However, the pitch standard deviation increases by about 90.7%. This shows that pitch is more sensitive to the stiffness of the upper floater.

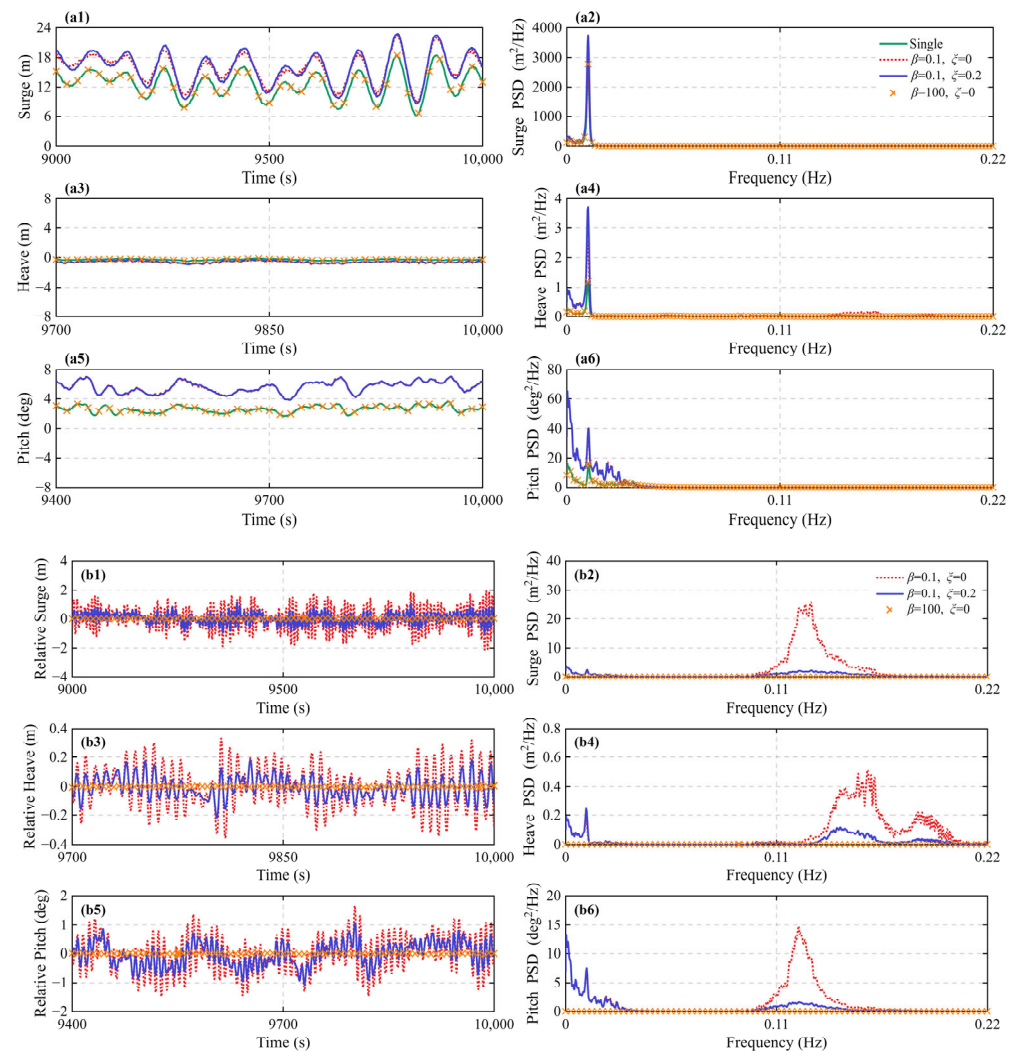


Figure 9. Time histories and PSD of motion responses under the operating condition: (a1–a6) platform surge, heave, and pitch responses; (b1–b6) relative surge, heave, and pitch responses. In each group, the left and right panels show the time histories and corresponding PSDs, respectively.

The change in the heave standard deviation is small. However, the vertical static offset of the platform motion reference point increases from 0.00 m to 3.69 m. This indicates that the softened upper floater deforms under the tendon tension, which causes a clear upward displacement of the reference point at the central column.

Table 11. Comparison of static offsets, dynamic and relative motions under different flexible-joint parameters.

Condition	Model	β	ζ	Surge [m]			Heave [m]			Pitch [°]		
				Static Offset	Std	Rel Std	Static Offset	Std	Rel Std	Static Offset	Std	Rel Std
Operational	Base	—	—	0.29	2.52	0.02	0.00	0.07	0.00	−0.73	0.43	0.01
	Flexible	100	0	0.30	2.53	0.02	0.00	0.07	0.00	−0.75	0.43	0.01
		0.1	0	−0.77	2.76	0.75	3.69	0.09	0.12	−1.47	0.82	0.61
		0.1	0.2	−0.77	3.03	0.36	3.69	0.06	0.06	−1.47	0.81	0.41
Extreme	Base	—	—	12.93	2.73	0.13	−0.04	1.26	0.02	−1.29	1.16	0.05
	Flexible	100	0	12.93	2.72	0.14	−0.03	1.26	0.02	−1.30	1.16	0.05
		0.1	0	11.56	2.82	4.40	3.67	1.29	0.58	−1.92	1.21	3.65
		0.1	0.2	11.56	2.76	1.59	3.67	1.29	0.33	−1.92	1.14	1.43

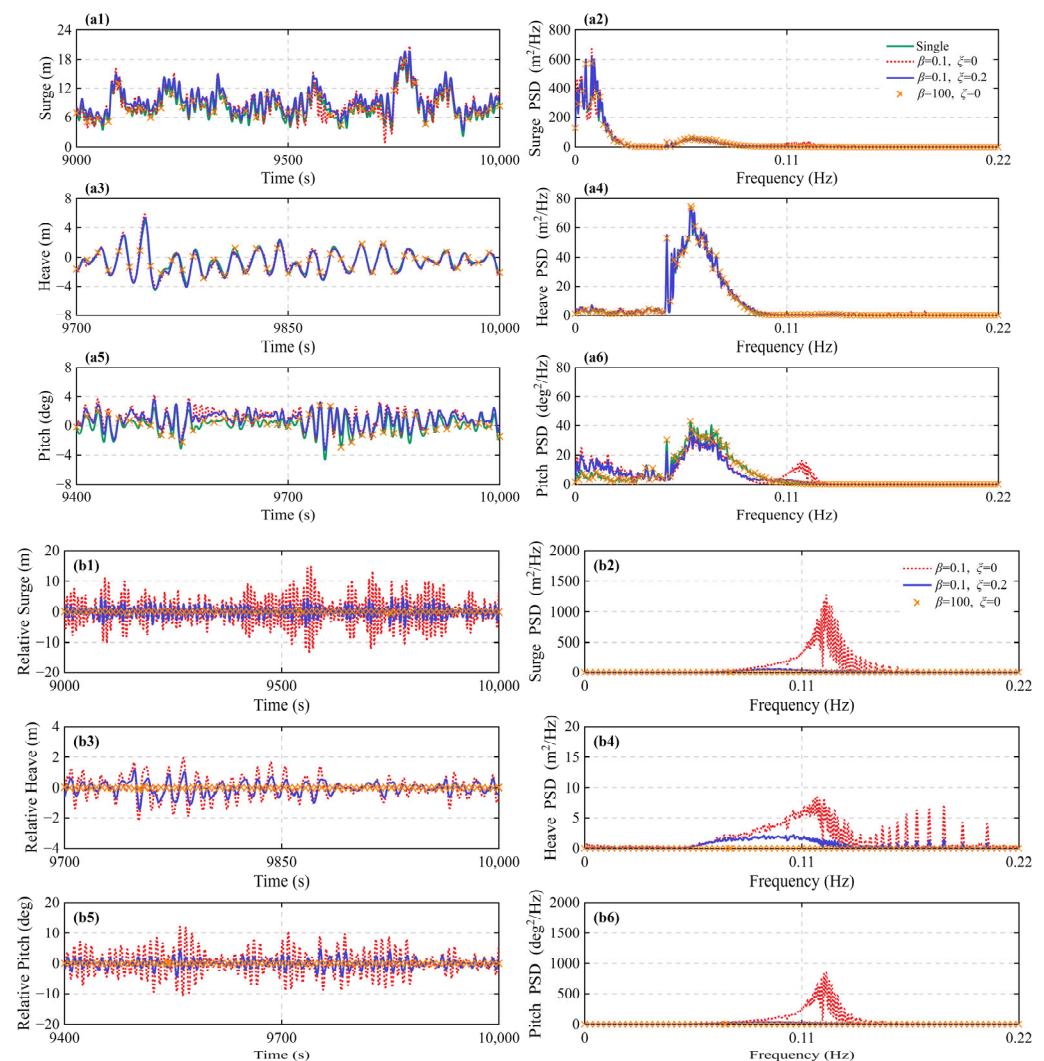


Figure 10. Time histories and PSDs of motion responses under the extreme condition: (a1–a6) platform surge, heave, and pitch responses; (b1–b6) relative surge, heave, and pitch responses. In each group, the left and right panels show the time histories and corresponding PSDs, respectively.

The relative motion between the two bodies is more sensitive to the flexible-joint parameters than the platform dynamic motion. In the low-stiffness undamped model, the standard deviations of relative surge and relative pitch increase to 0.75 m and 0.61° , respectively. After damping is introduced, they decrease to 0.36 m and 0.41° , respectively. The reductions are 52.0% and 32.8%, respectively. The PSD results show that low stiffness mainly increases the relative-motion energy in the WF range. Damping effectively suppresses the spectral peaks in this frequency band.

Under the extreme condition, the platform dynamic responses in surge, heave, and pitch are less sensitive to the flexible-joint parameters. The maximum increase in standard deviation relative to the baseline rigid-body model is about 4.3%. In contrast, the relative motion between the two bodies is further amplified. Its dependence on the flexible-joint parameters is consistent with that observed under the operating condition. Therefore, the relative motion between the two bodies is a more suitable indicator for identifying the flexibility effect of the upper floater than the platform dynamic motion.

6.3. Tower-Top Acceleration and Tower-Base Loads

Figure 11 and Table 12 compare the time histories, PSDs, and statistical characteristics of the tower-top acceleration, tower-base bending moment, and tower-base axial force. The results show that upper-floater stiffness and damping mainly affect the tower-top acceleration and tower-base bending moment, whereas their influence on the tower-base axial force is limited. Under the extreme condition, the low-stiffness undamped model increases the standard deviations of the tower-top acceleration and tower-base bending moment by 75.8% and 72.1%, respectively, relative to the rigid-body baseline. These increases are substantially suppressed when equivalent damping is introduced.

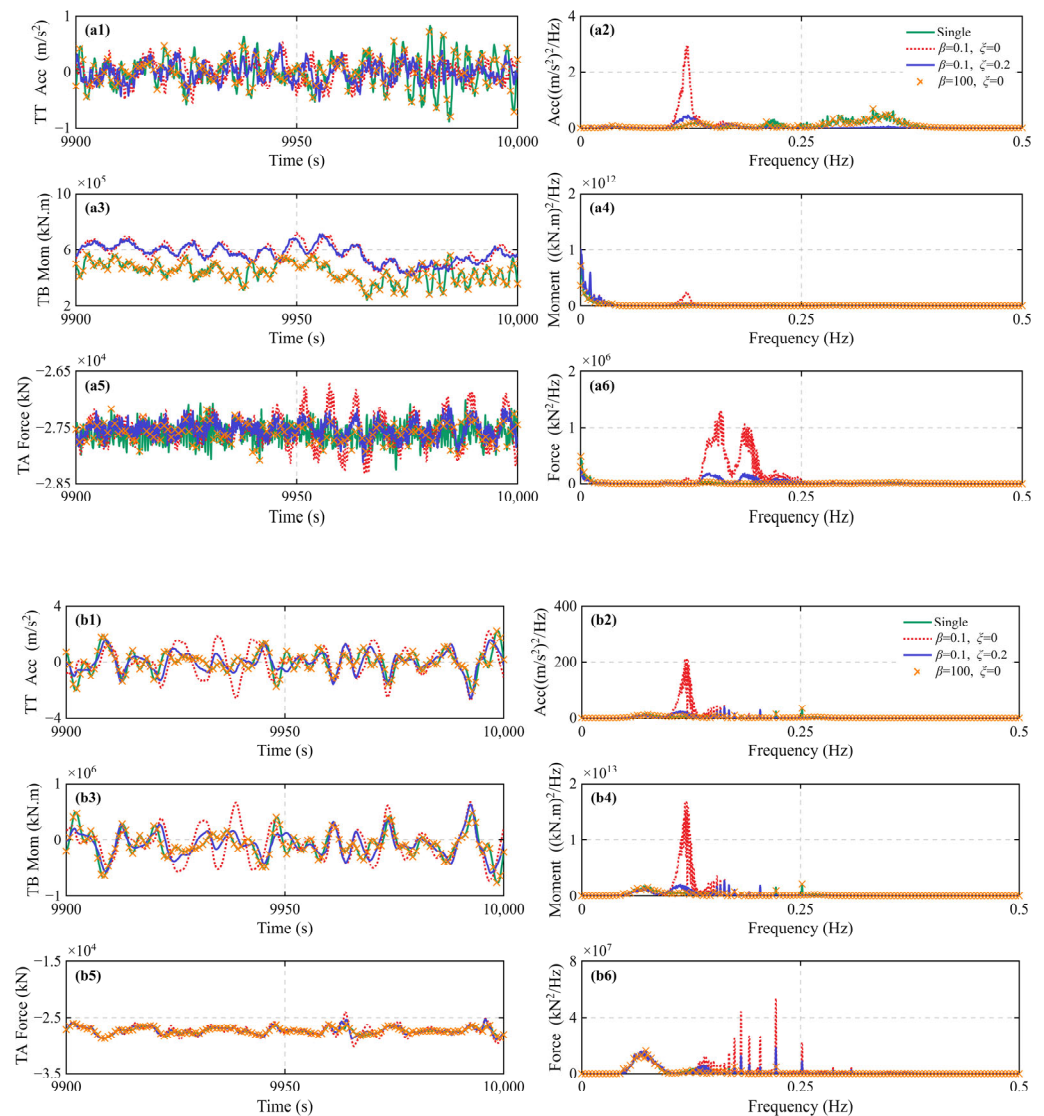


Figure 11. Time histories and PSDs of tower responses under operating and extreme conditions: (a1–a6) operating condition; (b1–b6) extreme condition. In each group, the rows show tower-top acceleration, tower-base bending moment, and tower-base axial force, respectively; the left and right panels show the time histories and corresponding PSDs.

The amplification of the tower-base bending response is qualitatively consistent with previous studies of flexible floating platforms. Li et al. [4] reported increases of more than 20% in the tower-base bending-moment response under certain operating conditions, while Dike et al. [23,24] showed that structural flexibility can significantly influence the dynamic and fatigue-sensitive responses of conventional semi-submersible platforms. The magni-

tudes should not be compared directly because of differences in platform configuration, turbine capacity, environmental conditions, and structural representation. Nevertheless, the consistent trend suggests that neglecting platform flexibility may underestimate tower dynamic loads and their potential fatigue implications.

Table 12. Statistics of tower responses under different flexible-joint parameters.

Condition	Model	β	ζ	TT Acc. [m/s^2]		TB Moment [$\text{MN}\cdot\text{m}$]		TA Force [MN]	
				Peak	Std	Peak	Std	Peak	Std
Operational	Base	—	—	0.92	0.22	662.95	86.35	28.37	0.17
	Flexible	100	0	0.92	0.22	664.16	86.37	28.36	0.17
		0.1	0	0.94	0.23	832.77	112.68	28.58	0.25
		0.1	0.2	0.70	0.16	782.38	101.69	28.17	0.14
Extreme	Base	—	—	5.64	0.99	1640.86	297.2	30.30	0.74
	Flexible	100	0	5.70	0.99	1648.87	297.39	30.31	0.74
		0.1	0	6.89	1.74	2092.00	511.45	31.83	0.99
		0.1	0.2	6.16	1.04	1627.98	307.61	30.97	0.81

Figure 12 further presents the exceedance probability curves under the DLC 6.1 parked condition defined in Table 9, representing a 50-year-return-period extreme environmental condition. Reducing the stiffness scaling factor shifts the distributions of tower-top acceleration and tower-base bending moment toward higher response levels. It also extends the high-response tails. Using 0.6 g as a reference limit for tower-top acceleration under the extreme condition, the low-stiffness undamped model still shows a clear exceedance probability near this limit. After damping is introduced, the high-response tail becomes shorter, and the exceedance risk is reduced.

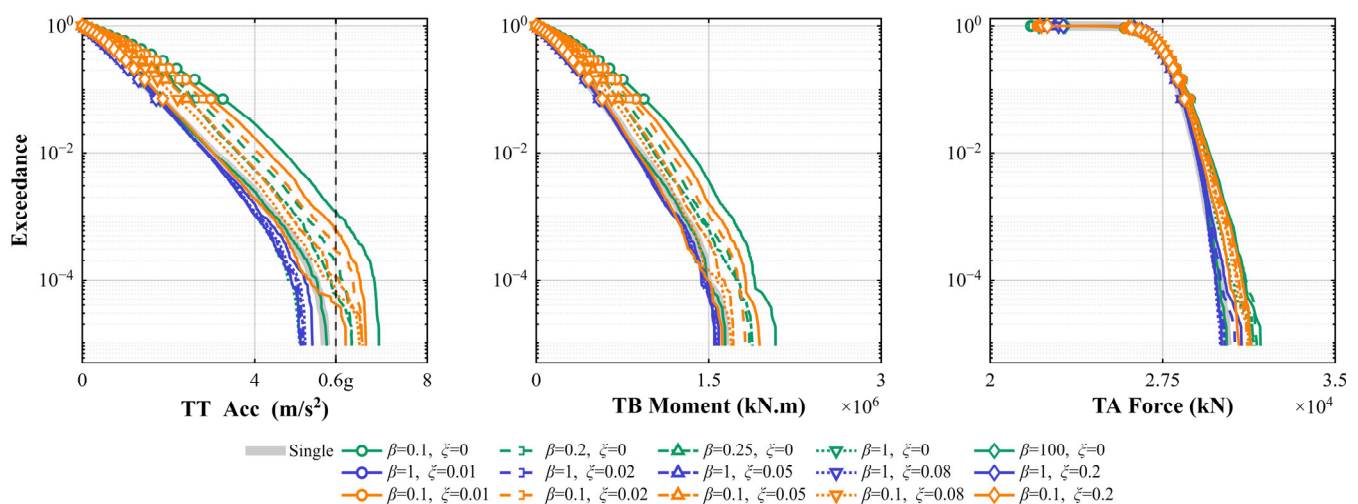


Figure 12. Exceedance probability curves for tower-top acceleration and tower-base loads under the extreme condition.

The curves of the tower-base axial force almost overlap for different parameter combinations. This confirms that the tower-base axial force is not sensitive to the flexible-joint parameters.

6.4. Effective Tensions in Mooring Line and Tendons

Figure 13 and Table 13 compare the effective tension responses of ML1, T11, and T21. The main PSD peak of ML1 occurs at 0.009 Hz. Its spectral shape and statistical

values change only slightly under different flexible-joint parameters. This indicates that the catenary mooring line tension is mainly controlled by the low-frequency global motion of the platform. It is not sensitive to the parameters of the upper floater.

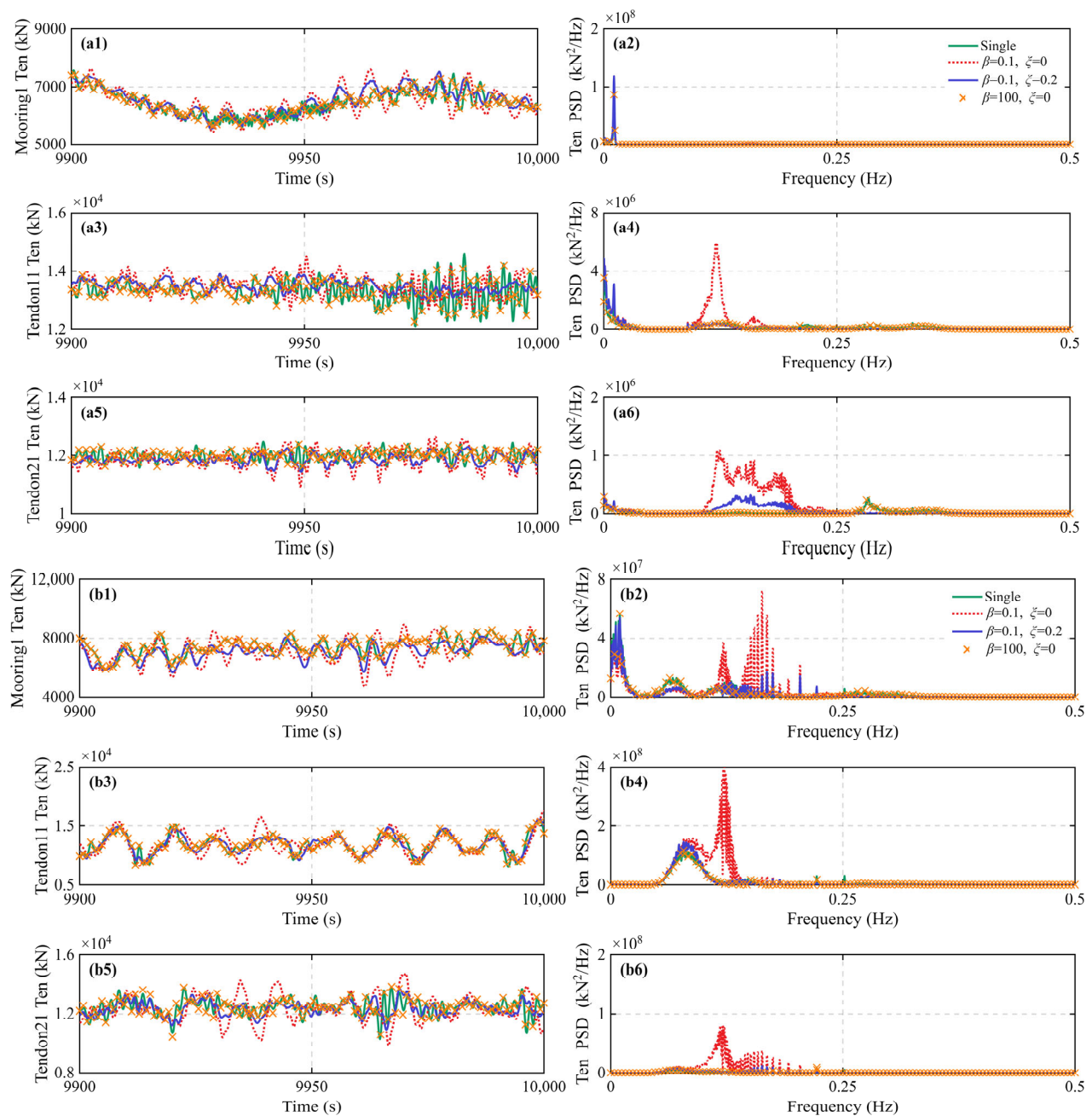


Figure 13. Time histories and PSDs of effective tensions under operating and extreme conditions: (a1–a6) operating condition; (b1–b6) extreme condition. In each group, the rows show the effective tensions of Mooring Line 1, Tendon 11, and Tendon 21, respectively; the left and right panels show the time histories and corresponding PSDs.

In contrast, the tension energy of T11 and T21 is mainly concentrated in the WF range. The upwave tendon T11 is the most sensitive to the stiffness of the upper floater. Reducing the stiffness scaling factor clearly enhances the WF spectral peak of T11. It also increases the maximum tension and the standard deviation. Increasing the damping parameter suppresses the WF spectral peak and reduces the tension fluctuation.

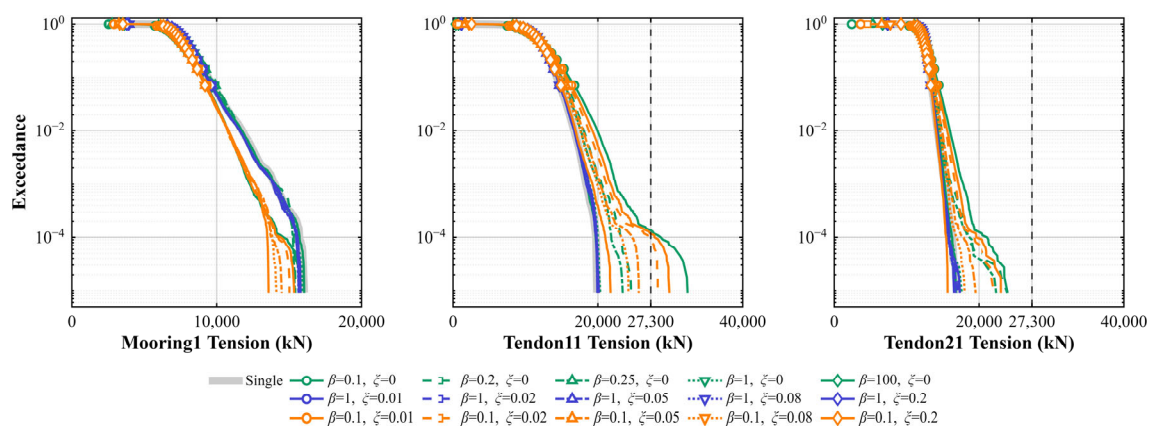
Table 13. Statistics of mooring-line and tendon tensions under different flexible-joint parameters.

Condition	Model	β	ξ	ML1 [MN]		T11 [MN]			T21 [MN]		
				Max	Std	Max	Min	Std	Max	Min	Std
Operational	Base	—	—	8.16	0.47	14.61	11.63	0.33	12.75	11.34	0.16
	Flexible	100	0	8.18	0.47	14.64	11.71	0.34	12.75	11.35	0.16
		0.1	0	8.26	0.54	14.79	11.59	0.40	12.97	10.63	0.27
		0.1	0.2	8.39	0.55	14.11	11.77	0.25	12.49	11.31	0.15
Extreme	Base	—	—	16.04	1.14	19.93	0.44	2.12	17.04	6.67	0.70
	Flexible	100	0	16.04	1.14	20.12	0.39	2.13	16.96	6.67	0.72
		0.1	0	15.75	1.23	32.50	0.49	3.17	23.99	2.43	1.41
		0.1	0.2	13.59	1.02	21.77	2.56	2.16	15.70	9.20	0.66

Under the extreme condition, the low-stiffness undamped model increases the maximum T11 tension from 19.93 to 32.50 MN, exceeding the tendon breaking load of 27.30 MN. Its standard deviation increases by 49.5%, while the minimum tension remains low at 0.49 MN. These results indicate that T11 is exposed to an overload risk while retaining only a limited slack-prevention margin.

After equivalent damping is introduced, the maximum tension and standard deviation of T11 decrease by 33.0% and 31.9%, respectively. The maximum tension falls below the breaking load, while the minimum tension increases to 2.56 MN. T21 also shows increased tension under reduced stiffness, but its maximum value remains below the breaking load in all the examined cases.

Figure 14 further shows that the flexible-joint parameters mainly affect the high-tension tails of the tendon responses. The ML1 curves change only slightly, and its maximum tension remains well below the breaking load. In contrast, reducing the upper-floater stiffness shifts the high-tension tails of T11 and T21 to the right, with T11 exceeding its breaking load under the low-stiffness undamped condition. Equivalent damping contracts the high-tension tails and reduces the corresponding extreme-load risk. These results identify T11 as the component governing the ultimate-strength margin of the suspension system.

**Figure 14.** Exceedance probability curves for effective tensions in mooring lines and tendons under the extreme condition.

These trends are consistent with previous studies of suspended two-body platforms, which showed that ballast and suspension-system properties can influence inter-body motion and tendon-load fluctuations [5,8,11]. The related NewSemi design study [30] also identified the upwave tendon as the critical suspension member and associated its dynamic tension with the relative motion between the upper floater and the ballast. The present

study extends these findings by showing that upper-floater stiffness and damping are additional parameters governing tendon-load amplification. This finding highlights the need to consider both suspension-system properties and upper-floater flexibility when assessing tendon safety in suspended-ballast two-body platforms.

6.5. Parameter Sensitivity Analysis

The results indicate that the flexibility effect is mainly governed by the stiffness scaling factor β . This trend is consistently observed in the free-decay, RAO, PSD, statistical, and exceedance-probability analyses. A lower β reduces the equivalent stiffness of the upper floater and changes the global dynamic characteristics of the system. In particular, it increases the platform pitch period and produces local response peaks near the WF range in heave and pitch. These changes further amplify platform surge, heave, relative motion between the two bodies, and tendon effective-tension fluctuations.

The damping parameter ζ plays a different role. It mainly affects the response amplitudes rather than the characteristic frequencies. The free-decay and PSD results show that ζ has only a limited influence on the natural periods and peak frequencies. However, under low-stiffness conditions, increasing ζ effectively suppresses the amplified responses. The reductions are most evident in the relative motion between the two bodies, tower dynamic responses, and tendon effective-tension fluctuations.

The sensitivity differs among response quantities, as summarized in Table 14. Tendon effective tension and relative motion between the two bodies are the most sensitive to the flexible-joint parameters. Tower-top acceleration and tower-base bending moment show moderate sensitivity. In contrast, platform surge static offset, ML1 tension, and tower-base axial force are less sensitive.

Table 14. Response sensitivity ranking and modeling implications.

Response	Sensitivity to β	Sensitivity to ζ	Most Affected Responses	Design Relevance
Pitch period	High	Low	global dynamics	Pitch period
Relative motion	High	Medium	flexibility indicator	Relative motion
Tower-base moment	Medium–High	Medium	structural load	Tower-base moment
Tendon tension	High	Medium	critical safety indicator	Tendon tension
Mooring tension	Low	Low	mainly LF surge-controlled	Mooring tension

The results also clarify the applicability of the different modeling approaches. When $\beta = 100$, most responses are close to those of the baseline rigid-body model. The upper floater can therefore be approximated as an integral rigid body under the nearly rigid limit. In contrast, when $\beta = 0.1$, the baseline rigid-body model cannot represent the relative motion between the two bodies. It may also underestimate the heave offset, surge response, tower-top acceleration, tower-base bending moment, and tendon dynamic tension. For two-body floating wind turbines with a low-stiffness upper floater, or when tendon dynamic load, slack risk, and tower dynamic response are of concern, the multi-body flexible-joint model is recommended for fully coupled dynamic analysis.

7. Conclusions and Future Work

This study developed a multi-body flexible-joint model for the Tsemi two-body floating platform supporting the IEA 22 MW reference wind turbine. The model includes the equivalent stiffness and equivalent damping of the upper floater and was used in fully coupled dynamic analysis. The effects of upper-floater flexibility on platform motions, relative motion between the two bodies, tower responses, and mooring-line/tendon loads were investigated. The main conclusions are as follows.

1. The multi-body flexible-joint model can represent the low-order global flexibility of the upper floater with reasonable accuracy. The reference stiffness was derived from Timoshenko beam theory and was able to reproduce the static and dynamic characteristics of the horizontal pontoon. The error in transverse displacement was 3.93%, and the error in the first transverse bending frequency was -1.85% . For the first four global elastic modes of the upper floater, the frequency differences were all below 7%, and the MAC values were not lower than 0.97. When $\beta = 100$, the static equilibrium position and the main free-decay periods of the multi-body model were close to those of the baseline rigid-body model. This confirms that the proposed model has a reasonable, nearly rigid limit.
2. The equivalent stiffness of the upper floater has a clear influence on the platform pitch response and the relative motion between the two bodies. For the low-stiffness case, the pitch natural period increased from 25.6 s to 30.6 s. Local WF response peaks also appeared in heave and pitch. When $\beta = 0.1$ and $\xi = 0$ the standard deviations of relative surge and relative pitch under the operating condition reached 0.75 m and 0.61° , respectively. These results show that the relative motion between the two bodies is a sensitive indicator of the flexibility effect of the upper floater.
3. Reducing the stiffness of the upper floater increases the tower dynamic response and tendon loads. Under the extreme condition, the standard deviations of tower-top acceleration and tower-base bending moment increased by 75.8% and 72.1%, respectively. The maximum effective tension in the upwave tendon T11 increased from 19.93 MN to 32.50 MN, exceeding its breaking load of 27.3 MN. Meanwhile, the minimum effective tension remained low at 0.49 MN, indicating a limited slack-prevention margin. These results indicate that the tendons of a low-stiffness two-body floating platform may be exposed to both overload and slack risk. Tendon loads should therefore be treated as a key concern in extreme-load assessment.
4. Equivalent damping can effectively reduce response amplification under low-stiffness conditions. When $\beta = 0.1$ and $\xi = 0.2$, the standard deviations of relative surge and relative pitch under the operating condition decreased by 52.0% and 32.8%, respectively. Under the extreme condition, the maximum effective tension in T11 decreased from 32.50 MN to 21.77 MN. For low-stiffness or lightweight platforms, or platforms with limited tendon safety margins, the flexibility and damping of the upper floater should be considered in fully coupled analysis.

The proposed multi-body flexible-joint model is a low-order lumped-parameter model intended for global-response analysis, equivalent-stiffness and damping sensitivity studies, and early-stage screening of unfavorable load cases. It does not resolve local plate-shell deformation and stresses, weld hotspots, buckling, fatigue damage, higher-order hydroelastic modes, or deformation-induced changes in the wetted-surface boundary conditions, and therefore cannot replace detailed structural models. The static, modal, and nearly rigid comparisons provide numerical verification but not experimental validation of the fully coupled hydroelastic response. Accordingly, the results should be interpreted as comparative response trends rather than design-level quantitative predictions.

Future work should combine detailed finite-element models, hydroelastic analysis, modal condensation, model tests, and monitoring data. These efforts can further calibrate the flexible-joint parameters, identify the physical sources of equivalent damping, and extend the method to complex environmental conditions, IEC design load cases, long-term fatigue assessment, and reliability analysis.

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Appendix A. Derivation of Reference Stiffness Matrix $\mathbf{K}_{joint,k}^{ref}$

For the k -th pontoon, the local z -axis is aligned with the pontoon axis, as shown in Figure 2b. The pontoon is modeled as a uniform Timoshenko cantilever beam of length L . A multi-body flexible-joint model is constructed using two rigid segments of length $L/2$ connected by a flexible joint at $z = L/2$. The joint stiffness is determined by matching the free-end flexibility of the equivalent model to that of the Timoshenko beam.

For the $x - \theta_y$ ending plane, define

$$\Delta \mathbf{u}_{TB,L}^x = \begin{bmatrix} \Delta x_{TB,L} \\ \Delta \theta_{y,TB,L} \end{bmatrix}, \quad \mathbf{f}_{TB,L}^x = \begin{bmatrix} F_x \\ M_y \end{bmatrix}. \quad (\text{A1})$$

The free-end flexibility of the Timoshenko beam is

$$\Delta \mathbf{u}_{TB,L}^x = \mathbf{C}_{TB,L}^x \mathbf{f}_{TB,L}^x, \quad (\text{A2})$$

where

$$\mathbf{C}_{TB,L}^x = \begin{bmatrix} \frac{L^3}{3EI_y} + \frac{L}{\kappa_x GA} & -\frac{L^2}{2EI_y} \\ -\frac{L^2}{2EI_y} & \frac{L}{EI_y} \end{bmatrix}. \quad (\text{A3})$$

For the flexible-joint model, the rigid segment from $z = L/2$ to $z = L$ gives

$$\Delta \mathbf{u}_{joint,L}^x = \mathbf{T}^x \Delta \mathbf{u}_{joint,L/2}^x, \quad \mathbf{T}^x = \begin{bmatrix} 1 & -L/2 \\ 0 & 1 \end{bmatrix}. \quad (\text{A4})$$

The displacement matching condition is

$$\Delta \mathbf{u}_{joint,L}^x = \Delta \mathbf{u}_{TB,L}^x \quad (\text{A5})$$

The corresponding force transformation follows from virtual work:

$$(\mathbf{f}_{TB,L}^x)^T \delta \Delta \mathbf{u}_{joint,L}^x = (\mathbf{f}_{joint,L/2}^x)^T \delta \Delta \mathbf{u}_{joint,L/2}^x \quad (\text{A6})$$

Therefore, the midpoint-joint flexibility is

$$\mathbf{C}_{joint,L/2}^x = (\mathbf{T}^x)^{-1} \mathbf{C}_{TB,L}^x (\mathbf{T}^x)^{-T} \quad (\text{A7})$$

Substituting $\mathbf{C}_{TB,L}^x, \mathbf{T}^x$ gives

$$\mathbf{C}_{joint,L/2}^x = \begin{bmatrix} \frac{L^3}{12EI_y} + \frac{L}{\kappa_x GA} & 0 \\ 0 & \frac{L}{EI_y} \end{bmatrix}. \quad (\text{A8})$$

Thus, the stiffnesses in the $x - \theta_y$ bending plane are

$$k_x = \left(\frac{L^3}{12EI_y} + \frac{L}{\kappa_x GA} \right)^{-1}, \quad k_{\theta_y} = \frac{EI_y}{L} \quad (\text{A9})$$

Similarly, for the $y - \theta_x$ bending plane

$$k_y = \left(\frac{L^3}{12EI_x} + \frac{L}{\kappa_y GA} \right)^{-1}, \quad k_{\theta_x} = \frac{EI_x}{L} \quad (\text{A10})$$

The axial and torsional stiffnesses are

$$k_z = \frac{EA}{L}, \quad k_{\theta_z} = \frac{GJ}{L} \quad (\text{A11})$$

Therefore, the reference stiffness matrix of the midpoint flexible joint is

$$\mathbf{K}_{\text{joint},k}^{\text{ref}} = \text{diag} \left[\frac{12EI_y}{L^3(1+\phi_x)}, \frac{12EI_x}{L^3(1+\phi_y)}, \frac{EA}{L}, \frac{EI_x}{L}, \frac{EI_y}{L}, \frac{GJ}{L} \right] \quad (\text{A12})$$

The shear-deformation influence factors ϕ_x and ϕ_y are

$$\phi_x = \frac{12EI_y}{\kappa GA L^2}, \quad \phi_y = \frac{12EI_x}{\kappa GA L^2}. \quad (\text{A13})$$

where E and G are Young's modulus and shear modulus, respectively. A is the sectional area. I_x and I_y are the second moments of area. J is the torsional constant. κ is the shear correction factor.

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