

On intermediate-scale open-sea experiments on floating offshore structures: Feasibility and application on a spar support for offshore wind turbines

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1 **On intermediate-scale open-sea experiments on floating offshore structures:**
2 **feasibility and application on a spar support for offshore wind turbines**

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8
9 **Abstract**

10 Experimental investigation of floating structures represents the most direct way for achieving their dynamic
11 identification and it is particularly valuable for relatively new concepts, such as floating supports for
12 offshore wind turbines, in order to fully understand their dynamic behaviour. Traditional experimental
13 campaigns on floating structures are carried out at small scale, in indoor laboratories, equipped with wave
14 and wind generation facilities. This article presents the results of a 1:30 open-sea experimental activity on a
15 scale model of the OC3-Hywind spar, in parked rotor conditions, carried out at the Natural Ocean
16 Engineering Laboratory (NOEL) of Reggio Calabria (Italy). The aim of the experiment is two-fold. Firstly, it
17 aims to assess the feasibility of low-cost, intermediate-scale, open-sea activities on offshore structures,
18 which are proposed to substitute or complement the traditional indoor activities in ocean basins. Secondly,
19 it provides useful experimental data on damping properties of spar support structures for offshore wind
20 turbines, with respect to heave, roll and pitch degrees of freedom. It has been proven that the proposed
21 approach may overcome some limitations of traditional small-scale activities, namely high costs and small
22 scale, and allows to enhance the fidelity of the experimental data currently available in literature for spar
23 floating supports for offshore wind turbines.

24 **Keywords**

- 25 1. Offshore structures
26 2. Spar
27 3. Floating wind turbines
28 4. Physical model
29 5. Heave damping
30 6. Roll damping

31
32 **Introduction**

33 Nowadays, floating structures are used by several industries, including oil & gas, renewables (wind, wave,
34 tidal), ports, and others, while the development and spread of many further concepts is envisaged to take
35 place in a near future [1]. In the case of offshore wind industry, several advantages in moving offshore wind
36 energy production towards deep waters can be exploited, including the availability of larger areas, stronger
37 and steadier winds, and the reduction of visual and acoustic impact. However, the development of such
38 concepts requires a significant amount of research in multiple areas of knowledge, including the

39 development of reliable dynamic models, able to represent the coupled behaviour of the floating wind
40 turbines [2-3]. While such models are usually implemented by means of numerical codes [4-5],
41 experimental activities play a crucial role for their validation, as well as for the system identification.

42 The experimental activities on floating offshore wind turbines may be classified in two groups, namely
43 small-scale and large-scale ones. Traditional small-scale activities (1:50-1:100) are carried out in controlled
44 environment such as wave tanks and ocean basins, where the desired wind-wave conditions can be
45 reproduced, to measure the dynamic response of the structure and to calibrate opportunely the numerical
46 codes [6-8]. Although the controlled environment allows to achieve very precise and reliable results, these
47 activities have some relevant disadvantages, namely high rental fees of the basins, limited duration of the
48 experiments, and limitations in representing all the relevant physical phenomena at scale level, which may
49 alter significantly the dynamic behaviour of the model with respect to the full-scale structure. On the
50 opposite side, large-scale activities (1:1-1:10) are carried out in open-sea and allow to represent all the
51 relevant features of the offshore wind turbines, including turbine-support interaction, mooring system and
52 grid connection, in relevant operational conditions [9-11]. Clearly, such projects are very expensive and
53 usually represent pilot activities, which are carried out by big companies and/or public bodies for
54 demonstration and commercial purposes, and whose results are rarely publicly available.

55 **1.1 Literature review**

56 Up to now, several small-scale and large-scale experimental activities have been conducted on spar support
57 structures for offshore wind turbines, aimed to prove the feasibility of the concept and validate the
58 corresponding numerical models. A full-scale prototype of a 2.3 MW spar floating offshore wind turbine
59 was installed in 2009 by Statoil [12], off the coast of Norway, on a water depth of about 200 m. The project,
60 called "Hywind Demo", has proved the technical feasibility of the spar configuration for floating offshore
61 wind turbines, but neither the detailed design characteristics of the offshore wind turbine, nor the
62 recorded field data are publicly available. In 2006, a 1:47 scale model of a 5-MW spar floating wind turbine
63 was tested by Nielsen et al. [13] at the Ocean Basin Laboratory of Marintek, in Trondheim (Norway). The
64 model was tested in irregular waves and turbulent wind speed and various control strategies were
65 adopted. The experimental data showed relatively good agreement with the numerical results obtained in
66 SIMO/RIFLEX, however some information were not released, concerning the detailed characteristics of the
67 model. In 2010, the Offshore Code Comparison (OC3) project [4] was established to verify the accuracy and
68 correctness of the most commonly used numerical codes for coupled analysis of offshore wind turbines.
69 Within this project, the OC3-Hywind spar buoy [14] was defined as the reference spar concept designed to
70 support the NREL-5MW reference offshore wind turbine [15]. Since then, this concept has been widely
71 used for experimental studies on offshore wind turbines, since Statoil's Hywind characteristics are not
72 released for public use. In 2011, a 1:128 scale model of the OC3-Hywind platform was tested by Shin [16] at
73 the Ocean Engineering Wave Tank of the University of Ulsan (South Korea). The experiment was conducted
74 under various environmental conditions, including regular waves, associated to constant wind speed and
75 rotating rotor, and irregular waves, associated to fixed rotor. Response Amplitude Operators (RAOs) and
76 significant motions were obtained and compared to numerical predictions obtained using FAST and MOSES
77 codes. In 2010, the Japanese Ministry of Environment started a national demonstration project which led to
78 the installation of a 1:2.35 (2012) and a 1:1 (2013) grid-connected 100 kW and 2 MW spar wind turbines off
79 Goto Islands (Japan), preceded by the testing of a 1:100 scale model, in the offshore structure basin of
80 National Maritime Research Institute (NMRI) in Tokyo (Japan), and of a 1:10 model at sea [9-10,17]. The
81 small-scale test was aimed to study the dynamic behaviour of the model, particularly concerning the effects
82 of blade-pitch control, under regular and irregular waves and constant wind speed. The 1:10 model was

83 aimed to further assess the safety and the performances of the chosen concept. Limited information are
84 available concerning the 1:1 test activity, while experimental data collected in 2012 during a severe
85 typhoon were compared to numerical predictions in FAST by Utsunomiya et al. [18], revealing quite good
86 agreement. In 2012, a 1:50 scale-model of a spar floating wind turbine was tested within a project
87 conducted by the University of Maine [19-20], aimed to compare it with a semi-submersible and a Tension-
88 Leg Platform model. The three models were tested at Maritime Research Institute Netherlands (MARIN) in
89 Wageningen (Netherlands) under various wind-wave conditions, including dynamic wind and bi-directional
90 sea states. Full system identification was achieved, including estimation of the damping ratios and the
91 RAOs. In 2013, a 1:100 scale model of a stepped-spar was tested by Sethumaran and Venugopal [21] at the
92 curved wave tank of the University of Edinburgh (Scotland, UK). Only hydrodynamic behaviour of the model
93 was investigated through regular and irregular wave tests and the experimental results in the frequency
94 (RAOs) and time domain were compared to numerical predictions obtained using Orcaflex code. Also,
95 different mooring systems were tested and their performances were compared. In 2016, a 1:50 scale model
96 of OC3-Hywind spar was tested at MARIN by Duan et al. [22], who obtained response spectra for various
97 load combinations and provided a deeper insight on the coupled dynamics of spar floating wind turbines.
98 All these activities provided useful experimental results on the spar dynamic behaviour, but suffered from
99 the above-mentioned limitations, in particular concerning the relatively high costs associated to basin
100 rental, the restrictions in experimental duration and the insurgence of scale effects.

101 **1.2 Aim**

102 The purpose of this paper is to present the results of a 1:30 scale experimental activity on a spar floating
103 support for offshore wind turbines, carried out near shore at the Natural Ocean Engineering Laboratory
104 (NOEL) of Reggio Calabria (Italy). The support model tested is inspired to OC3-Hywind and has been
105 represented in parked rotor conditions. The experimental activities carried out during this campaign were
106 aimed at addressing and solving some of the problems inherent to the traditional experimental activities in
107 ocean basins (some initial results were published in references [23-24]).

108 In recognition of the fact that the well-known identification techniques adopted in indoor laboratories must
109 be modified to work in a non-controlled marine environment, this paper offers a broad-wide overview
110 about the requirements, test methodologies, instrumentations and identification methods necessary for
111 operating dynamic identification of offshore floating structures' intermediate-scale models in open-sea
112 conditions. Then, the results obtained from the free decay tests and the irregular wave tests performed on
113 the 1:30 spar structure are presented in terms of RAOs, damping coefficients and significant motions in
114 heave, roll and pitch, in order to calibrate a numerical model of the structure implemented through the
115 software Ansys AQWA [25-26]. These experimental data represent valuable and original information on the
116 hydrodynamic behaviour of the OC3-Hywind platform, since the scale factor chosen for the model is
117 sufficiently high to minimize the scale effects in the representation of wave forces. Finally, the feasibility of
118 the new approach proposed is discussed and its main advantages and limitations are outlined.

119

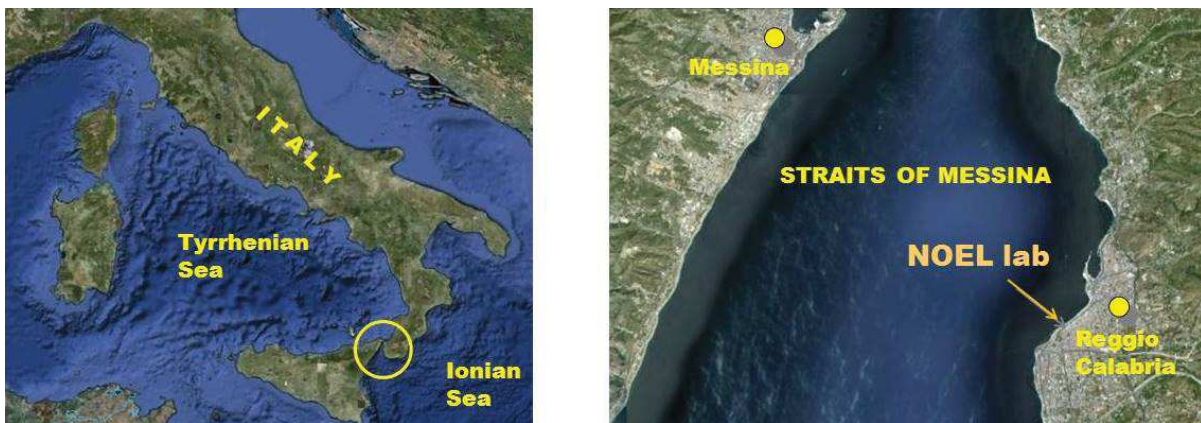
120 **Experimental set-up**

121 **2.1 Suitable test site characteristics**

122 The choice of the test site is a crucial step for the arrangement of intermediate-scale, open-sea,
123 experimental activities, since local wind-wave conditions must fulfil some restrictive requirements. First of
124 all, the local waves at the test site must be representative of the full-scale conditions, according to Froude
125 scaling laws. As observed by Boccotti [27], such a condition can be obtained if the wave spectrum of the

126 model waves is a scale model of the design spectrum at all the water depths. Such a requirement is
127 particularly important when dealing with deep-drafted structures, such as spar buoys, and is challenging to
128 achieve in open sea, since smaller sea states are usually made up of wind-generated waves superimposed
129 by swells, which have a higher impact on the water particle kinematics at deeper levels and alter
130 significantly the similitude with respect to full-scale wind-generated design sea states. On the other hand, it
131 is known that, while small wind-generated sea states are essential for modelling the dynamic response in
132 Froude scale, confused and mixed sea-states play a crucial role for the realization of this kind of
133 experimental activities. Indeed, although they do not represent in scale any realistic condition, they allow
134 to investigate the response of the model structure over a larger frequency range, possibly close to its
135 natural frequencies, so that estimation of RAOs and damping coefficients can be achieved. Finally, the test
136 site should present other characteristics, favourable for field experiment activities [27].

137 The test site chosen for the present study is the Natural Ocean Engineering Laboratory (NOEL), located in
138 the sea front of Reggio Calabria (Italy), on the eastern coast of Messina's Strait (LAT 38°06.538'N; LONG
139 15°38.478'E) (Fig.1). The site is particularly favourable for the selected case study, since it presents small
140 wind-generated sea states with significant wave heights H_s between 0.20 m and 0.40 m, peak periods T_p
141 between 1.8 s and 2.6 s and JONSWAP-like spectra, which occur regularly, as well as the required variety of
142 wave loading condition for model identification over a relatively large frequency range. The realization of
143 these sea states is due to the unique environmental and geographical characteristics of the area, i.e. the
144 local NNW wind, blowing from Sicily with a high stability, the orientation of the coast from SW to NE,
145 sheltering the swells coming from South and the relatively small fetch, which is about 10^4 m. Finally, the
146 tide amplitude is within 0.1 m: such a small value is of crucial importance for experimental testing of
147 offshore structures, since larger variations of the water depth may affect significantly the operation of the
148 mooring system. A detailed description of the test site metocean conditions is available in ref. [27-29]



149
150 *Figure 1 - NOEL site [29].*

151 **2.2 Importance of scale effects for spar structures**

152 The Froude scaling laws, in terms of the variables of interest for the scale models of floating wind turbines,
153 are reported in Table 1.

154

<i>Parameter</i>	<i>Scale factor</i>	<i>Units</i>
Length	λ_L	m
Angle	$\lambda_\theta = 1$	rad
Time	$\lambda_T = \lambda_L^{0.5}$	s
Velocity	$\lambda_v = \lambda_L^{0.5}$	m s ⁻¹
Angular velocity	$\lambda_\omega = \lambda_L^{-0.5}$	rad s ⁻¹
Acceleration	$\lambda_a = 1$	m s ⁻²
Density	$\lambda_\rho = 1$	kg m ⁻³
Mass	$\lambda_m = \lambda_L^3$	kg
Mass moment of inertia	$\lambda_I = \lambda_L^5$	kg m ²
Force	$\lambda_F = \lambda_L^3$	N
Power	$\lambda_P = \lambda_L^{3.5}$	W
Keulegan-Carpenter number	$\lambda_K = 1$	-
Reynolds number	$\lambda_R = \lambda_L^{1.5}$	-

156 In Table 1, Keulegan-Carpenter number K_C and Reynolds number R_e are defined as functions of the
 157 maximum wave velocity v_{max} , a characteristic wave period T , which is usually identified as the peak period
 158 of the sea state, a characteristic length of the structure D , which is usually identified as its diameter, and
 159 the water kinematic viscosity ν :

$$160 \quad K_C = \frac{v_{max} T}{D} \quad (1)$$

$$161 \quad R_e = \frac{v_{max} D}{\nu} \quad (2)$$

162 As Froude scale inevitably alters Reynolds number R_e , several challenges arise in the scale representation of
 163 the wind turbine and support hull. Since in this work only the parked rotor conditions will be investigated, a
 164 detailed treatment of the scaling procedures regarding the wind turbine will not be further discussed, see
 165 for example [30-32]. Regarding the hull, wave-structure interaction depends on the relative size of the
 166 structure with respect to the wavelength. Morison’s equation [33] can be adopted when the spar radius R is
 167 smaller than $0.10L$, being L a characteristic wave length, representative of the sea state [34]. According to
 168 Jonkman, [14], such a condition is fulfilled for all the most relevant operational and ultimate conditions of
 169 the OC3-Hywind spar buoy, hence the unit wave force vector on the hull may be expressed as the following
 170 function of the vertical distance z from still water level:

$$171 \quad \mathbf{f}(z, t) = (1 + C_a) \rho \pi R^2(z) \mathbf{a}(z, t) - C_a \rho \pi R^2(z) \ddot{\mathbf{u}}(z, t) + \\ + C_d \rho R(z) |\mathbf{v}(z, t) - \dot{\mathbf{u}}(z, t)| [\mathbf{v}(z, t) - \dot{\mathbf{u}}(z, t)] \quad (3)$$

172 where \dot{f} represents the time derivative of function f , ρ is the water density, \mathbf{v} and \mathbf{a} are wave velocity and
 173 acceleration vectors, and \mathbf{u} is the structure rigid body motion vector. The added mass and drag coefficients
 174 C_a and C_d generally depend on Keulegan-Carpenter and Reynolds numbers [35] but they can be assumed
 175 constant if the following condition is fulfilled [27]:

$$176 \quad R_e > 10^4 K_C \quad (4)$$

177 Condition (4) is generally satisfied for full-scale offshore wind turbine spar under all the most relevant wave
 178 conditions [23], but the same may not be true for the corresponding small-scale models. Indeed, the
 179 Reynolds number scales with a factor equal to $\lambda_L^{1.5}$, hence condition (4) may be violated for smaller models,
 180 resulting in scale effects due to the alteration of the hydrodynamic coefficients, which describe wave-
 181 structure interaction (see, for example references [36-37]).

182 Other challenges related with the scaling of floating structures concern the mass distribution of the
 183 structure and the mooring system. It is almost impossible, indeed, to scale down exactly all the structural
 184 properties, e.g. wall thickness, because manufacturing as well as strength requirements of the scale model
 185 must be taken in due consideration. Hence, in general it shall be ensured that the scale model matches the
 186 full-scale structure in terms of global properties such as mass, position of the centre of gravity, mass
 187 moments of inertia and mooring linearized stiffness. Also in this case, the smaller is the model, the more
 188 challenging is to fulfil these requirements.

189 2.3 Description of the 1:30 spar model

190 The OC3-Hywind spar buoy is basically a vertical tapered cylinder, moored to seabed by three catenary
 191 lines, and supporting the NREL-5MW reference offshore wind turbine [14]. The 1:30 model installed at
 192 NOEL (Fig. 2) is made up of a steel hull and an aluminium tower, separated by a plastic disc to avoid
 193 galvanic corrosion. The hull has been ballast-stabilized by steel discs deployed in its lower section, so that
 194 the required position of the centre of gravity has been achieved. The rotor-nacelle assembly (RNA) has
 195 been modelled as a lumped mass at the top of the tower. The main geometry and mass characteristics are
 196 reported, respectively, in Table 2 and Table 3, while some additional details on the design of the model are
 197 described in Ref. [23].



198
 199 Figure 2 – Spar hull model before (left) and after (right) installation, sustaining the parked turbine model
 200 [29]

201 Table 2 – Geometry of the structure at model scale and full scale and comparison with the OC3-Hywind.

<i>Parameter</i>	<i>Units</i>	<i>Scale model (1:30)</i>	<i>Full-scale structure (1:1)</i>	<i>OC3-Hywind (1:1)</i>
Diameter	m	0.217; 0.313	6.51; 9.39	6.50; 9.40
Draft	m	3.884	116.5	120.0
Centre of buoyancy, vertical position (from SWL)	m	-1.974	-59.21	-62.06
Taper, vertical position	m	-0.020; -0.287	-0.61; -8.62	-4.00; -12.00

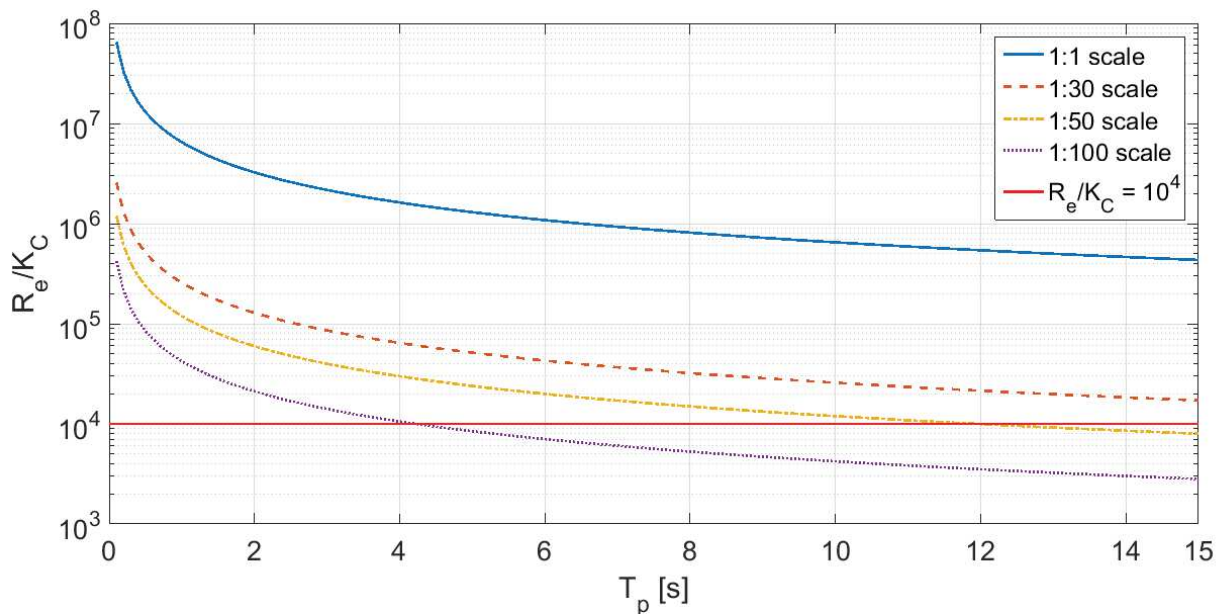
(from SWL)				
RNA, vertical position (from SWL)	m	3.080	92.39	90.00

202

203 Table 3 – Mass characteristics of the structure at model scale and full scale and comparison with the OC3-
204 Hywind.

Parameter	Units	Scale model (1:30)	Full-scale structure (1:1)	OC3-Hywind (1:1)
Total mass	kg	297.0	$8.02 \cdot 10^6$	$8.07 \cdot 10^6$
Centre of gravity, vertical position (from SWL)	m	-2.496	-74.88	-78.00
Moment of inertia about x-axis (respect to the CoG)	kg m ²	994.9	$2.42 \cdot 10^{10}$	$2.30 \cdot 10^{10}$
Moment of inertia about y-axis (respect to the CoG)	kg m ²	995.0	$2.42 \cdot 10^{10}$	$2.30 \cdot 10^{10}$
Moment of inertia about z-axis	kg m ²	4.870	$1.18 \cdot 10^8$	$1.91 \cdot 10^8$

205 Based on condition (4), it is possible to estimate how hydrodynamic coefficients of Morison’s equation are
206 altered by the scale factor chosen. To this aim, Fig. 3 shows how the ratio between Reynolds number and
207 Keulegan-Carpenter number varies with the scale factor for the spar support considered, assuming the
208 wave peak period T_p at full scale as the characteristic period T and the diameter of the surface-piercing
209 section of the spar hull as the characteristic length D in equations (1-2).



210

211 Figure 3 – Variation of R_e/K_C ratio depending on the scale factor.

212 Fig. 3 confirms that the distortion of Reynolds number at 1:30 scale is rather limited, so that a correct
 213 representation of the hydrodynamic coefficients can be achieved in the whole range of peak periods
 214 considered. Differently, 1:50 models may present slight alterations of the hydrodynamic coefficients for the
 215 highest periods (scaled representation of severe storms and swells) and 1:100 models significantly alter
 216 Reynolds number, resulting in a distortion of wave forces in the whole range of realistic wave peak periods.
 217 This is a significant advantage of intermediate-scale open-sea field experiments in terms of representation
 218 of the actual dynamic behaviour of spar floating supports for offshore wind turbines.

219 The mooring system of the model structure has been designed considering the irregular and inclined
 220 seabed at the NOEL site, and the local harshest metocean conditions. It is based on the full-scale mooring
 221 system design of UMaine-Hywind spar [38], which is equivalent to OC3-Hywind but intended for a water
 222 depth of 200 m instead of 320 m. This reference design, however, has been used only for defining the
 223 shape of the mooring system, i.e. three 120°-spaced catenary lines, and the weight per unit length of each
 224 line. Differently, the position of the anchors and the length of the lines have been set so as to match local
 225 bathymetry and to ensure structure safety under local extreme conditions. Consequently, the resulting
 226 mooring system of the model has longer mooring lines and smaller stiffness with respect to the full-scale
 227 UMaine Hywind spar structure. Moreover, the model mooring system is asymmetric, due to the different
 228 water depths of the sea-side and land-side anchors. The design of each line has been carried out through an
 229 in-house quasi-static numerical code, based on the particularization of the catenary equation to inclined
 230 seabed conditions. Let us consider a 2-D reference system $Ax'z'$ in the plane of the catenary line, originating
 231 in the anchor point A and pointing towards the structure (z' positive upwards). Then, the catenary equation
 232 may be written as:

$$233 \quad z'(x') = \frac{F_{x'}}{w} \cosh \left[\frac{w}{F_{x'}} x' + \operatorname{arcsinh}(\tan \beta_A) \right] - \frac{F_{x'}}{w} \sqrt{1 + \tan^2 \beta_A} \quad (5)$$

234 being $F_{x'}$ the horizontal tension of the line, w the weight per unit length and β_A the inclination at the anchor
 235 point. At the same time, in the local curvilinear system of the line Asn (s positive towards the structure), the
 236 shape of the line may be expressed as:

$$237 \quad s(x') = \frac{F_{x'}}{w} \sinh \left[\frac{w}{F_{x'}} x' + \operatorname{arcsinh}(\tan \beta_A) \right] - \frac{F_{x'}}{w} \tan \beta_A \quad (6)$$

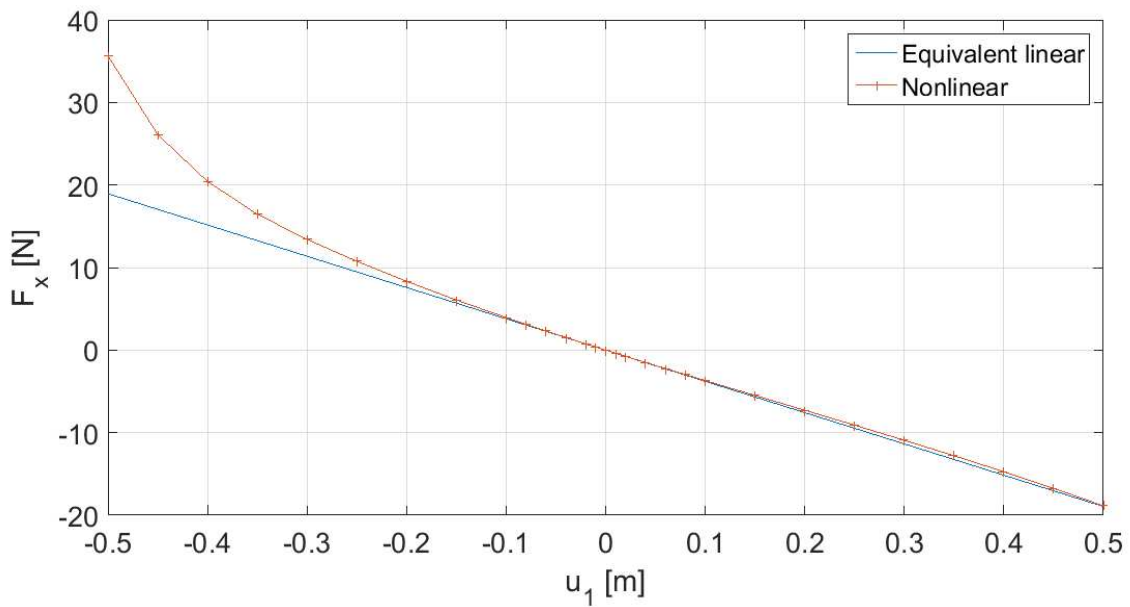
238 The quasi-static shape and restoring force of each line has been obtained as a function of surge and sway
 239 structure motions by combining equations (5-6) and the boundary conditions, i.e. the fixed position of the
 240 anchor, the variable position of the structure and the length of the line. The resulting restoring forces of the
 241 three lines for each structure configuration have been summed in the global 3-D reference system $Gxyz$, to
 242 obtain the global restoring forces for the structure. The final design of the lines has been obtained through
 243 a trial-and-error procedure to achieve the desired stiffness. The resulting characteristics of the mooring
 244 system are reported in Table 4, while Fig. 4-5 show the restoring forces as functions of the surge and sway
 245 motions of the model. Although these forces are nonlinear, linearized surge and sway stiffness can be
 246 defined as the tangent stiffness of the force-motion function in the equilibrium position. The corresponding
 247 linearized behaviour is reported in Fig. 4-5 too. In the installation phase, the yaw stiffness has been
 248 enhanced through a delta connection of the mooring lines. The length of each side of the delta has been set
 249 equal to about 10% of the corresponding line length, following the practical instructions of Quallen et al.

250 [39]. Additional details on the numerical model used for the design of the mooring system of the model
 251 may be found in Ref. [23]. A picture of the mooring system taken after installation is shown in Fig. 6.

252 Table 4 – Characteristics of the mooring system of the model structure.

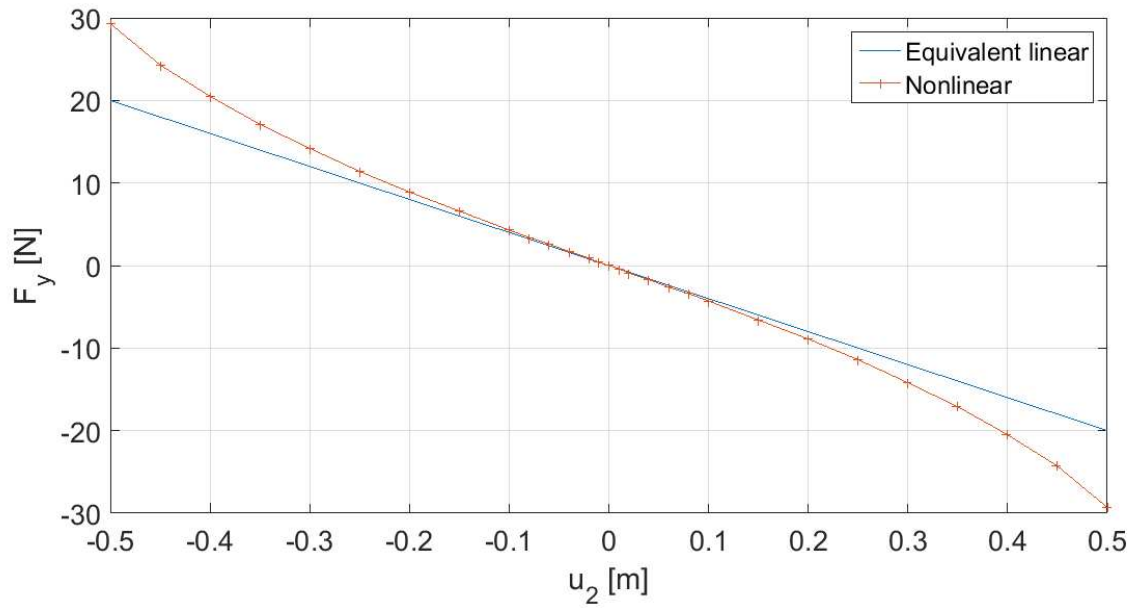
Parameter	Units	Model (1:30)	
		Sea-side	Land-side
Fairleads, vertical position (from SWL)	m	-2.224	-2.224
Anchor depth	m	10.40	2.75
Seabed inclination	°	14.21	18.09
Linear mass of the line	kg m ⁻¹	0.159	
Radius anchor-spar centreline	m	14.04	12.92
Line length	m	16.56	13.30
Delta connection length	m	1.50	
Top tension	N	24.06	16.99
Touchdown length	m	4.91	3.15
Linearized surge stiffness	N m ⁻¹	37.89	
Linearized sway stiffness	N m ⁻¹	42.80	

253



254

255 Figure 4 – Nonlinear and linearized force-motion behavior of the mooring system of the model in quasi-static
 256 conditions for surge degree of freedom



257

258 *Figure 5 -Nonlinear and linearized force-motion behavior of the mooring system of the model in quasi-static*
 259 *conditions for sway degree of freedom*



260

261 *Figure 6 - Catenary mooring system of the model, including delta connections [29]*

262

263 **2.4 Measurement systems**

264 The experimental activity has been carried out using two independent measurement systems.

265 The first measurement system is for wave data collection, and is made up of two fixed poles, installed close
 266 to the spar model, in an undisturbed wave field. Each pole sustains an emerged ultrasonic probe, to
 267 measure the time history of the wave surface elevation at a fixed point, and a submerged pressure
 268 transducer, to measure the wave pressure time history at the same point, at an average depth of 0.78 m.
 269 The four sensors have been synchronized and the correlation factor between ultrasonic probes and

270 pressure transducers output has been estimated for each sea state, resulting generally greater than 0.85.
271 However, the ultrasonic probes experienced some temporary breakdown (about 28% of data), hence the
272 pressure transducers have been used for the estimations of the wave spectra, due to their greater
273 reliability. Significant wave height, wave spectrum and mean propagation direction for each sea state have
274 been obtained following the methods described in Ref. [27]. Also, the tide variation of the sea level has
275 been calculated for each sea state.

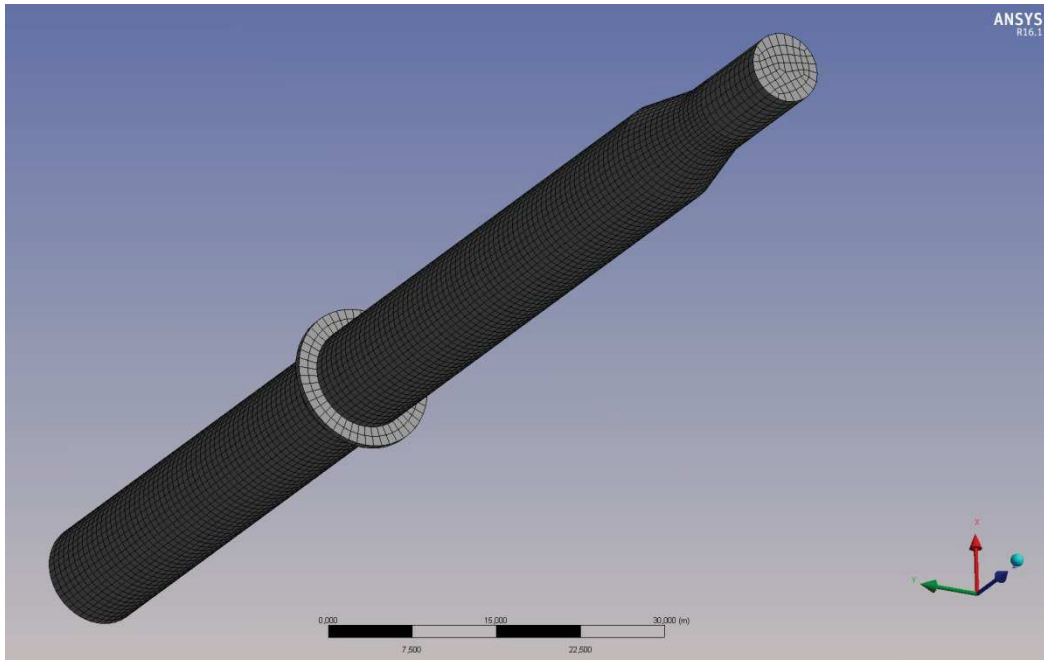
276 The second system is devoted to the measure of the spar dynamic response, and is composed of a
277 differential global positioning system (DGPS), which returns the position of the structure with respect to a
278 fixed point on the land, and an Attitude and Heading Reference System (AHRS) inertial platform, which
279 measures the inclination of the platform with respect to the three axes.

280 The two measurements systems have been synchronized, and both have a sampling frequency of 10 Hz.

281 **2.5 Numerical model of the structure**

282 A numerical model of the spar structure has been implemented in ANSYS AQWA (v. 16.1) [26], and has
283 been calibrated with experimental data collected at NOEL. The whole numerical model has been
284 implemented at 1:1 scale, which returns an immediate comparison with OC3-Hywind full-scale structure.

285 Following the approach of Jonkman [14], the hydrodynamic loads have been modelled adopting a potential
286 theory approach, augmented by the drag term of Morison's equation. According to Jonkman, delta
287 connections of the mooring lines have been represented through an additional yaw stiffness equal to $K_{\delta\delta} =$
288 $9.834 \cdot 10^7 \text{ Nmrad}^{-1}$, and the linear additional damping coefficients $B_{11} = B_{22} = 1.000 \cdot 10^5 \text{ Nsm}^{-1}$, $B_{33} =$
289 $1.300 \cdot 10^5 \text{ Nsm}^{-1}$, $B_{\delta\delta} = 1.300 \cdot 10^7 \text{ Nsmrad}^{-1}$ have been added. Initially, the additional damping matrix has
290 been set by Jonkman based on small-scale free decay tests operated by Statoil on a model of the Hywind
291 platform. Then, the additional damping matrix has been calibrated in Section 3.2, using the experimental
292 data collected on the 1:30 model installed at NOEL. The hull has been represented through a "Point mass"
293 item (Table 3), and the mesh includes 6735 diffracting elements (Fig. 7) and 104 line bodies, which are used
294 to include the viscous drag forces. The mooring system has been represented by three catenary lines on a
295 constant water depth of 200 m, sized so that linearized horizontal stiffness is equivalent to the value
296 reported in Table 4 for the structure installed at NOEL.

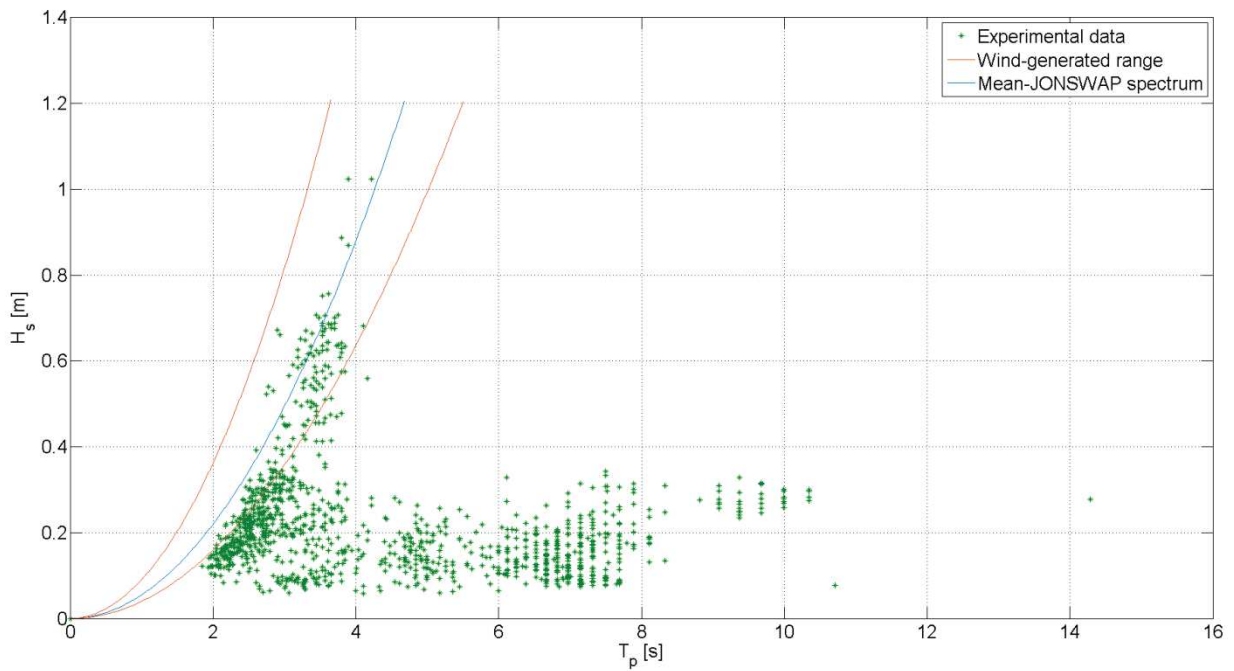


297

298 *Figure 7 - Numerical model of the spar structure in ANSYS AQWA*

299 **3. Results of the experimental activity**

300 The experimental activity on the 1:30 model of OC3-Hywind platform started in July 2015 and was
 301 concluded in March 2016. An overall number of 1281 5-minute-long wave and motion records was
 302 collected selecting the widest range of local wave conditions possible. The scatter diagrams of significant
 303 wave heights with respect to peak periods are shown in Fig. 8.



304

305 *Figure 8 - Scatter diagram of the significant wave heights recorded at NOEL with respect to peak period.*

306 Wave data collected include both wind-generated local sea states, swells and mixed sea states. The wind-
 307 generated sea states are useful for the representation of the scaled behaviour of OC3-Hywind in severe
 308 wave conditions, while the other ones are important for the determination of the spar characteristics in the
 309 frequency domain. The discrimination criterion adopted in Fig. 8 to distinguish wind-generated data is the
 310 following:

$$311 \quad \begin{cases} \psi^* > 0.65 \\ gK_{\min} T_p^2 < H_s < gK_{\max} T_p^2 \end{cases} \quad (7)$$

312 being ψ^* the narrow-bandedness parameter of the wave head of pressure spectrum defined as in Ref. [27],
 313 g the acceleration due to gravity and K_{\min} , K_{\max} two steepness parameter. The narrow-bandedness
 314 parameter is obtained as the module of the minimum of the auto-covariance function of the wave pressure
 315 time series, normalized to the maximum of the same function, and tends to unity as the spectrum tends to
 316 be infinitely narrow. The first condition in (7) is hence aimed to exclude the mixed sea states, which are
 317 relatively broad-banded. The second condition is instead aimed to exclude pure swells, whose steepness is
 318 significantly lower than that of wind waves. The limit values of the steepness parameters used in (7) have
 319 been derived by Arena et al. [40], considering the whole range of shape parameters of the JONSWAP
 320 spectrum, which is representative of wind-generated waves [41]. The resulting limit values are $K_{\min} =$
 321 $4.057 \cdot 10^{-3}$ and $K_{\max} = 9.246 \cdot 10^{-3}$. Overall, 139 wind-generated sea states have been selected from the whole
 322 set of 1281 data, using the dual criterion in (7).

323 3.1 Damping estimations from free decay tests

324 An estimation of the natural frequencies and damping coefficients of the 1:30 model in heave, roll and
 325 pitch has been firstly obtained through free decay tests performed at sea, in sufficiently calm wave
 326 conditions ($H_s \leq 0.11$ m). The procedure for the realization of the tests has been described in Ref. [24].
 327 With respect to the traditional free decay tests carried out in ocean basins, the continuous irregular wave
 328 action alters the smallest decay cycles, making the tests shorter and slightly coarser.

329 The method adopted in this paper for the estimation of damping coefficients is derived from Ref. [42],
 330 where extensive description of the method and alternative formulations may be found. In synthesis, for
 331 each free decay test, each degree of freedom is treated as uncoupled and thus the corresponding 1-DOF
 332 equation of motion is:

$$333 \quad (M_{ii} + A_{ii})\ddot{u}_i + B_{ii}\dot{u}_i + D_{ii}|\dot{u}_i|\dot{u}_i + C_{ii}u_i = 0 \quad (8)$$

334 u_i being the degree of freedom (DOF) involved in the free decay test, M_{ii} the corresponding term in the
 335 inertia matrix, A_{ii} the hydrodynamic added mass, C_{ii} the stiffness term, B_{ii} the linear damping coefficient and
 336 D_{ii} the non-linear damping coefficient. Equation (8) implicitly assumes that the DOFs are not coupled, which
 337 is realistic for heave, pitch and roll. By equating the work done by the restoring moment and the energy
 338 dissipated by linear and nonlinear damping for each half-cycle (positive or negative) one obtains:

$$339 \quad \Delta u_{i,peak} = p_1 u_{i,mean} + p_2 u_{i,mean}^2 \quad (9)$$

$$340 \quad p_1 \equiv \frac{\pi}{2\omega_{n,ii}} \frac{B_{ii}}{(M_{ii} + A_{ii})} \quad (10)$$

341

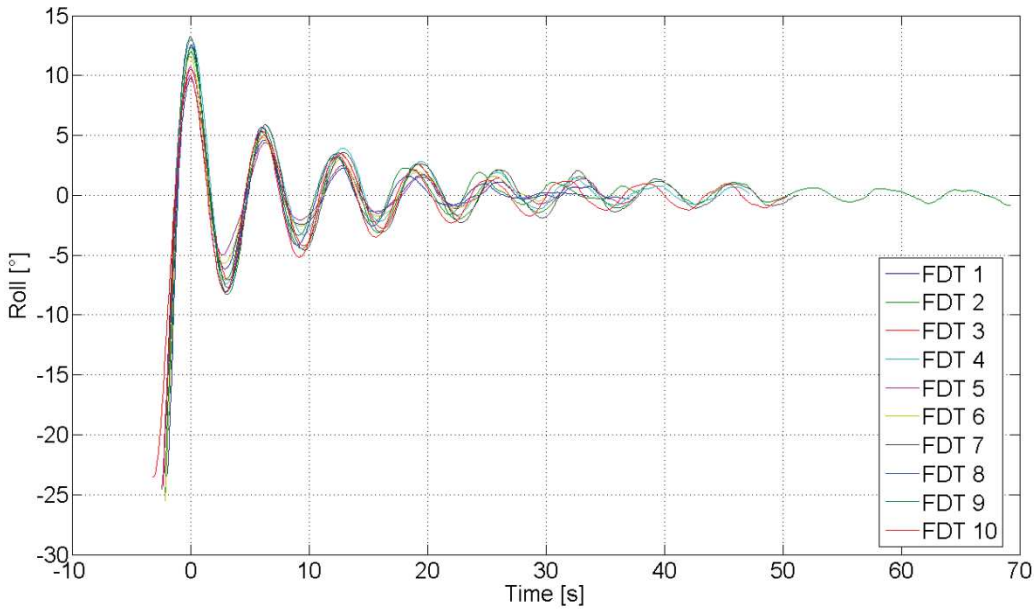
$$p_2 \equiv \frac{4}{3} \frac{D_{ii}}{(M_{ii} + A_{ii})} \quad (11)$$

342

$\omega_{n,ii}$ being the natural frequency, which can be read from the test, $u_{i,mean}$ the mean displacement value
343 between two successive peaks (positive or negative) of the test and $\Delta u_{i,peak}$ their difference.

344

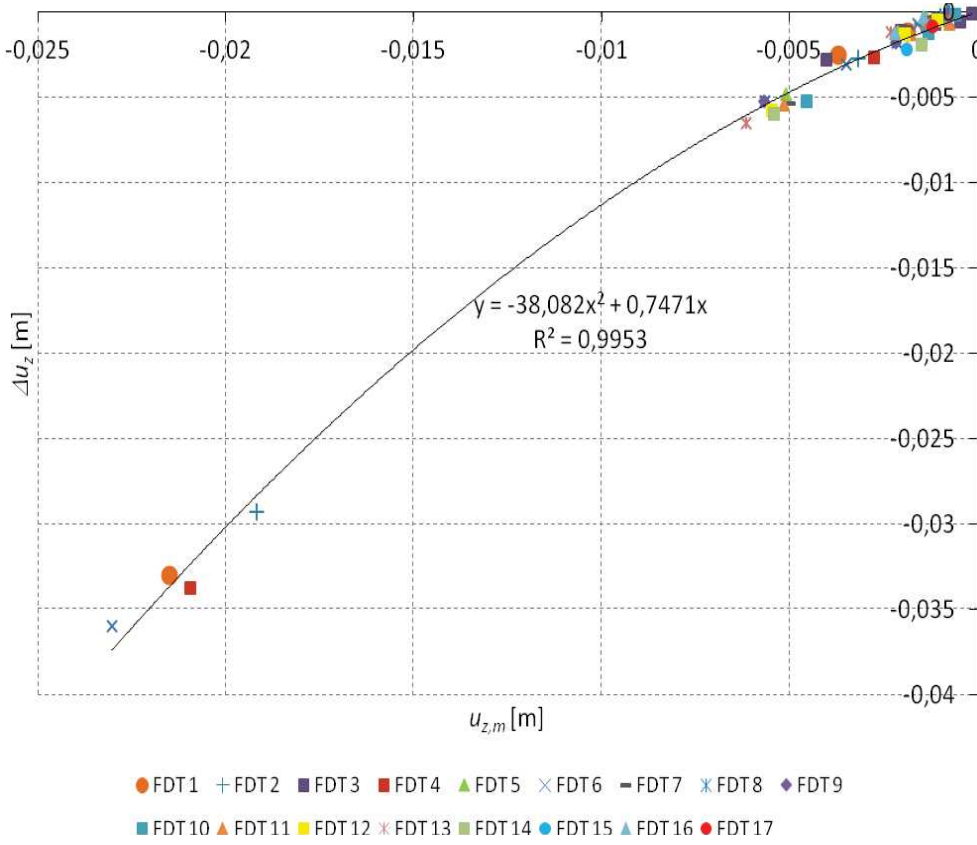
In total, 17 free decay tests have been conducted in heave, and 10 in roll/pitch (Fig. 9). Equation (9) has
345 been fitted to the experimental data collected from all the free decay tests, obtaining the curves shown in
346 Fig. 10 and Fig. 11, respectively. The resulting estimations in terms of natural frequencies and linear
347 damping coefficients are reported in Table 5. Interestingly, the estimates of the linear damping coefficients
348 are larger than those suggested by Jonkman [14], which were based on the results of free-decay tests
349 performed on a small-scale model of Statoil's Hywind. This may be due to the relatively large scale of the
350 model, which allows to reduce the scale effects and to obtain a more reasonable approximation of the
351 actual values to be used for the modelling of the full-scale structure.



352

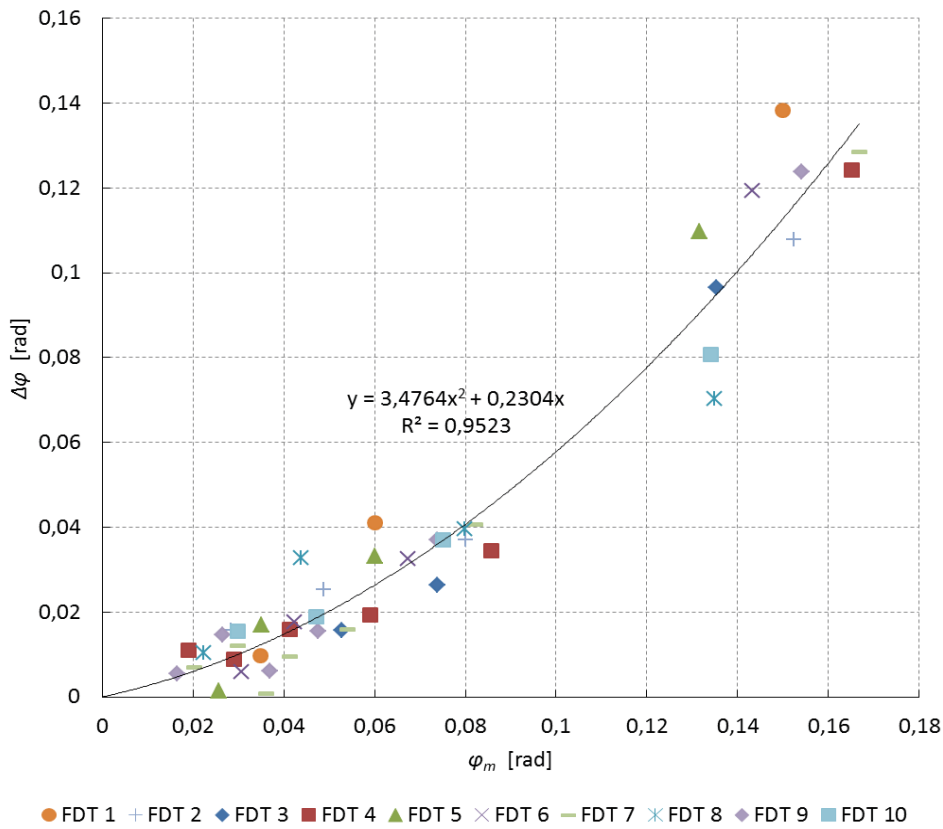
353

Figure 9 - Time histories of the ten roll free decay tests conducted at NOEL.



354

355 Figure 10 – Fitting of Equation (9) to experimental data for the heave degree of freedom.



356

357 Figure 11 – Fitting of Equation (9) to experimental data for the roll degree of freedom.

358

359 Table 5 – Experimental estimation of natural frequencies and linear damping coefficients via free decay
360 tests.

Degree of freedom	ω_n [rad/s]		B_n [kg s ⁻¹ ; kg m ² s ⁻¹ rad ⁻¹]	
	Model scale (1:30)	Full-scale (1:1)	Model scale (1:30)	Full-scale (1:1)
Heave	1.090	0.199	153.6	7.570·10 ⁵
Roll/Pitch	1.006	0.202	228.5	1.014·10 ⁹

361 The nonlinear damping coefficient D_{roll} has been used instead for the indirect estimation of the
362 hydrodynamic drag coefficient C_d of the hull. It is obtained by equating the nonlinear term of equation (8)
363 and the drag term of the roll moment about the centre of gravity, given by Morison's equation (3). Wave
364 elevation and spar motions in surge and heave have been neglected, so to obtain:

365

$$C_d = \frac{D_{roll}}{\rho \int_{z_{min}}^0 R(z) |z - z_G| (z - z_G)^2 dz} \quad (12)$$

366 z_{min} and z_G being the draft and the centre of gravity position of the model structure, respectively. The
367 resulting value is $C_d = 1.998$, which is consistent with the expected one for calm water conditions [37]. This
368 represents a further confirmation of the feasibility of Morison's equation for the representation of viscous
369 drag hydrodynamic forces on the spar structure.

370

371 3.2 Irregular wave tests: RAOs and damping estimations

372 A further stage of the model identification has been achieved through the analysis of the spar response to
373 the irregular waves recorded at the test site. The RAOs of the model have been estimated from the
374 experimental data and the numerical model in Ansys AQWA has been calibrated, by setting the linear
375 damping coefficients such that the numerical RAOs fit well the experimental ones. Concerning the
376 nonlinear damping, it has been represented through the drag term of Morison's equation, using a constant
377 drag coefficient $C_d = 0.6$, representative of the spar behaviour in moderate to severe irregular waves [14],
378 as those measured during the experiment (Fig. 8).

379 The determination of experimental RAOs in open sea cannot be achieved as in ocean basins, where regular
380 waves and irregular sea states, with the desired broad-banded spectra, can be generated. Consequently, an
381 alternative method for the estimation of the RAOs has been developed and applied to the data collected
382 during the experimental activity, as described below. The relationship between a wave spectrum and the
383 corresponding structure response spectrum, in a given DOF u , is expressed by:

384

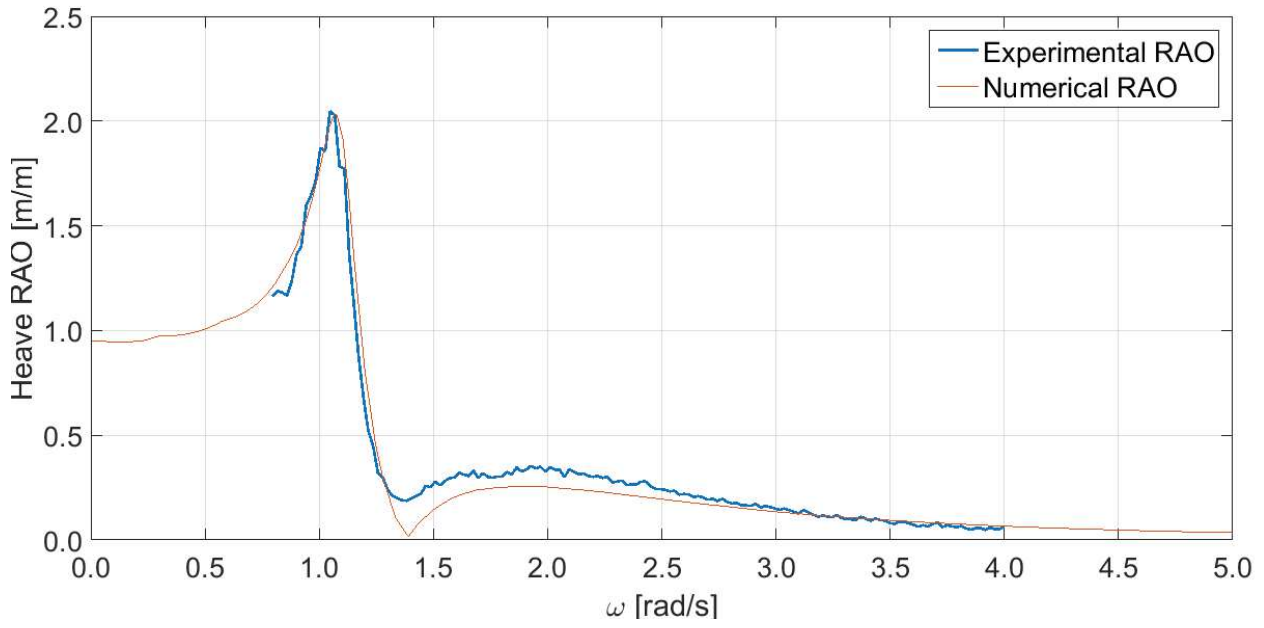
$$E_u(\omega) = RAO_u^2(\omega) E_\eta(\omega) \quad (13)$$

385 The inverse relationship of equation (13) would give a well-defined estimation of the RAO in the whole
 386 frequency domain only when using white noise spectra as input. However, in practice, each sea state has a
 387 limited energy content in the frequency domain, i.e. is narrow-banded. For this reason, only the portion of
 388 the frequency domain with enough energy content (see Appendix) has been considered for the estimation
 389 of experimental RAOs, depending on the wave spectrum of the sea state considered. Following these
 390 considerations, each sea state provides information about the RAOs on a limited range of frequencies and a
 391 global estimation of the RAOs over the whole frequency domain may be obtained only by averaging over
 392 and putting together a sufficient number of data, each contributing to its own frequency range. The
 393 expression used in this paper for the estimation of each of these contributions is:

$$394 \quad RAO_u(\omega) = \sqrt{\frac{E_u(\omega)}{E_{\eta,ph}(\omega)}} \frac{\cosh[k(d + z_{PT})]}{\cosh(kd)} \Leftrightarrow E_{\eta,ph}(\omega) \geq \alpha E_{\eta,ph}(\omega_p) \quad (14)$$

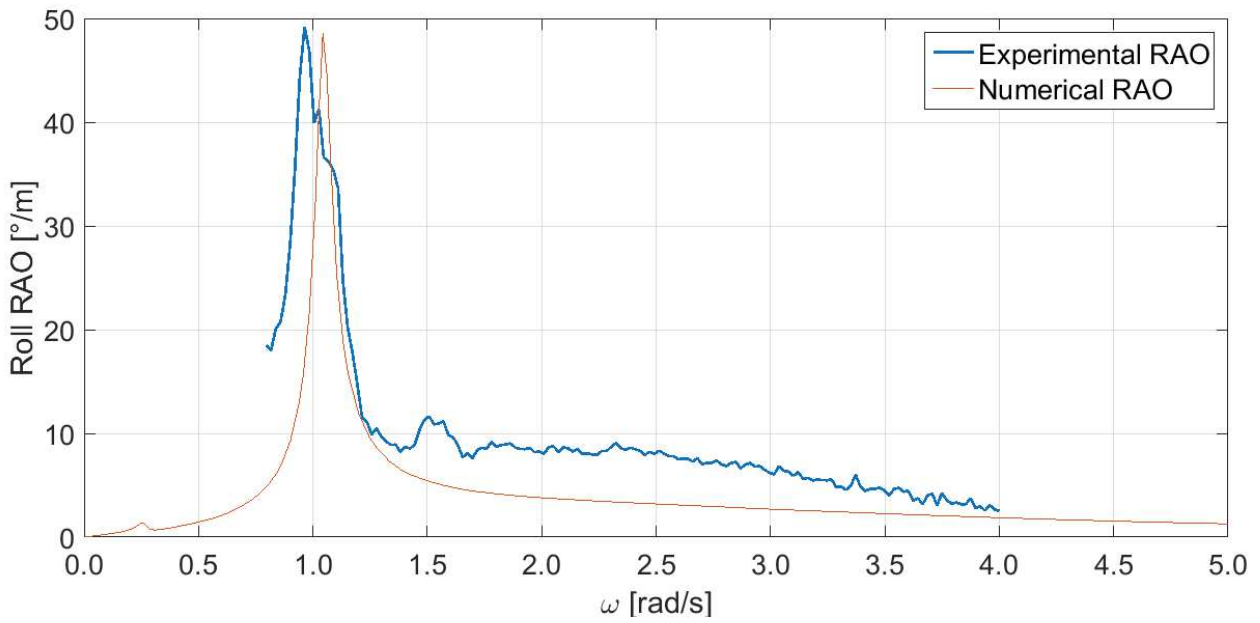
395 being E_u the motion response spectrum in the u^{th} degree of freedom, $k(\omega)$ the wave number, d the water
 396 depth, z_{PT} the distance of the pressure transducer from the still water level, ω_p the peak frequency of the
 397 wave spectrum, and α a positive parameter smaller than unity. In the present study, the value chosen for
 398 such parameter is $\alpha = 0.10$ and the portion of the frequency domain considered for the estimation of the
 399 RAOs is $\omega = (0.80 - 4.10)$ rad/s at model scale (1:30), which corresponds to about $\omega = (0.15 - 0.75)$ rad/s at
 400 full scale (1:1). Both these values have been calibrated through a parametric analysis, which is described in
 401 the Appendix. It should be noted that wave head of pressure spectra $E_{\eta,ph}$ have been used in equation (14)
 402 instead of wave surface elevation ones E_η . This is because it has been observed that pressure transducers
 403 guarantee greater reliability with respect to ultrasonic probes. The attenuation factor, which depends on
 404 the distance z_{PT} of the pressure transducer from the still water level, has been then used to refer the
 405 resulting RAO contributions to the wave surface elevation, as it is common practice in offshore engineering.

406 Due to the axial symmetry of the spar, for the heave RAO estimation it has been possible to use the whole
 407 experimental dataset collected at NOEL, i.e. the 1281 sea states and the corresponding time histories of the
 408 model structure response, while a selection based on the mean propagation direction value has been made
 409 for the estimation of roll and pitch RAOs. In particular, only the sea states with mean propagation
 410 directions between -40° and 0° with respect to the North have been included in the dataset considered,
 411 obtaining a total number of 824 sea states. The calibration of the numerical model has been made by
 412 setting the linear damping coefficients such that the peaks of the numerical RAOs approximately
 413 correspond with those of the experimental ones. The estimates obtained through the free decay tests have
 414 been used for starting the trial and error fitting procedure. The numerical RAOs used for the calibration
 415 process have been obtained in ANSYS AQWA through a linearization process, performed on a sea state with
 416 significant wave height of 6 m, mean-JONSWAP spectrum (averaged as in Ref. [27]) and wave propagation
 417 direction of -20° (1:1 scale). The resulting experimental and numerical RAOs in heave, roll and pitch at the
 418 model scale (1:30) are shown, respectively, in Fig. 12-14. The estimated peak frequencies and damping
 419 coefficients are instead reported in Table 6.



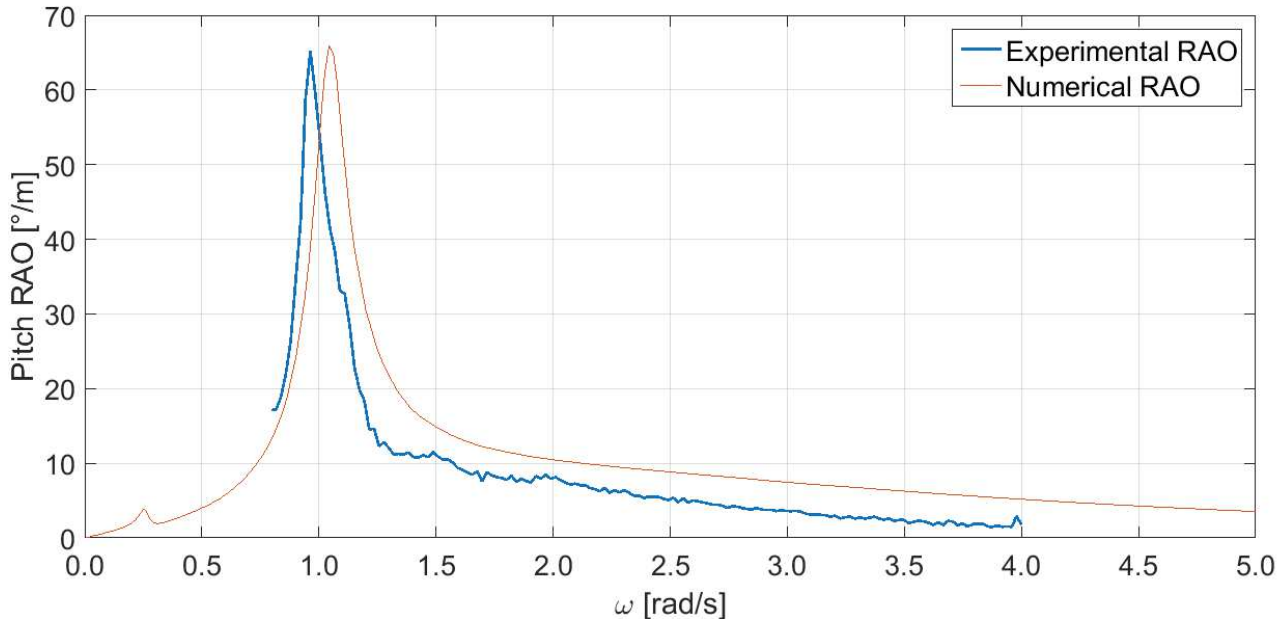
420

421 *Figure 12 - Experimental and numerical heave RAOs at model scale (1:30).*



422

423 *Figure 13 - Experimental and numerical roll RAOs at model scale (1:30).*



424

425 Figure 14 – Experimental and numerical pitch RAOs at model scale (1:30).

426 Table 6 – Experimental estimation of natural frequencies and linear damping coefficients via irregular wave
427 tests.

Degree of freedom	ω_n [rad/s]		B_n [kg s ⁻¹ ; kg m ² s ⁻¹ rad ⁻¹]	
	Model scale (1:30)	Full-scale (1:1)	Model scale (1:30)	Full-scale (1:1)
Heave	1.047	0.191	50.72	2.500·10 ⁵
Roll	0.963	0.176	89.03	3.950·10 ⁸
Pitch	0.963	0.176	183.7	8.150·10 ⁸

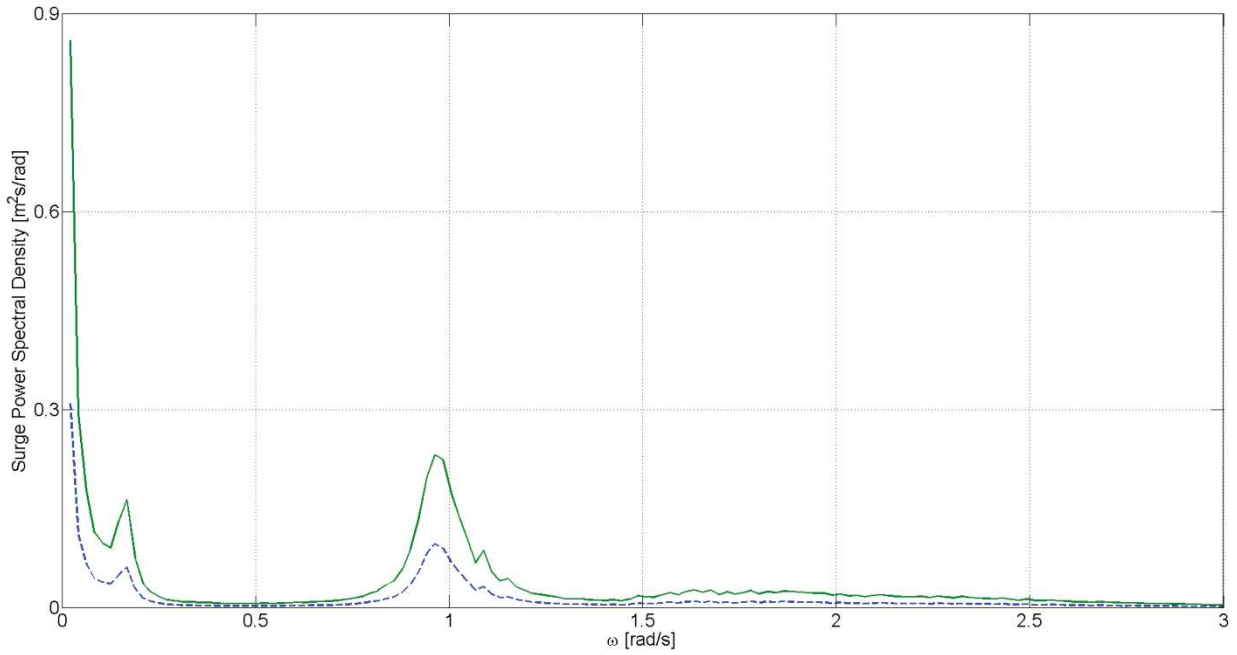
428 Natural frequency estimations obtained via free decay tests (Table 5) and irregular wave tests (Table 6) are
429 consistent to each other, while small differences are observed with respect to the numerical predictions,
430 based on the model characteristics reported in Table 2-3. In particular, the peak frequencies of numerical
431 RAOs are slightly larger than those observed experimentally and the difference is of about 2.5 % for heave
432 and of about 8.1 % for roll and pitch. This is probably due to the inevitable uncertainties introduced in the
433 computation of the mass and mass distribution of the model by multiple causes, including welding and
434 manufacturing procedures, use of non-structural elements such as sensor cables, marine growth and so on.
435 As a result, mass and mass moment of inertia reported in Table 3 are slightly underestimated, resulting in
436 the corresponding overestimation of the natural frequencies.

437 Linear damping coefficients estimated through irregular waves (Table 6) are instead smaller than those
438 estimated through free decay tests (Table 5), while they are still greater than those predicted by Jonkman
439 [14], based on free decay tests carried out in a small-scale experimental activity. This result leads to
440 interesting conclusions about the feasibility of the free decay tests for the estimation of the model damping
441 coefficients. The estimation of B_n performed by free decay tests is indeed referred to calm water
442 conditions, is based on the hypothesis of uncoupled motions and takes into account the damping
443 properties of the model only around its natural frequencies. Differently, estimation performed through
444 experimental RAOs allow to investigate directly the coupled dynamic behaviour of the model in irregular

445 waves and extends the analysis over a wider frequency range. For these reasons, the latter approach
446 provides more accurate results, which should be used for the calibration of the hydrodynamic numerical
447 models. This encourages the realization of intermediate-scale open-sea experiments on floating supports
448 for offshore wind turbines, since they can provide information concerning the structure dynamic behaviour
449 under relevant wave conditions and at relatively large scales.

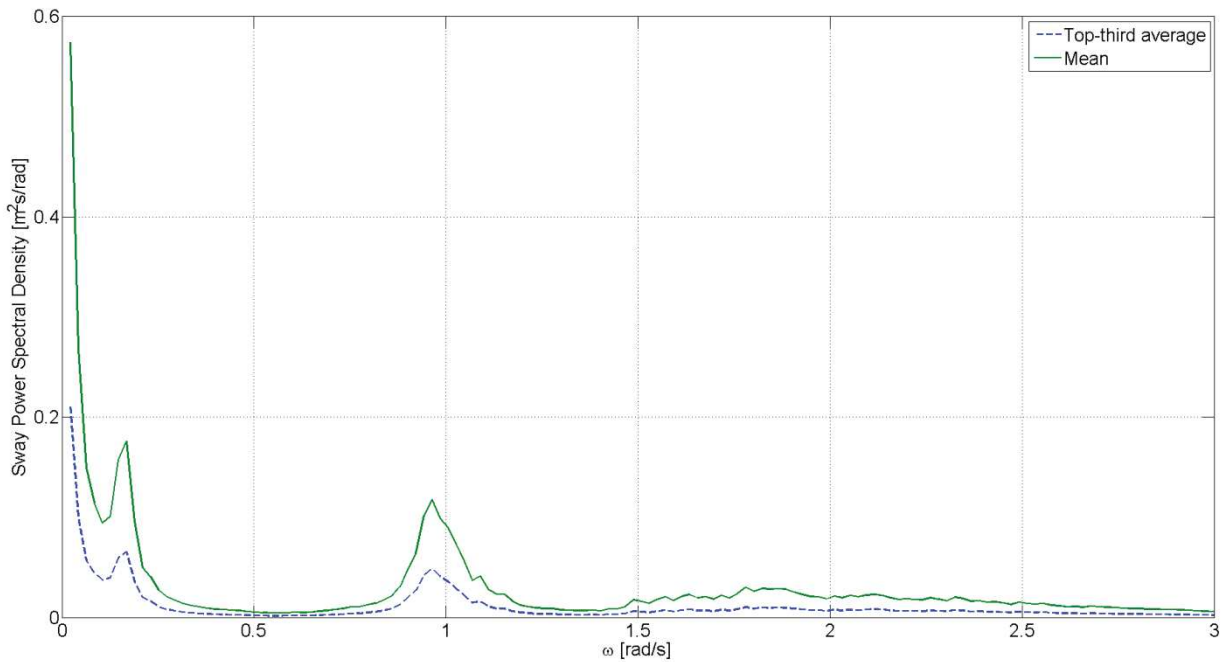
450 Another interesting consideration concerns the difference observed in roll-pitch linear damping coefficients
451 estimated through irregular waves (Table 6). Although the model presents some minor asymmetries due to
452 the mooring system and the on-board measurement station, the main cause for that is the directionality of
453 the sea states used for the estimation of the RAOs. Each sea state has indeed different mean propagation
454 directions, which are not uniformly distributed in the range $(-40^\circ; 0^\circ)$, and directional spreading functions.
455 On the opposite side, the sea state used in the numerical model has a single propagation direction (-20°)
456 and no directional spread. In both cases, the dominant rotation of the model is pitch, since the mean
457 propagation direction is close to 0° , however this effect is mitigated in the experiment, where the variability
458 of the mean propagation direction and the directional spread cause an increase of roll motions and a
459 reduction of pitch ones. Consequently, the calibration of the model results in an overestimation of pitch
460 linear damping coefficient, since the directionality of the measured sea states acts as a fictitious additional
461 damping, and in an underestimation of the roll linear damping coefficient, for the opposite reason. The two
462 values obtained for the damping coefficients may be indeed seen as upper and lower bounds, respectively,
463 for pitch/roll linear damping of the spar support. In general, more accurate estimations can be obtained by
464 recording more data, which would allow a finer discretization of the measured wave directional range for
465 the calibration of the numerical model. Differently from roll and pitch, it should be also noted that
466 directionality has no effect in the heave RAO, since heave motion is not affected by wave propagation
467 direction, due to the axial symmetry of the structure.

468 The main drawback of the approach proposed is that no prediction concerning the model structure
469 behaviour could be made outside the envelope of the frequency ranges of the sea states considered ($\omega =$
470 $0.80 - 4.10$ rad/s), due to the scarcity of sea states with sufficient energy content at the corresponding
471 frequencies. Consequently, the dynamic identification of the model in surge, sway and yaw degrees of
472 freedom could not be realized through the experimental estimation of the RAOs. Nevertheless, some useful
473 considerations could be drawn from the spectral analysis of these motions. The power spectral densities
474 have been calculated for the whole set of 1281 records and the mean and top-third averaged surge and
475 sway spectra are shown in Fig. 15-16, respectively.



476

477 Figure 15 - Mean and top-third averaged surge response spectra of the whole dataset of 1281 sea states
 478 recorded during the experiment.



479

480 Figure 16- Mean and top-third averaged sway response spectra of the whole dataset of 1281 sea states
 481 recorded during the experiment.

482 The averaged power spectral densities shown in Fig. 15.16 may be divided in various regions, corresponding
 483 to different contributions. The first region ($\omega < 0.1$ rad/s) is that of slow-drift motions, which may be
 484 induced by wind, currents and/or tidal, as well as by slow-drift wave forces. The second region corresponds
 485 to surge and sway natural frequencies, which are equal to each other: $\omega_1 = \omega_2 = 0.168$ rad/s. This result
 486 confirms that the stiffness values in surge and sway are close to each other, in spite of the geometric
 487 asymmetry of the mooring system. It should be also noted that the range of natural frequencies of the

488 individual power spectral densities span from $\omega = 0.147$ rad/s to $\omega = 0.189$ rad/s. This is due to the
489 nonlinear mooring stiffness and to the randomness of wave energy content distribution (thought small) in
490 the corresponding frequency range. The third region shows the coupling between surge/sway and
491 pitch/roll, as well as a small coupling with heave motion. The frequencies of the corresponding peaks in Fig.
492 15-16 are respectively $\omega = 0.963$ rad/s to $\omega = 1.089$ rad/s, which are consistent with the values reported in
493 Tables 5-6. Also in this case, the individual power spectral densities show a variability of the natural
494 frequencies, which is due to nonlinear effects and should be further addressed in further time-domain
495 analyses of the experimental results. Finally, the fourth region ($\omega > 1.1$ rad/s) corresponds to the wave-
496 induced motions, which are smaller than the other contributions in average but are significant for
497 individual wind-generated sea states. Although the analysis of response spectra gives some clear
498 information about the dynamic behaviour of the model in surge and sway degrees of freedom, it could not
499 be used for direct estimation of the corresponding linear damping coefficients necessary for the calibration
500 of the numerical model. To this aim, the Authors proposed an alternative identification method [43], based
501 on the adoption of output-only identification techniques, borrowed from Operational Modal Analysis. This
502 approach has already proved successful for the dynamic identification of a numerical model of a spar
503 structure, similar to the present one, and may be hence used in future studies to complement the
504 identification process of the intermediate-scale models of offshore structures installed at sea.

505

506 **Conclusions**

507 This paper presents the results of an experimental activity on a 1:30 scale model of a spar floating support
508 structure (OC3-Hywind) for offshore wind turbines, in parked rotor conditions. The experiment was
509 conducted at sea, in the Natural Ocean Engineering Laboratory of Reggio Calabria (Italy), between July 2015
510 and March 2016. The aim of the experiment was to investigate the feasibility of intermediate-scale, open-
511 sea experimental activities on floating support structures for offshore wind turbines, which may overcome
512 some of the most relevant limitations of the traditional small-scale ones, namely the high costs, the
513 introduction of significant scale effects, and the limited duration of the experimental campaigns. Traditional
514 identification techniques, commonly adopted in wave tanks and ocean basins, could not be directly applied
515 to the interpretation of the experimental data, due to the non-controlled nature of the marine
516 environment, hence they have been opportunely modified to meet the requirements of the application
517 considered. 1281 5-minutes long sea states, including wind-generated waves, mixed sea states, and swells
518 have been measured, and they have been used to perform the dynamic identification of the model
519 considered, and to calibrate its numerical model, implemented in ANSYS AQWA. The main conclusions are:

- 520 • An optimised approach for intermediate-scale, open-sea experiments of floating offshore wind
521 turbines has been proposed. In particular, the requirements of a suitable test site have been
522 identified, and some identification techniques compatible with the non-controlled marine
523 environment have been proposed.
- 524 • The free decay tests provided a good estimation of the natural frequencies of the model, but
525 proved to overestimate the linear damping coefficients.
- 526 • The irregular wave tests were used to estimate the natural frequencies, damping coefficients and
527 Response Amplitude Operators (RAOs) in the operational wave conditions of the model. The
528 experimental RAOs obtained match well with the numerical predictions, and allowed to calibrate
529 the numerical model.

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- The limited frequency range of the natural sea states recorded at the test site resulted in the impossibility of identify the dynamics of the model over the complete frequency range. This is an intrinsic limitation of the open-sea experiment, when traditional input-output identification techniques, such as those presented in this paper, are applied. Due to this limitation, experimental RAOs estimation and numerical model calibration could be performed only for heave, roll and pitch degrees of freedom of the model.
 - Some alternative approaches have been proposed to overcome the above limitations. Spectral analysis of surge and sway motions has proved to be suitable for identifying the corresponding natural frequencies and the most relevant dynamic characteristics. Finally, output-only identification techniques typical of Operational Modal Analysis [43] have been suggested as potential alternative/complementary methods for the full identification of intermediate-scale models of floating structures installed at sea.

542

543 **Acknowledgements and competing interests**

544 The activity has not been supported by any specific project.

545 The Authors have no competing interests to declare.

546

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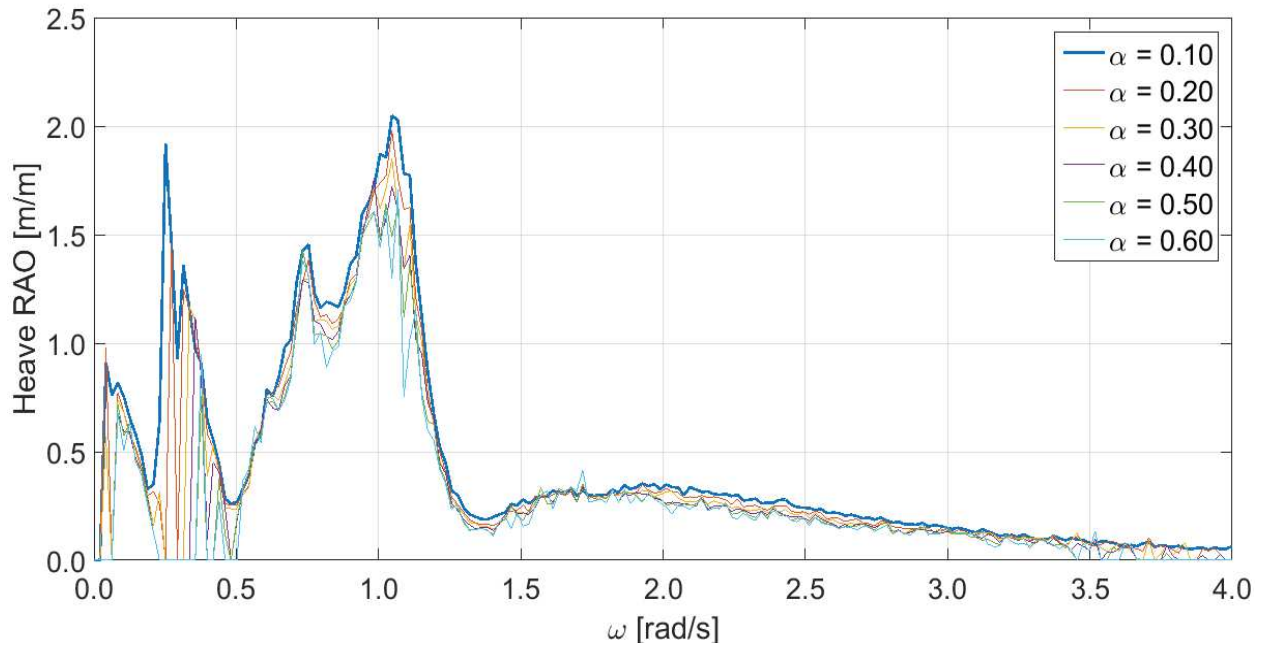
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653

654 **A. Appendix**

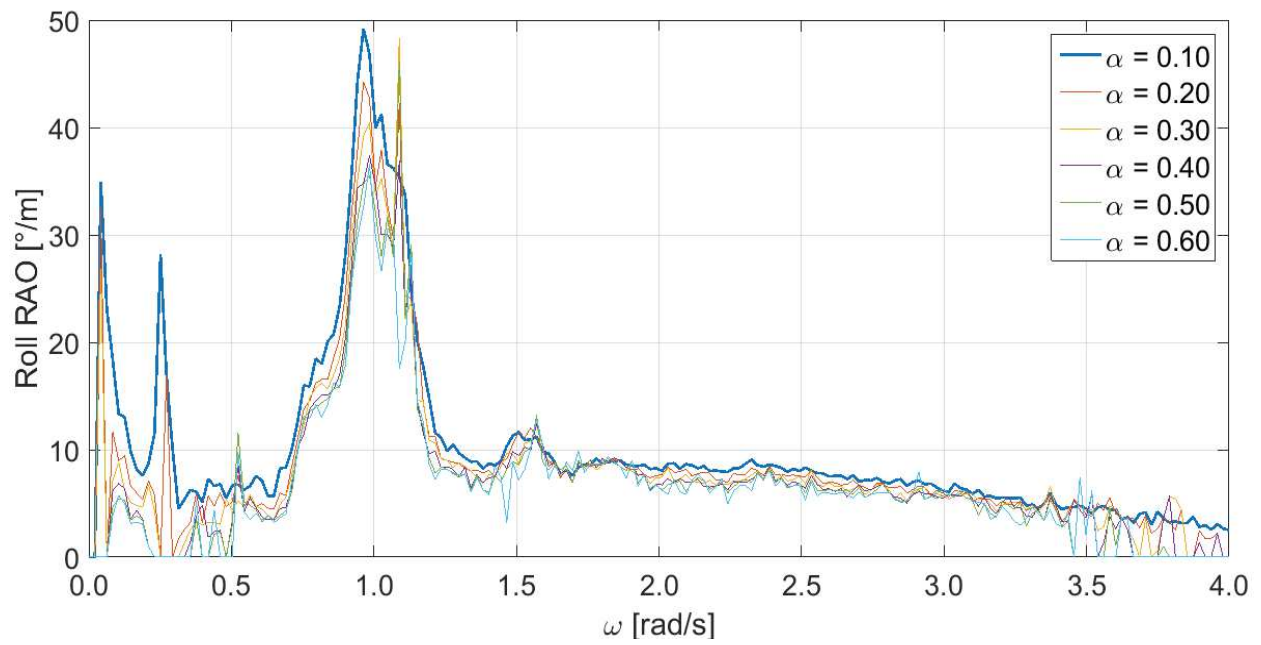
655 Parameter α of equation (14) has been calibrated through a parametric analysis on the whole dataset
656 collected during the experiment. Ideally speaking, the domain of meaningful values for this parameter
657 ranges from 0 to 1. In the limit case of $\alpha = 0$, the response spectra of each sea state would be used for the
658 determination of the RAOs in the whole frequency domain, including the portions where the wave
659 spectrum ordinates are very small and their estimation is not accurate, due to the unavoidable numerical
660 inaccuracies. Such a case would result in RAOs tending to infinite in these portions, and generally in strong
661 inaccuracies in their estimation. In the opposite limit case of $\alpha=1$, only the peak spectral ordinates would be
662 used for the determination of the RAOs, resulting in a single point in the frequency domain for each sea
663 state. While in this case the numerical inaccuracies in the spectrum determination would be negligible, the
664 resulting estimation of the RAOs would be limited to few points in the frequency domain. In this study, the
665 value of α has been set equal to 0.10 after a parametric analysis, aimed to evaluate its effect on the
666 estimation of experimental RAOs. Similarly, frequency range has been limited to $\omega = (0.8 - 4.0)$ rad/s (1:30
667 scale), due to the scarcity of data out of this range.

668 The parametric analysis has been conducted by estimating the experimental RAOs for six different values of
669 the parameter α of equation (14), ranging from 0.10 to 0.60. At this stage, the six estimates of the RAOs for
670 each of the three degrees of freedom have been plotted on the frequency range $\omega = (0.0 - 5.0)$ rad/s. The
671 results obtained are shown in Fig A1-A3.



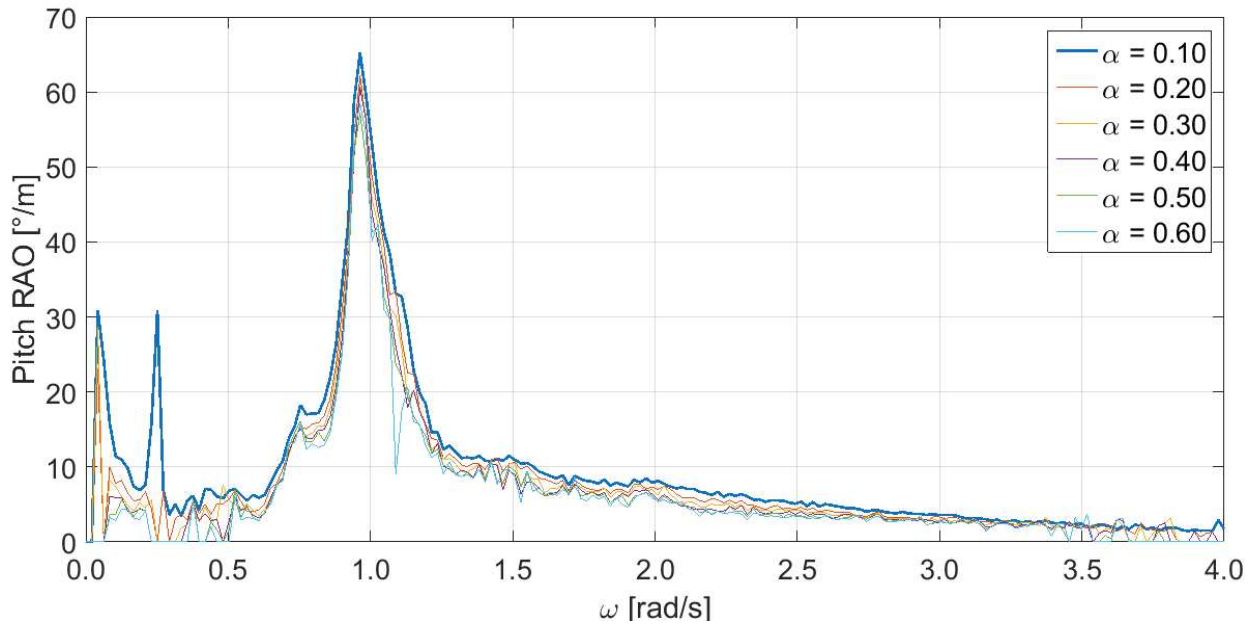
672

673 Figure A1 - Experimental heave RAOs obtained for different values of α (1:30 scale).



674

675 Figure A2 - Experimental roll RAOs obtained for different values of α (1:30 scale).



676

677 *Figure A3 – Experimental pitch RAOs obtained for different values of α (1:30 scale).*

678 Concerning the frequency range, as expected, the number of contributions (eq. 14) available for the
 679 averaging procedure is too small for the smallest and highest frequencies, resulting in inaccurate
 680 estimations in the corresponding portions of the frequency domain. Basing on the analysis of the wave data
 681 available (Fig. 8), the frequency range $\omega = (0.80 - 4.10)$ rad/s has been chosen for the estimation of the
 682 RAOs. It corresponds to a maximum wave period of about 8 s, which is consistent with the wave conditions
 683 observed at NOEL site (Fig 8). In the selected frequency range, the global shape of the RAOs is not
 684 significantly altered by the variation of α . This confirms the robustness of the calculation procedure,
 685 provided that sufficient wave data are available. However, the RAOs corresponding to $\alpha = 0.10$ are
 686 significantly smoother than the other ones. This is due to the fact that larger values of α involve a
 687 significant decrease of the number of contributions (eq. 14) to the average estimate at certain frequencies.
 688 This phenomenon is particularly evident for the roll RAO at the frequency $\omega = 1.089$ rad/s. As a
 689 consequence, for the case study considered, the value $\alpha = 0.10$ has been chosen. Higher values for α can be
 690 used by increasing the number of samples in the dataset used in the calibration.