Extreme wave loads on monopile substructures: precomputed kinematics coupled with the pressure impulse slamming load model

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Published in:
Proceedings of International Offshore Wind Technical Conference 2019

Publication date:
2019

Document Version
Peer reviewed version

Citation (APA):
EXTREME WAVE LOADS ON MONOPILE SUBSTRUCTURES: PRECOMPUTED KINEMATICS COUPLED WITH THE PRESSURE IMPULSE SLAMMING LOAD MODEL

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ABSTRACT
Monopiles are nowadays the preferred substructure type for bottom-fixed offshore wind turbines at shallow to intermediate water depths. At these locations, the large waves that contribute to extreme loads are strongly nonlinear. Therefore they are not easily reproduced via the simple engineering models who are commonly used in the offshore industry. In the current approach, we develop a design pattern which improves this standard methodology.

To retain nonlinearity in the force computations, we have precomputed a number of wave realizations by means of a potential fully-nonlinear code (OceanWave3D), for a wide span of nondimensional water depths and significant wave heights. The designer can then extract a wave kinematics time series from the precomputed set, scale it by the Froude law, and couple it with a suitable force model to compute loads. To complete the picture, slamming loads are calculated by means of the so-called pressure impulse model, recently developed at DTU. Rather than computing the time series of the slamming load, the model uses a few parameters, all except one determinable from the incident wave to calculate the pressure impulse.

First comparisons with experimental results, obtained in the framework of the DeRisk project, are promising. The force and the wave elevation statistics from the precomputed simulations show a good agreement with the experimental force and wave elevation signals. Some discrepancies are present, due to an imperfect scaling and to the differences in the physical and numerical domains. The computed loads from the slamming model match the experimental ones quite closely, once the wave celerity extracted by the precomputed kinematics is corrected by an ad-hoc factor.

INTRODUCTION
When designing offshore wind turbines installed on monopiles support structures, it is vital to correctly estimate ultimate load states (ULS) coming from extreme waves to avoid overly conservative safety factors or nonconservative designs.

At the depths at which monopiles are typically installed (20[m] ÷ 40[m]), large waves events that occur during severe sea states have typical nonlinear characteristics: they are three-dimensional, their crests are not symmetric, and they are prone to breaking. Especially when the turbine is parked and the aerodynamic damping is low, loads from these wave events can become design drivers. The kinematics of these waves is not easily described, unless a complex model is used. However, these complex models may be too computationally heavy for the designer that wishes to achieve a quick result for a large number of test cases.

Current norms for design of the offshore wind turbine substructures, such as IEC-61400-3 [1], suggest some engineering approaches to calculate ULS that allow to retain both the non-
linearity and the stochasticity of the process. In the constrained wave approach, a 50-year regular extreme wave is embedded into an irregular background linear sea realization. The extreme wave is usually a regular stream function wave \[^2\]. In this way, the designer can take into account both nonlinearity and stochasticity of the wave process. However, stream function waves are derived on the assumption of flat bed, are symmetric about their crests, and have a deterministic behavior. Moreover, the largest force or moment response on a flexible structure is not always associated with the highest wave, as it also depends on the dynamic response of the structure.

In addition to the above mentioned loads, waves breaking in the proximity of a structure also exert an impulsive load, called *slamming force*. The load is generated by a sudden exchange of momentum between the slamming front of the wave and the structural wall. The impact load can be seen as an additional, fast-paced load cycle that happens on top of the quasi-static force cycle induced by the non-breaking underlying wave on the structure. Slamming loads on cylinders can be predicted by a number of engineering models \[^3, 4\]. However, comparisons with experimental results \[^5\] show that these models are quite sensitive on the modelling assumptions.

The objective of the current work is to improve the accuracy in the computation of extreme loads in shallow to intermediate waters by introducing a new procedure. The first part of the new strategy consists in generating a precomputed set of wave realizations by means of a potential fully-nonlinear code (OceanWave3D \[^6\]), covering a wide span of nondimensional water depths (\(h\)) and significant wave heights (\(H_s\)). Rather than performing own computations, the designer extracts wave kinematics time series from the database and calculates the forcing on a structure via a suitable load model (for example the Morison \[^7\] or the Rainey \[^8\] models). Secondly, to further increase the level of physics detail, the designer calculates slamming loads from wave breaking, using the new approach developed at DTU by Ghadirian and Bredmose \[^9\]. From this model, rather than the force time series associated with a certain impact, the user computes the time integrated pressure impulse (and consequently force impulse). Preliminary results show that the model over predicts the pressure impulse from a slamming impact on a cylinder computed via CFD by only 3%. More validation was also conducted against experiments (see \[^10\]) and consistent results were obtained.

The objective of this paper is to exemplify a typical procedure for computing extreme wave loads on a cylinder via our methodology. The results will be compared to wave tank experimental results obtained in the framework of the DeRisk project \[^11\].

### METHODOLOGY

#### THE COMPUTED KINEMATICS

The fundamental idea behind the current methodology is to pre-compute a set of extreme sea state realizations via the fully-nonlinear potential code OceanWave3D \[^6\]. The runs were all generated on a bidimensional, shoaling computational domain \(L = 25 \cdot 10^3 \text{[m]} \) long. The domain, in Figure 1, was \(h_{in} = 250 \text{[m]} \) deep at the inlet (offshore) and \(h_{out} = 12.5 \text{[m]} \) at the outlet (onshore). While the first and last \(2.5 \cdot 10^3 \text{[m]} \) were characterized by a flat bottom, the central \(20 \cdot 10^3 \text{[m]} \) have a gentle 1:100 tangent hyperbolic slope. The shape of the sea bottom was calculated via the following equation:

\[
x^* = \frac{x - 2.5 \cdot 10^3 \text{[m]}}{2 \cdot 10^3 \text{[m]}} \quad (1)
\]

\[
h(x) = \begin{cases} 
250 & x^* \leq 0 \\
250 \left(1 - \frac{\tanh((x^*)^1)}{2(1-4x^*_2)}\right), & 0 < x^* \leq 1 \\
12.5 & x^* > 1 
\end{cases} 
\quad (2)
\]

To generate the 2D wave realizations, object of the current study, a number of linear sea states were imposed in a relaxation zone adjacent to the inlet boundary. As the waves travel towards the shore, they start to interact and become increasingly nonlinear. An initial warm-up time of 2 hours was allowed, to achieve a fully developed flow through the domain. Afterwards the wave kinematics is sampled at different depths and stored into binary files. For each depth, we stored the velocities and their spatial gradients, the kinematics (Eulerian acceleration, the surface elevation and its spatial gradients, in 17 points, which was the grid resolution in the z-wise direction.

In fact, the bidimensional computational domain was discretized with \(N_X = 2^{14} + 1 \) cells in the wave direction, and \(N_Z = 17 \) points in the depth direction. While the grid was regular in the x-wise direction, the vertical grid spacing was not even, due to the shoaling domain.

Further details on how the flow governing equations are solved in this unevenly spaced grid is available in the paper from Engsig-Karup et al. \[^6\].

The time step for the simulation was \(\Delta t = 0.07 \text{[s]} \), which guaranteed a Courant Number lower than 1 in the whole domain.

The incoming spectra were of the JONSWAP type \[^12\]. The peak enhancement factor \(\gamma\) was derived according to the DNV recommended practices \[^13\]. To achieve a satisfactory description of all the waves in the spectrum, it was decided to resolve the shortest waves with at least 9 grid points in the x-direction. Hence the spectrum high-cut frequency was set at \(T_{hc} = 3 \text{[s]} \). The spectrum was then discretized into \(2^{19}\) components in the frequency axis, to achieve a \(\Delta t = 0.07 \text{[s]} \), which guaranteed a return time of roughly 10 hours.

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For each sea state, 10 different realizations were performed. For each of these realizations, the amplitude of the discrete components was always the same, defined by the above mentioned JONSWAP spectrum. On the contrary, their phases were each time different, as they were randomly sampled from a uniform distribution.

A simple breaking model is present in the fully nonlinear code. As a potential code cannot directly model breaking, some energy is subtracted from the domain when a certain condition is met. In particular, a smoothing is applied at the free surface when the surface vertical downward particle acceleration overcomes a certain threshold, which in this paper was set as $0.5g$, where $g$ is the gravitational acceleration.

THE PRESSURE IMPULSE MODEL

The slamming force from waves is often modelled with the Wienke and Oumeraci model [4]. The accuracy of this model has often been assessed in terms of maximum force, i.e. the maximum value of the overall wave force plus the slamming contribution from the impact load model. For structural modes with high frequency relative to the fundamental wave frequency, the structural response is governed by the force impulse, that is, the time integrated force, rather than the peak force value. Hence, even with a reliable prediction of the peak force, the response will still depend on an accurate description of the impact time scale.

A simpler approach to the slamming loads may thus be obtained by direct modelling of the time integrated force. The recent pressure impulse model of Ghadirian and Bredmose [9] addresses this idea with a closed-form solution to a boundary value problem defined by simple wave parameters. The solution is the pressure impulse field (time integrated impact pressure) on the cylinder face, which can further be integrated over the structure to achieve the force impulse. The spatial distribution of the pressure impulse is thus part of the solution.

We describe the model briefly here. The fluid domain and its boundary conditions are shown in figure 2. The impacting wave is described in an idealized geometry. The domain is wedge-shaped with with azimuth limits $-\theta_{max} \leq \theta \leq \theta_{max}$ and consists of initially moving and still fluid above and below $z = -\mu H$ respectively. Here $H$ is the vertical distance from the bed to the free surface for the wave at the initial impact and $\mu H$ is the vertical height of the impact zone. Until before the time of impact, the upper part is approaching the cylinder with velocity $U$ in the negative $x$ ($\cos \theta$) direction. At the boundaries $\theta = \pm \theta_{max}$, $z = 0$ and $r = b$ the condition $P = 0$ has to be satisfied. The Laplace equation is solved in the cylindrical coordinate system to yield

$$ P = \sum_{m=1}^{\infty} \sum_{n=1}^{\infty} \left( A_{mn} \cos(L_m \theta / \theta_{max}) \sin(k_n z / H) \right) $$

$$ = \frac{L_{qm}(k_n / H) + \alpha_{mn} K_{qm}(k_n / H)}{\partial_r (L_{qm}(k_n / H) + \alpha_{mn} K_{qm}(k_n / H))} \bigg|_{r=a}, $$

(3)

where $q_m = L_m / \theta_{max}$ is the order of the Bessel functions, $k_n = (n - 1/2) \pi$ and $L_m = (m - 1/2) \pi$. Further $\alpha_{mn}$ is chosen such that $P = 0$ at $r = b$ and

$$ A_{mn} = \frac{2 \rho U L_{max} \cos(k_n \mu)}{\theta_{max} k_n} \int_{-\theta_{max}}^{\theta_{max}} \cos(\theta) \cos(L_m \theta / \theta_{max}) d\theta dz. $$

(4)

From this equation, the non-dimensional pressure impulse is seen to depend on the normalized outer radius, $b/H$, impact height $\mu$, cylinder radius $a/b$ and the maximum impact angle $\theta_{max}$:

$$ \frac{P}{\rho U H} \left( \frac{r}{H}, \theta, \frac{z}{H} \right) = f \left( \frac{b}{H}, \mu, \frac{a}{b}, \theta_{max} \right). $$

(5)

In figure 3 an example distribution of the pressure impulse on the cylinder is shown for $b/H = 5$, $\mu = 0.5$, $a/b = 0.5$ and
\( \theta_{\text{max}} = \pi/4 \). The effect of each parameter was investigated in [9]. It was concluded that \( \theta_{\text{max}} \) was the only parameter not determinable from the incident wave. A value of \( \pi/4 \) was found to provide a good match to the CFD impact analysis. Initial results for validation against model tests was presented by Ghadiran and Bredmose [10]. Here, the force impulse was determined for 7 slamming events from the DHI DeRisk experiments (see below) and reproduced by the pressure impulse model. A good comparison was achieved with \( \theta_{\text{max}} = \pi/5 \), although this smaller value relative to \( \pi/4 \) may be a consequence of some under-estimation by the chosen method for detection of the experimental force impulse.

**MODEL TESTS**

The model tests that will be used for comparison in the current work were performed at DHI in 2015 in the framework of the DeRisk project. The experiments aimed at reproducing storms with different return times (10 to 1000 years) for two typical northern sea locations (20\[m\] and 33\[m\] depth). Values in this section are hereby reported at full scale, which is 50:1 with respect to model scale. For a conversion of some parameters to model scale, see Table 1. The wave tank was \( W = 1250[m] \) wide and \( L = 1650[m] \) long, with a flat bottom. The basin’s wave generator was made up of 36 wave paddles, hinged at the bottom, and was capable of generating both 2D and 3D waves. A cylinder with a diameter of \( D = 7.7[m] \) was positioned 360\[m] away from the paddles. Crushed stones were positioned on the basin boundaries, on the opposite side of the wave generator, to reduce wave reflection. Steel wave absorbers in a M shape were also positioned behind the cylinder, in order to absorb part of the waves and to reflect the unabsorbed energy away from the cylinder location.

An array of \( 5 \times 5 \) wave gauges was positioned around the cylinder with a spatial resolution of 10\[m\]. Five additional wave gauges were positioned in the space between the wave gauge array and the wave paddle generator, see also Figure 4.

The cylinder was connected to two force transducers, one at the top and one at the bottom of the cylinder, and allowed for the measurement of the loads in all six degrees of freedom, sampled every 0.01\[s\]. The first bending eigen-frequency of the transducers and cylinder assembly was tuned to be far away from the wave spectrum frequencies, in order to avoid pollution of the wave force signal by the structural response.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Wave-tank</th>
<th>Full-scale</th>
</tr>
</thead>
<tbody>
<tr>
<td>( R[m] )</td>
<td>0.14</td>
<td>7</td>
</tr>
<tr>
<td>Depth [m]</td>
<td>0.4</td>
<td>20</td>
</tr>
<tr>
<td>Basin Width [m]</td>
<td>25</td>
<td>1250</td>
</tr>
<tr>
<td>Basin Length [m]</td>
<td>33</td>
<td>1650</td>
</tr>
</tbody>
</table>

**TABLE 1**: Conversion from lab scale to full scale for some parameters. The experiments were designed to have a 1:50 scale with respect to full scale.

---

\[ \]
THE CHOSEN TIME SERIES

One particular experiment was chosen for the validation of the simulation results. This sea state, relative to the location with 20[m] water depth, had a 1000 year return time. The parameters in full scale are resumed in the first row of Table 2. The measured time series is 6 hours long.

The choice of this experimental time series is dictated by the fact that it was the one which had the largest number of close-by simulations in the nondimensional \((H_s^*, h^*)\) plane. This allowed for a meaningful comparison. For other experiments the comparison was not as good. One reason for this is the fact that not enough Froude-similar simulations were found among the computed time series.

Secondly, even though the peak shape parameter of the input JONSWAP spectra was chosen according to best practices, the evolution of the wave spectrum across the computational domain led to a redistribution of the energy. On the other hand, the experiments were performed on a flat bed, and the waves were generated via paddle motion. This can lead to remarkably different spectra at the location of the comparison, which might make the exercise more difficult for certain locations in the nondimensional \((H_s^*, h^*)\) plane.

RESULTS

As mentioned above, we produced a number of wave realizations by using our fully nonlinear potential code, and then we sampled them at different depths, generating a large number of wave kinematics time series. Among these, we selected 5 simulations that were characterized by a nondimensional significant wave height and a nondimensional depth which were comparable with the selected experimental realization, described in the previous section.

The nondimensional water depth and significant wave height were computed as:

\[
h^* = \frac{h}{gT_P^2} \quad (6)
\]

\[
H_s^* = \frac{H_s}{gT_P^2} \quad (7)
\]

To find the peak period of the wave elevation signal, we computed the power spectral density for each of the simulated time series, and applied a running average with a width of 100 samples in order to smooth it down. After that, we computed the peak period as the inverse of the frequency for which the power spectral density was maximum. The significant wave height was taken as 4 times the standard deviation of the wave elevation signal.

The scaled results are reported in Table 2.

<table>
<thead>
<tr>
<th>Exp</th>
<th>(H_s[m])</th>
<th>(T_p[s])</th>
<th>(\gamma)</th>
<th>Depth [m]</th>
</tr>
</thead>
<tbody>
<tr>
<td>S_1</td>
<td>7.29</td>
<td>14.07</td>
<td>1.75</td>
<td>20</td>
</tr>
<tr>
<td>S_2</td>
<td>7.32</td>
<td>13.85</td>
<td>1.75</td>
<td>20</td>
</tr>
<tr>
<td>S_3</td>
<td>7.24</td>
<td>13.85</td>
<td>1.75</td>
<td>20</td>
</tr>
<tr>
<td>S_4</td>
<td>7.30</td>
<td>13.80</td>
<td>1.75</td>
<td>20</td>
</tr>
<tr>
<td>S_5</td>
<td>7.29</td>
<td>13.78</td>
<td>1.75</td>
<td>20</td>
</tr>
</tbody>
</table>

TABLE 2: Scaled simulations. The parameter \(\gamma\) is the peak enhancement factor that was used to generate the JONSWAP spectra. We can see that, after the scaling, the simulations slightly underestimate the experimental significant wave height and the experimental peak period.

In order to Froude-scale the simulations to match the scale at which the experiments were performed, we calculated a scale factor \(\lambda\) by taking the ratio of the experimental and simulated depth:

\[
\lambda = \left( \frac{h_{ex}}{h_{sim}} \right)^{1/2} \quad (8)
\]

Once the scaling factor is calculated, the simulated velocity, wave elevation, acceleration time series are Froude scaled (see for example table 10-1 from [13]). After the scaling, the total simulated time among all the selected computations was 40 hours.

In practice, a different scaling would be possible. One could calculate the \(\lambda\) factor by taking the square of the ratio between the peak periods, or by taking the ratio of the significant wave heights. However, we decided to use the ratio between the depth as this was more straight-forward to define and as it led to a consistent force integration, since it fixed the water depth to 20[m].

The comparison of the wave elevation statistical parameters, in Figure 5 shows a quite good agreement between the simulations and the parameters.

In Figure 5(a), we can observe the different time series plotted on top of each other. In all the following comparisons, the red color denotes experiments, and the blue color denotes each of the 5 simulations chosen for comparison. In order to obtain a total representative simulated signal, we decided to stack the single simulations one after the other, to finally obtain the green signal. We can see that while the experiment was 6 hours long, the total simulated length was roughly 23 hours.

In Figure 5(c), the spectral peak periods of the simulated and of the overall computed simulations show a very good match. It
is however possible to see that the total energy of the experimental time series is 5\% higher than in the computational one. We have in fact tried to achieve the best possible scaling between the simulations and the experiment. However, the nondimensional significant wave height and nondimensional depth of the simulation was not exactly matching the experimental one, as from Table 2. Therefore, when scaling according to Equation 8 we achieved a sub-optimal $H_S$ scaling, which probably led to the observed discrepancy.

The shape of the spectra disagree quite strongly for frequencies larger than 0.1[Hz], where the experiment exhibits clear peaks at two superharmonics. These secondary peaks are located at frequencies that are not multiple of the spectral peak frequency. In a paper by Schløer et al. [14] the experiments were reproduced by OceanWave3D in a domain that was physically equivalent to the wave tank, and they observed peaks in the wave elevation spectrum at the same locations. This indicates that these peaks are likely linked to local effect due to the domain configuration.

In Figure 5(b), the experimental wave elevation peak exceedance probability agrees very well with the simulated results down to an exceedance probability level ($P$) of $10^{-1}$. For less likely events, they start to deviate, and the experimental curve lies above the simulated overall signal. A first explanation can be found in the different energy found in the the experimental and numerical signal. Secondly, part of the deviations might be caused by the numerical breaking filter which overestimates the simulated breaking with respect to the experimental one.

A force time series was computed from the simulations by means of the Rainey [8] force model. The distributed force on the cylinder can be computed as:

$$F_d[N/m] = \frac{1}{2} \rho C_D u |u| + \rho \pi (1 + C_m) R^2 \frac{du}{dt}$$  \hspace{1cm} (9)

In the above equation, $\rho$ is the fluid density, $C_D$ is the fluid density, $u$ is the streamwise fluid velocity, $C_m$ is the added mass coefficient, $R = 3.5[m]$ is the cylinder radius, and $du/dt$ is the total (material) derivative of the streamwise velocity. The values for $C_m$ and $C_D$ for the smooth cylinder were found via the DNV recommended practices (see 6-7 and 6-9 in RP-C205 [13]).

In addition to the above, a so-called axial divergence distributed force is predicted by the Rainey model:

$$F_{ad}[N/m] = \rho \pi R^2 C_m \frac{\partial \eta}{\partial x} u^2$$  \hspace{1cm} (10)

Once the force is computed, it is possible to compare the statistics of the experimental and simulated forces, see Figure 6.

The spectra for the force also show an interesting picture: the simulations predict a consistently higher energy in the simulated force signal than in the experimental one. The spectral energy is distributed similarly to what noted for the surface elevation. This might be due to the domain configuration (sloped versus flat bottom), as also noted while analyzing the statistics of
The wave elevation. Moreover, the energy in the simulation signal is 15% higher than for the experimental one, which also explains the trend in the force peak exceedance probability. In fact, in Fig. 6(b), the computed force peak exceedance probability curve is higher than the experimental one for the main population, down to a probability level of $10^{-2}$. However, the picture is inverted for the largest events. Part of this difference might be explained by the fact that the force model does not include any slamming calculation, therefore underestimating the magnitude of the extreme events.

This last issue will be further analyzed in the following section, where we will calculate slamming forces on the cylinder by means of the above mentioned pressure impulse model.

**APPLICATION OF THE PRESSURE IMPULSE MODEL**

While the force output from the fully nonlinear kinematics and the Rainey force model is applicable for most waves, it does not include the force from slamming. We now pursue a method to add a slamming contribution on top of the kinematics-based force. In a general design case, no experimental results will be available. The method must thus be based on the incident wave kinematics alone. We here present our first attempt for such a method and its calibration against model tests.

First, experimental values of the force impulse was extracted from the chosen test series, which was 6 hours long. From the inline force time series the 30 waves with the largest peak values were selected and then visually investigated to select the waves which generated slamming loads. With this method 7 slamming events from the measurements were detected. Next, the experimental force impulse was calculated by the same method as used by Ghadirian and Bredmose [10]. In this approach, the slamming part of the inline force time series are isolated by using the Butterworth filtering extensively. The area between the filtered force time series and the non-filtered one is taken as the impulsive force from the experiments.

Next, a method to detect the numerical waves that would slam on the structure was developed. The condition for choosing these events was that the breaking filter in OceanWave3D is active closest to and upstream of the cylinder. This is a standard output of the OceanWave3D model. As mentioned, the breaking filter is active when the downward vertical particle acceleration exceeds a threshold $dw/dt < -\beta g$ with $\beta = 0.5$. The force impulse for all the detected events was next calculated as follows. The fluid thickness, $H$, was calculated as the depth plus the crest height of the wave and $a$ is the radius of the cylinder ($D = 7 \text{ [m]}$). For the outer radius $b$, it was shown in Ghadirian and Bredmose 2019 [9] that the results of the pressure impulse model are not sensitive when it is larger than an asymptotic value. This was also the case in the present study, so $b$ was chosen to be $L_p/2$ where $L_p$ is the peak period wave length from linear dispersion. Further, as a simple choice, the impact zone height was taken equal to the distance between the still water level and the maximum crest height for the breaking event

$$\mu H = \eta_{\text{max}}$$

For consistency with the experimental force impulse detection and based on the results of Ghadirian and Bredmose 2019 [10], the parameter $\eta_{\text{max}}$ was chosen to be equal to $\pi/5$.

The last model parameter, the impact velocity $U$, was chosen to be $1.5C_p$, where $C_p$ is the celerity of the peak period wave using the linear dispersion. This choice was made since, the results from OceanWave3D were only available at the location of the monopile. In addition, from other cases it was observed that the average celerity of the waves calculated from correlation of free surface elevation on different wave gauges is around the linear approximation of the peak period wave celerity. We plan to perform simulations with more wave gauges in the domain for the revised paper to be able to calculate the individual wave celerity.

This observation, however, means that the celerity of the larger, and longer, individual waves, such as the waves selected as the slamming events must be larger than the linear approximation of the peak period wave. By calibration of the numerical force impulses against the experimental force impulses, an im-
pact velocity of $1.5C_p$ was chosen. The wave gauge based investigation support that this value is reasonable.

Based on the ratio of the experimental time series (6 hours) and the numerical time series (23 hours), 26 events were chosen from the numerical results. These 26 events were selected as the events giving the largest force impulses. The experimental and numerical force impulses are compared in figure 7 in terms of the 1 hour exceedance probabilities for the specific sea state. A good match is observed, partially owing to the calibration of the impact velocity as described above.

CONCLUSIONS

In this paper, we have presented an initial comparison of forces by precomputation of kinematics and experiments. We have also demonstrated Froude scaling and its potential for extending the validity of the precomputed kinematics.

The computed surface elevation spectrum matched fairly well with the experimental one. On the other hand, there was some over prediction in the force spectrum, linked to the different spectral shapes, differences in the achieved peak period, and differences in the main bathymetry of the computational and experimental domains. These aspects are currently subject to further investigation.

A method for addition of slamming loads of a new pressure impulse model was further presented. The procedure is based on incident wave kinematics only, and can thus be applied together with the information available from the precomputed kinematics. The model parameters were extracted from the incident waves, and by calibration of the impact velocity a good match between the experimental and numerical force impulses was found.

While the above results are initial and may be modified, for example by an improved experimental slamming detection, they show the potential of the combined application of precomputed incident wave kinematics and the pressure impulse model.

Further validation is part of our present work, and will be reported as further output of the DeRisk project.

ACKNOWLEDGMENT

This work was funded by the Innovation Fund Denmark and other partners as part of DeRisk project with grant number 4106-00038B. This support is gratefully acknowledged by the authors.

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