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Hydrodynamic investigation of design parameters for a cylindrical type floating solar system

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ABSTRACT: In this paper, the coupled dynamic response of a moored horizontal cylinder type platform (simplified) for floating photovoltaic applications is obtained using Riflex, a non-linear time domain modelling approach. Cylinder diameter, submergence and environmental parameters are investigated and their influence on the dynamic response is discussed. Response amplitude operators, power density spectra, and response amplitudes for an irregular sea state spectrum are compared for each parametric variation. The static mooring system characteristics are found using equations of elastic catenary and compared with numerical force-excursion tests, which are found to be in general agreement. Natural periods are determined and compared with numerical free-decay tests. The dynamic responses due to first order wave forces, exerted by linear long-crested regular waves, are determined for a range of wave periods. The responses are found to be insensitive to diameter changes and mostly sensitive to submergence changes throughout the applied load cases.

1 INTRODUCTION

Floating solar (FS), also known as floating photovoltaics (FPV), is an emerging renewable energy technology. The FPV industry has been growing such that the cumulative installed capacities have been doubling year-on-year since the first installation in 2007. Such growth places FPV as one of the fastest-growing renewable energy sources. Many of the FPV structures that exist today have been designed to operate safely in shallow, benign, inland water bodies. However, in 2019 a typhoon in Japan caused the mooring lines of a 13.7 MWp inland plant to fail, which resulted in the estimated damage of 70 percent of the photovoltaic (PV) panels (Kaneko and Kato, 2019). It is understood that scarce knowledge of dynamic responses in exposed areas and extreme conditions combined with the lack of design standards and best practice guidelines can be attributed to the failure. These insufficiencies present challenges for the industry: to assess and ensure the systems' structural integrity and the application of due diligence when designing, installing and operating FPV systems. All the while considering sustainability and economic aspects. Several types of devices have been developed for inland applications and are discussed by Friel et al., (2019). Similar to

floating fish farms, FPV plants are floating structures with small drafts and superstructures, and the buoyancy is provided by pontoons or floaters that either themselves form the floating structure or the floaters are connected to aluminium, steel or fibre-reinforced plastic (FRP) members (Kristiansen and Faltinsen, 2009). Unlike their ground-mounted counterparts, FPV arrays require a mooring system to provide station-keeping by limiting movement induced by waves and wind. Additionally, they require more durable and corrosion-resistant components to withstand harsher marine environments. Of the available literature, few authors have investigated FPV devices with regard to hydrodynamics, aerodynamics or the coupled aero-hydro-elastic responses in nearshore environments.

Figure 1. Floating PV systems which utilise cylindrical tubes for buoyancy: (left) Floating Upsolar and Solar MarinEnergy technology, (Clot, 2018) (right) Workers assembling FPV technology.

This research focuses on investigating the coupled dynamic responses of a moored horizontal cylinder

type FPV device (Figure 1). The following is conducted in the paper:

- (a) Riflex, a static and dynamic finite element program, is employed to determine the time-domain responses of several simplified design cases when exposed to regular long-crested waves,
- (b) The influence that the cylinder diameter and cylinder submergence have on the dynamic responses throughout a range of incident wave periods is investigated,
- (c) Comparisons are made between the response amplitude operators, power density spectra and amplitudes for several design cases,
- (d) The natural periods of each system are estimated and compared with numerical free-decay tests performed in Riflex,
- (e) The mooring system characteristics are estimated and compared with force-excursion test completed in Riflex,

Figure 2. Numerical model visualizations: (left) Visualization of floating structure with coordinate system, (right) Visualization of numerical model in equilibrium configuration.

2 ENVIRONMENTAL CHARACTERISTICS

In lakes, reservoirs, and bays wind-generated waves are limited by the geometry of the water body, fetches are normally restricted, and the waves are generated locally. The fetch lengths tend to be measured from the shoreline to the point of interest, in the direction of the wind. Whereas, in offshore and nearshore environments the fetch lengths and widths are normally of a similar order of magnitude (Smith, 1991), and are measured from the origin of the wind to the point of interest in the direction of the wind. The numerical model used in this investigation is restricted to regular waves only. Therefore, this study is made applicable to nearshore locations, large lakes, reservoirs and potentially bays, by applying regular waves with periods ranging between 2 and 8 s and a fixed amplitude of 0.5 m. Such wave periods may be found in water bodies with fetches ranging between 2 and 100 km and can be generated by a wide range of wind velocities and durations. The waves are numerically modelled using a wave potential according to Linear Airy Theory. When the dynamic

responses for each wave period are found, a Jonswap spectrum is formulated using a significant wave height $H_s = 1$ m and peak period, $T_p = 5$ s, which corresponds to a fetch distance equal to 70 km. To further investigate each configuration, we determine the response amplitude operators (RAO) by dividing the average peak responses, obtained from the steady-state harmonics, by the wave amplitude. It should be noted that the RAO's obtained in this study are influenced by the mooring system, unlike conventional frequency-domain models. The resultant RAO is then used with the wave spectrum and Equations 1 and 2 to determine the response spectrum $S_n(\omega)$ and response amplitude $X_n(\omega)$ at each wave frequency ω . The expressions, as presented by Faltinsen, (2005), are given in the following equations:

$$S_n(\omega) = RAO_n^2 S_\zeta(\omega) \quad (1)$$

$$X_n(\omega) = \sqrt{2S_n(\omega)\Delta\omega} \quad (2)$$

Where RAO_n is the response amplitude operator value at each wave frequency and subscript n corresponds to the degree of freedom (surge, heave and pitch).

3 NUMERICAL MODEL

3.1 Hydrodynamic Load Model

Floating solar systems, when placed in the ocean, will be subjected to three principal force mechanisms; inertia, gravity and viscous effects. In this investigation, forces due to aerodynamic excitation, water impact, breaking waves and potential damping are neglected and the non-linear hydrodynamic interactions between the flotation systems are also neglected. A unique hydrodynamic load model that is applicable for partly submerged floating bodies is applied. The model considers Froude-Krylov forces F^{FK} , radiation forces F^R (added mass), a simplified representation of diffraction forces F^S and viscous drag forces F^D from Morison's formula. The total hydrodynamic force expression is given as the sum of each force contribution on each sub-element:

$$F^H = \sum \Delta F_n^{FK} + \sum \Delta F_n^S + \sum F_n^R + \sum \Delta F_n^D \quad (3)$$

The method discretises each structural member into sub-elements and calculates the hydrodynamic forces at each time step using the instantaneous element position, structural kinematic and wave kinematic properties at the centre of buoyancy of the sub-element. Each force contribution has coordinate transformations which are omitted here for simplicity. The Froude-Krylov force on a sub-element ΔF_n^{FK}

includes contributions from buoyancy and is determined using the following equation:

$$\Delta F_n^{FK} = f_n^{FK} \Delta x = (f_n^{Buoy} + \rho A_S \dot{u}_n) \Delta x \quad (4)$$

Where f_n^{Buoy} is the buoyancy force calculated using the instantaneous immersed cross-sectional area of the sub-element A_S , $\dot{u}_n$ is the vector component of the water particle acceleration normal to the sub-element, ρ is the water density and Δx is the sub-element length (Dixon et al., 1979), (Kaplan, 1959), (SINTEF Ocean, 2017). The added mass, diffraction and drag forces are calculated in terms of accelerations, velocities, and the frequency-independent coefficients (Kvittem et al., 2012). The coefficients are specified for a fully submerged cross-section, and the instantaneous values are found using the proportion of the immersed area. The element-wise added mass force ΔF_n^R may be expressed as:

$$\Delta F_n^R = f_n^R \Delta x = -\rho A_S \ddot{v}_y C_{my} \Delta x - \rho A_S \ddot{v}_z C_{mz} \Delta x \quad (5)$$

Where C_{my} and C_{mz} are the local 2D fully submerged added mass coefficients, and $\ddot{v}_y$ and $\ddot{v}_z$ are the local structural acceleration components. The simplified diffraction force ΔF_n^S contribution, which is expressed in terms of the added mass, may be written as:

$$\Delta F_n^S = f_n^S \Delta x = \rho A_S \dot{u}_y C_{my} \Delta x + \rho A_S \dot{u}_z C_{mz} \Delta x \quad (6)$$

Where $\dot{u}_y$ and $\dot{u}_z$ are the local water particle acceleration components. The transverse drag force is expressed as:

$$\Delta F_n^D = f_n^D \Delta x = \frac{1}{2} \rho C_{Dy} h_{rel} |u_{rn}| u_{ry} \Delta x + \frac{1}{2} \rho C_{Dz} b_{rel} |u_{rn}| u_{rz} \Delta x \quad (7)$$

Where $h_{rel} = A_s h / A$, and $b_{rel} = A_s b / A$, A is cross-sectional area and b and h are characteristic lengths of the cross-section in the local y- and z-axis (SINTEF Ocean, 2017). C_{Dy} and C_{Dz} are the fully submerged drag coefficients in the local y- and z-axis, and u_{rn} is the transverse relative velocity vector.

3.2 Static finite element scheme

The static finite element solution considers the forces due to self-weight and buoyancy, also known as volume forces. The static analysis determines the equilibrium shape, position and forces of the structural components, floating bodies and mooring system. The static finite element solution is achieved by solving a set of nodal displacement vectors ' r ', as seen in Equation 8, using an iterative Euler-Cauchy procedure from an initial stress-free configuration to obtain the final static equilibrium configuration. The internal structural reaction force vector ' $R^S(r)$ ' must

equal the external force vector ' $R^E(r)$ ' for the system to be in equilibrium.

$$R^S(r) = R^E(r) \quad (8)$$

External forces are separated into small increments and applied in accession at each load step. Furthermore, equilibrium iterations are implemented at each load step and if satisfactory results (within prescribed accuracy limits) are achieved the method proceeds to the next load step, where the initial values for the equilibrium calculations are those obtained at the previous load step.

3.3 Dynamic finite element scheme

In the dynamic procedure, a non-linear system of differential equations represents the equation of motion, which is similar to the static equilibrium equation, but with additional time-dependent force vectors. The expression is illustrated in Equation 9 below.

$$R^I(r, \ddot{r}, t) + R^D(r, \dot{r}, t) + R^S(r, t) = R^E(r, \dot{r}, t) \quad (9)$$

The equation includes an inertial force vector $R^I(r, \ddot{r}, t)$ containing properties of the structural mass and added mass, a damping force vector $R^D(r, \dot{r}, t)$ containing properties of internal structural damping and hydrodynamic damping and an internal structural reaction force vector $R^S(r, t)$ that contains information relating to stiffnesses and structural deformations. The summation of the three must equal the external force vector for the system to be in equilibrium. For this study, the displacement equilibrium accuracy is prescribed to be 10 μm . The non-linear time-domain solution is achieved using a Newmark Beta constant step integration technique on the equilibrium equation and a Newton-Raphson equilibrium iteration at each step. In Riflex, there are different formulations used for the numerical implementation of hydrodynamic loads, which consider the order of the polynomial used in the displacement functions and to represent the hydrodynamic loads on the finite elements. The hydrodynamic loads may generally be described as non-conservative loads, that are dependent on position, direction and/or loaded area (SIMO Project Team, 2012). All forces which are applied in the analysis are applied as either point loads or distributed line loads.

4 MODEL CONFIGURATIONS

A standard cartesian coordinate system is adopted and the six degrees of freedom are, respectively, surge, sway, heave, roll, pitch and yaw. The origin is taken about the intersecting planes of symmetry at the mean water level. The seafloor topology is flat and parallel

to the mean water level, and some general soil stiffnesses are applied.

4.1 Model design

The simplified FPV raft consists of three hollow, parallel cylinders with a length of 3 m, spacing 3 m, material thickness 0.025 m and density 970 kg/m³. The cylinders are connected by 6 vertical I-beams with length 0.355 m, and two horizontal I-beams of length 6m. The I-beams have cross-sectional dimensions (H 0.13 x W 0.12 x T 0.01 m), density of 2700 kg/m³, Young's Modulus of 68 GPa, and Poisson ratio of 0.34. In each finite element model, the structural members are modelled as beam elements.

The photovoltaic panels and some structural members have been omitted for simplicity. The mooring system and structural members are consistent throughout each numerical model. By increasing the cylinder density we equalize the buoyancy force, simulate ballasting and maintain the desired draft. The design case properties are illustrated in Table 1 below.

Table 1. Design case properties

Design Case	Cylinder Properties			Total Mass (kg)
	Outer Diameter (m)	Volume	Spacing (m)	
		Submergence		
		(% of Total Cylinder Vol.)		
1	0.50	50	3.00	1378
2	0.75	50	3.00	2514
3	1.00	50	3.00	4099
4	0.50	65	3.00	1649
5	0.50	80	3.00	1921
6	0.50	95	3.00	2203

The centre of gravity (COG) and buoyancy (COB) of each system are required to determine the metacentric heights and natural periods. The COG and COB are measured from the origin and are illustrated in Table 2.

Table 2. Centre of gravity, centre of buoyancy, and metacentric height

Case	Centre of gravity	Centre of buoyancy	Metacentric height	
	(x, y, z) (m)	(x, y, z) (m)	GM _L	GM _T (m)
1	0, 0, -1.15	0, 0, -0.11	2.78	29.62
2	0, 0, -0.55	0, 0, -0.16	2.16	20.14
3	0, 0, -0.29	0, 0, -0.21	1.83	15.41
4	0, 0, -0.89	0, 0, -0.13	2.10	22.15
5	0, 0, -0.73	0, 0, -0.17	1.52	16.11
6	0, 0, -0.61	0, 0, -0.02	0.80	9.15

It is well known that for fully submerged cylinders the added mass in heave, at any frequency, and surge are equal and their coefficients tend towards one as the depth of the cylinder increases. However, for penetrating, or partly immersed cylinders, the same cannot be said. Greenhow and Yanbao, (1987) and Greenhow and Ahn, (1988) state that the heave added mass at half-submergence equals $2\rho\nabla/\pi^2$, where ∇ is the displaced volume, and ρ is the water density. Taylor, (1930) used conformal mappings to equate the surge added mass coefficient per unit length as:

$$A_{surge} = \frac{1}{2\pi} [\sin(\theta) - \theta + \frac{\pi\theta}{3} (1 - \cos(\theta)) \left(\frac{4\pi - \theta}{(2\pi - \theta)^2} \right)] \quad (10)$$

Where $\theta = 2\cos^{-1}(-z/r)$ and z is the depth to the cylinder centreline and r is the cylinder radius. Thus, for the diameter cases, the surge added mass coefficient equals 0.5, as seen in Table 3 below. For the submergence cases, the heave added mass coefficient is estimated from plots presented by Greenhow and Yanbao, (1987).

Table 3. Non-dimensional parameters

Design Case	Hydrodynamic Properties				
	Re	KC	Fr	A ₁₁	A ₃₃
1	219.71e+3	20.88	0.24	0.50	0.20
2	329.57e+3	13.92	0.19	0.50	0.20
3	439.42e+3	10.44	0.17	0.50	0.20
4	219.71e+3	20.88	0.24	0.79	0.52
5	219.71e+3	20.88	0.24	1.18	0.55
6	219.71e+3	20.88	0.24	1.80	0.60

Equations presented by Newman, (1977) for the maximum fluid particle velocity in a wave cycle are used to determine the non-dimensional properties. *Re*, *KC* and *Fr*, and are calculated for a regular wave with a period of 5 s and amplitude of 0.5m

4.2 Mooring system

The mooring system has four spread catenary lines with continuous cross-section geometry. The lines have a length of 43.589 m, horizontal projection length of 38.18 m, vertical projection length of 10 m (water depth), a diameter of 0.025 m, density 7700 kg/m³ and Young's Modulus of 200 GPa. In each finite element model, the mooring lines are modelled using bar elements. The mooring system has a great influence on the dynamic responses and the overall stability of a floating structure. The system stiffness characteristics at static equilibrium are useful in estimating the initial stability of the structure and natural periods. In this study, a hybrid numerical-analytical technique, presented by Al-Solihat and Nahon, (2016), is applied to determine the stiffness matrix for the catenary mooring system (Figure 3).

Firstly, the in-plane 2-dimensional stiffness matrix (K_{ij}^p) for a single mooring line must be found at the fairlead location 'P'. Due to the symmetry of the mooring system the 6 x 6 stiffness matrix (K_m) may then be determined using Equation 10.

$$K_{11} = \frac{1}{2}n(K_{11}^p + \frac{H}{l})$$

$$K_{15} = -n(-\frac{R}{2}K_{12}^p + \frac{D}{2}K_{11}^p + \frac{DH}{l})$$

$$K_{22} = K_{11}, \quad K_{24} = -K_{15}, \quad K_{33} = nK_{22}^p$$

$$K_{44} = n\left(-DRK_{12}^p + \frac{D^2}{2}K_{11}^p + \frac{R^2}{2}K_{22}^p + DV + \frac{HR}{2} + \frac{D^2H}{2l}\right)$$

$$K_{51} = K_{15}, \quad K_{55} = K_{44}, \quad K_{66} = n\left(\frac{HR^2}{l} + HR\right) \quad (10)$$

Where H and V represent the in-plane horizontal and vertical tension components at the fairlead location. l and h represent the horizontal and vertical projected lengths respectively. D and R represent the z-axis distance from the mean water level to the fairlead connections and radius from the centre of the platform respectively. In Figure 3, an illustration of the catenary mooring system and attached raft may be seen. The 6 x 6 stiffness matrix of the mooring system is illustrated below. Where the translational DoF are in N/m and the rotational DoF are in N.m/deg.

$$K_m = \begin{bmatrix} 176.4 & 0 & 0 & 0 & 387.7 & 0 \\ 0 & 176.4 & 0 & -387.7 & 0 & 0 \\ 0 & 0 & 316.2 & 0 & 0 & 0 \\ 0 & -387.7 & 0 & 3007 & 0 & 0 \\ 387.7 & 0 & 0 & 0 & 3007 & 0 \\ 0 & 0 & 0 & 0 & 0 & 2063 \end{bmatrix}$$

Figure 3. Slack catenary mooring lines with suspended and resting section.

4.3 Natural Periods

In order to understand whether each system will be susceptible to resonance in the specified locations, the natural periods should be pre-determined. The natural periods of each design cases are approximated using the hydrostatic stiffness, mooring system stiffness, structural mass and added mass matrices. Using the corresponding properties for each design case in Tables 1-3 we estimate the natural periods and compare with numerical free-decay tests conducted in Riflex. The free-decay tests were conducted by applying dynamic nodal forces that are equal to 140 N, and spread across the principal axis of each cylinder. The natural periods may be observed in Table 4 below.

Table 4. Natural Periods Comparison

Case	Analytical		Numerical Free Decay	
	fn_1 (s)	fn_3 (s)	fn_1 (s)	fn_3 (s)
1	20.26	1.17	20.35	1.20
2	28.12	1.30	24.50	1.30
3	36.37	1.45	31.20	1.45
4	23.18	1.36	23.00	1.45
5	27.07	1.53	26.00	1.70
6	32.63	1.97	28.00	2.25

For the range of wave periods analysed in this study, the surge natural periods are not situated in a region where resonance may occur due to wave first order excitation. The heave natural periods, however, approach the lower end of the typical wave period range. In this investigation, a larger range of periods was applied in one case to determine whether the solution presents a peak around the surge natural frequency. The peak was identified and may be observed below for the 0.5 m diameter case.

Figure 4. Surge Natural Frequency Illustration – 0.5m Diameter Case

5 NUMERICAL RESULTS AND DISCUSSION

5.1 Influence of diameter size on dynamic response

The response amplitude operator plots for each diameter case are illustrated in Figure 5.

Figure 5. RAO's - Diameter variations

As can be seen from the surge RAO plot in Figure 5, the responses are mostly insensitive to diameter increases. It was initially believed that the surge response would be sensitive to diameter changes considering that the external force vector will increase with larger submerged cross-sections. The Froude-Krylov, viscous drag, added mass and diffraction forces are all taken proportional to the immersed cross-sectional area (A_S), however, these increases may be counterbalanced by the greater inertial forces required to accelerate the structure. In each diameter case, the surge RAO exhibits large responses between 2 and 3 s periods.

The heave response is found to be insensitive to diameter changes with the exception between the 2-2.5 s periods. In this low period range, the heave response appears to decrease as the diameter increases. It is unclear why this occurs, it may partly be described by larger non-linear variations in hydrostatic forces occurring with smaller diameters or by the lower restoring force capabilities. This occurrence will be explored in future studies.

The response spectra and amplitudes plots are illustrated in Figure 6. These plots display the expected response of each system, in each of the degrees of freedom, when exposed to the Jonswap wave spectrum. The surge spectra present two peaks

at 3 s and 5 s (wave). The first peak occurs due to the large response in the 2-3 s region of the response amplitude operator and the second peak represents the main wave frequency. The heave spectra and amplitudes display a singular peak at the main wave frequency. The pitch response illustrates one large peak, which spans the entire period range and decays as the period increases beyond the main wave frequency.

Figure 6. Response Spectra and Amplitudes - Diameter variations

5.2 Influence of cylinder submergence on dynamic response

The response amplitude operator plots for each submergence cases are illustrated in Figure 7.

As can be seen from the surge RAO plot the responses are partly sensitive to submergence changes and are found to be more sensitive in the low period region than the diameter cases. Below 3.5 s the largest draft has the lowest response. To quantify the decrease, the surge response difference is approximately one third between the 50% and 95% submergence cases at the 2 s period. This may indicate that if high-frequency waves are forecasted at the installation site, then greater submergences may be considered to minimise forces and surge motions. The evident response trend in the low period region goes against what may be expected; 'larger submergences will result in higher external forces, viscous drag forces and inertial forces'. However, the response does not present itself in such a way that confirms this hypothesis. This trend may also be described by the greater forces required to accelerate

the structure due to the larger mass associated with greater submergences.

Figure 7. RAO's - Submergence variations

The heave RAO plot illustrates that the response is sensitive to submergence changes throughout the period range. The heave response appears to increase linearly with submergence above periods of 4.5 s. However, below 4.5 s the response becomes non-linear; this trend may be linked to the greater non-linearities occurring in the higher frequency waves. Furthermore, the response peak in the 2 – 2.5 s periods is believed to be approaching the heave

Figure 8. Response Spectra and Amplitudes - Percentage submergence variations 0- degree wave heading

natural period and correlates with Table 4. The heave and pitch present a degree of coupling; a trough is observed in the heave RAO at approximately 2.5 s and the pitch RAO peaks at the same period. The location of the peak pitch responses may be linked to the wavelength and cylinder spacing, which is identical in each case.

In Figure 8, the response spectra and amplitude plots for the submergence cases can be observed. In the surge plots, two peaks are observed, which appear to decay towards a singular peak as the submergence increases. The heave plots illustrate the greater responses with submergence at the main wave period.

6 CONCLUDING REMARKS

A non-linear time-domain modelling method is applied to determine the coupled dynamic responses of various simplified FPV configurations, and the responses have indicated the following:

- Increasing the cylinder diameter has little influence on the responses,
- Larger submergences tend to have lower surge responses in the lower periods and converge above 4.5 s periods. Submergence increases will cause a higher response in heave throughout the entire period range,
- Two peaks are present in the surge responses of the 50%, 65%, and 80% submergence cases, however, these tend to decay and merge with the main wave period as the submergence increases.

A larger cylinder diameter will raise project costs due to the greater cost of components and possibly with transportation. However, overtopping effects may be avoidable and higher payloads can be achieved. A compromise of increasing the submerged volume of the cylinders is related to the reduced capability of providing restoring forces. Furthermore, the high transverse metacentric heights imply most design cases will be stable in the roll motion. The low longitudinal metacentric heights imply the system will be less stable in pitch.

The results presented in this paper may be useful for designing modular FPV systems which utilise cylindrical floats of similar diameter. The Riflex software is capable of modelling FPV devices that utilise horizontal semi-immersed cylinders but is limited to regular waves and is thought to lose some accuracy when higher frequency waves are applied. The magnitude of the error is currently unknown, although some preliminary experimental testing has provided a small insight into the inaccuracy. The experimental investigation is currently ongoing and will be discussed in subsequent studies. The preliminary nature of these tests notwithstanding, useful insights are drawn revealing that overtopping,

internal sloshing and potential damping occur in some load cases. These are all physical effects that have not been included in the numerical model.

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