

VALIDATION OF A PROBABILISTIC METHOD FOR THE STRUCTURAL DESIGN OF SEAWAVE SLOT-CONE GENERATORS

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The paper deals with the validation of a wave by wave approach for the calculation of the wave loadings exerted on an overtopping type Wave Energy Converter named Seawave Slot-Cone Generator (SSG). The prediction method, originally developed for regular waves, employs the Iribarren number (Battjes, 1974), the slope parameter (Svendsen, 2006) and the Linear thrust parameter (Buccino et al., 2015) as main predictors. The approach has been tested against five 2-D random wave tests, carried out in view of the design of a new pilot plant to be located along the Norwegian coast.

Keywords: Wave Energy Converters, Seawave Slot-Cone Generator, physical modeling, wave loadings.

INTRODUCTION

The wave energy has the potential to be a particularly valuable contributor to a low-carbon energy mix, since beside being very abundant (Brooke, 2003; Falnes, 2002; Contestabile et al, 2015), it has a different geographic distribution, greater predictability and less intermittency compared to wind and solar. Accordingly, even in the awareness that only a small fraction of this huge resource can be exploited, more than 1,000 Wave Energy Converters (WECs) have been patented worldwide.

Despite the large research activity, which also studied the interaction between WECs and the Marine Environment (e.g. Azzellino et al., 2013) a number of prototype power units were destroyed in storms (Falcão, 2010). This makes evident that the lack of a specific knowledge on the capability of WECs of resisting wave actions may represent a serious hurdle, of both technical and economical nature, to the development of the devices (Contestabile et al., 2017a, Vicinanza et al, 2013).

In this paper the case of the Seawave Slot-cone Generator is considered, which belongs to the class of the “Overtopping Devices” (Buccino et al., 2015a; Contestabile et al., 2017b). Patented by WavEnergy SAS (Stavanger, Norway), this device includes a number of reservoirs placed on top of each other with an inclined outer wall (Fig.1). The incoming waves running up the structure, enter it and, on their way back to sea, move a turbine connected to a power unit.

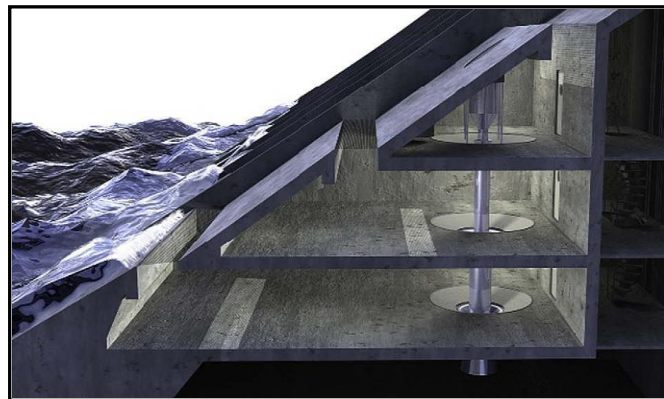


Figure 1. Aesthetic view of SSG.

The WEC is generally located on top of a steep foreshore, named “focuser” in the following, which increases the run-up height and as a consequence the potential wave energy.

A large amount of funds has been collected in view of the installation of two prototypes along the West Norwegian coasts; one on the Isle of Kvitsøy (in the Bokna fjord), the other around Svåheia. Several tests conducted on its hydraulic response (Margheritini et al. 2009, Vicinanza et al., 2012) allowed to derive a series of formulae for the prediction of the amount of water collected in each tank, and the geometry of the device has been optimized to maximize the production efficiency. The

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structural response has received a smaller attention and no tools exist for the prediction of wave loadings acting on the device. Only recently, Buccino et al. (2015b) suggested a set of design equations obtained from the results of physical model tests conducted with regular waves. As for the application to random seas, a wave by wave procedure has been suggested and a first comparison with results of CFD experiments has been discussed in a former paper (Vicinanza et al., 2015)

In the following, the approach is instead validated against five 2-D physical model tests conducted at the University of Naples in the frame of the design of a SSG pilot plant to be located at Svaheia, along the North Western Norwegian coasts.

PREVIOUS STUDIES

The first study on the structural response of SSG was conducted by Vicinanza and Frigaard (2008). They examined the behavior of a 1:60 model of the Kvitsøy pilot plant subjected to several random sea states and compared the results with the Takahashi et al. (1994) method for sloping top caisson breakwaters. The authors noticed that the predictive method conducted to an underestimate of the magnitude of forces. Further analysis (Vicinanza et al.(2011); Buccino et al.(2012)) induced to the conclusion that among the formulae of the Japanese design practice, the method proposed by Tanimoto and Kimura (1985) for trapezoidal monolithic breakwaters, could be reasonably employed also for SSGs. Recently, Buccino et al. (2015b) proposed a set of design equations based on results obtained from regular wave tests conducted in the Small Scale Channel of the Laboratory of Coastal Engineering at the University of Naples "Federico II".

The predictive method needs the knowledge of three non dimensional parameters. One is the surf similarity parameter or inshore Iribarren number (Battjes, 1974):

$$\xi = \frac{tg \alpha_{av.}}{\sqrt{\frac{2\pi \cdot H}{g \cdot T^2}}} \quad (1)$$

in which H is the incident wave height at the toe of the focuser and $tg \alpha_{av.}$ is the average slope from the toe of the foreshore to the top of the SSG. The second quantity is the (mean) “*slope parameter*” (Svendsen, 2006), which represents the ratio between the length of the waves and the mean horizontal distance between the toe of the foreshore and the shoreline:

$$S = \frac{tg \alpha_{av.}}{kd} \quad (2)$$

The third variable is referred to as *Linear Thrust Parameter* (L_{TP}) and represents the maximum excess (on a wave cycle) of the pressure thrust at the toe of the focuser due to the presence of waves.

$$L_{TP} = \frac{H \cdot \tanh kd}{kd^2} \quad (3)$$

L_{TP} is in fact a linearized-slightly-modified form of the wave Momentum Flux Parameter (M_{FP}) originally introduced by Hughes (2004, see also Calabrese et al., 2003) and tends to the wave height to depth ratio, H/d , in shallow waters. It is finally worth to emphasize that only two of the above governing quantities are independent.

Starting from the experimental outcomes the chart of Figure 2 has been obtained. Here L_{TP} is on the ordinates and the Iribarren number is on the abscissas; on this plane, all the variables relevant to the breaking process (slope angle, wave steepness, wave height to depth ratio) are expressly taken into account. The map allows to predict the breaker type and the loading features at the wall.

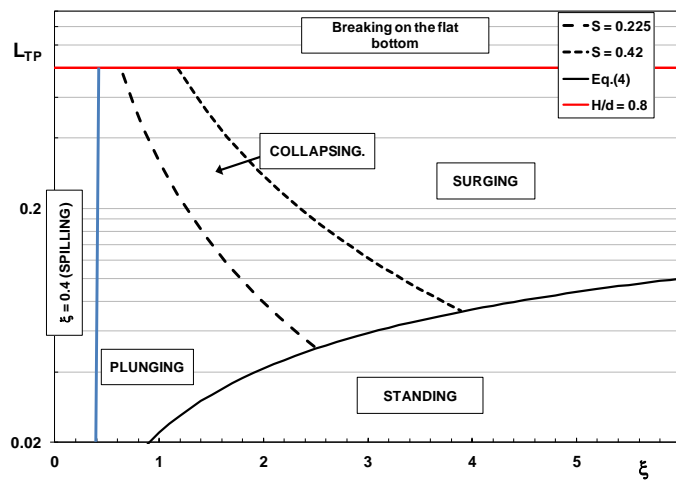


Figure 2. The plane ξ , L_{TP}

The wave profiles are distinguished into standing, surging, collapsing and plunging (Galvin, 1968; Calabrese et al., 2008). The following formula is supplied for the upper limit of the standing area:

$$L_{TP} = \frac{0.021 \cdot \xi}{1 + 0.031\xi} \quad (4)$$

which tends to 0 for small values of the Iribarren number, as on very mild slopes the waves are always expected to break. On the other hand, the limit of the Equation (4) for ξ tending to infinite is 0.68, which represents the shallow water approximation of the Daniel’s criterion (1952) for the onset of breaking at vertical face breakwaters. In the breaking zone the wave shapes are better distinguished via the parameter S . The authors suggest $S = 0.420$ and $S = 0.225$ as limits for the transition from surging to collapsing and from collapsing to plunging. As far as wave forces are concerned, the classification introduced for vertical breakwaters within the EU funded project PROVERBS has been adopted (Oumeraci et al, 1999). Accordingly, a force chronogram is termed *pulsating* if it exhibits a unique smooth peak over a wave period (Figure 3, left panel); a double peak pattern is instead named either slightly breaking or impact, depending on whether the first sharp maximum ($F_{h,max}$ in Figure 3) is lower or higher than 2.5 times the second “pulsating” peak ($F_{h,q}$).

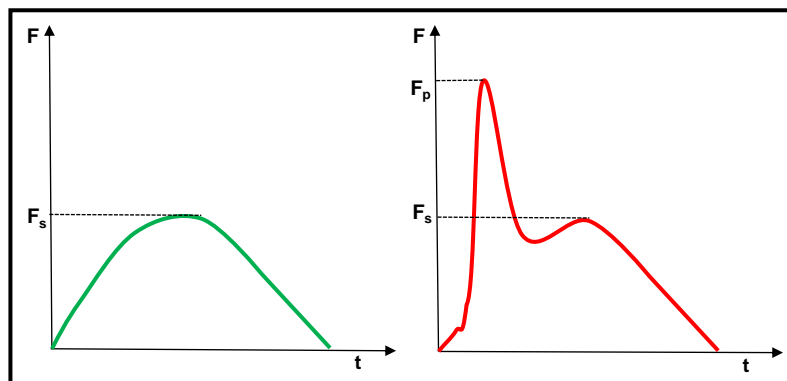


Figure 3. Examples of horizontal force time history at the front face of SSG.

For prediction purposes, Buccino et al. (2015b) reasoned that due to the inherent randomness of the breaking process, even under a regular wave attack, the structural response could be best represented by a probability density function (pdf) of wave pressure, rather than a single deterministic value. Thus, it was assumed that for any H_i and T , the average hydrodynamic pressure acting onto the front face of the WEC at the instant of maximum force(\hat{p}_{av} .in Figure 4) could be approximately described via a log-normal pdf.

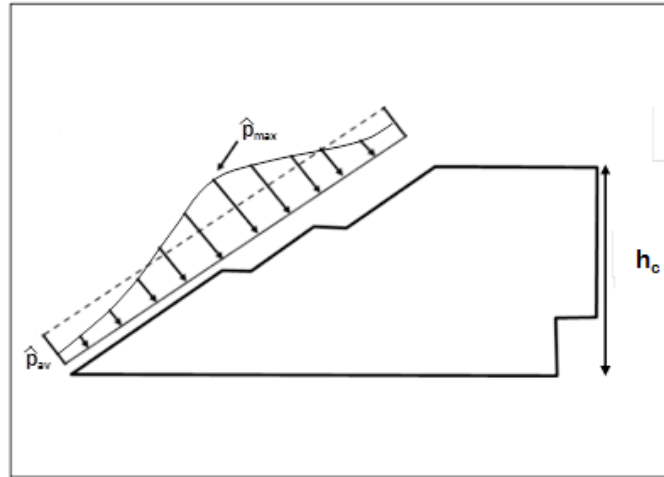


Figure 4. Definition of \hat{p}_{av} .

Mean and standard deviation of \hat{p}_{av} can be calculated according to the following equations:

$$E\left(\frac{\hat{p}_{av.}}{\rho \cdot g \cdot d}\right) = \begin{cases} 0.77 \cdot L_{TP} & \text{non - impact} \\ [2.68 \cdot \xi^{-2.42}] \cdot L_{TP} & \text{impact} \end{cases} \quad (5)$$

$$\sqrt{\text{VAR}\left(\frac{\hat{p}_{av.}}{\rho \cdot g \cdot d}\right)} = \begin{cases} 0.0012 - 0.0474 \cdot u + 0.8017 \cdot u^2 & \text{non - impact} \\ 0.0009 \cdot \exp(10.39 \cdot t) & \text{impact} \end{cases} \quad (6)$$

in which:

$$\begin{cases} u = \frac{L_{TP}}{\xi^{0.6}} \\ t = \frac{L_{TP}^{0.3}}{\xi} \end{cases} \quad (7)$$

RANDOM WAVE EXPERIMENTS

As mentioned above, the Buccino et al. (2015b) method is based on results of regular wave experiments. For application to random seas, a *wave by wave approach* has been suggested by the authors. The latter consists of calculating, via Eqs. (5)-(7), the wave force (average pressure) corresponding to a given probability level (e.g mean, 90th percentile etc.) for any individual zero crossing wave of the incoming train. Accordingly, Cumulative distribution Functions (CdF) associated with a given percentile can be derived.

The approach has been recently compared to the results of short *CFD* experiments (100 waves), exhibiting good performances. Thus, in the following a second verification with long lasting physical tests is carried out.

The random wave experiments have been conducted in the Small Scale Channel of the University of Napoli *Federico II*, in the frame of the design of a pilot plant of SSG to be located at Svaheia, along the North Western Norwegian coasts. The tests are described in detail in Vicinanza et al. (2011) and lasted approximately 2000 waves. The Table 1 reports the target values of the wave parameters.

	H_{m0} [m]	T_p [s]
Test 1	0.136	1.858
Test 2	0.144	1.895
Test 3	0.151	1.957
Test 4	0.159	1.957
Test 5	0.166	1.957

The incident wave train has been separated by the reflected one, through the Zelt and Sklerbeja (1992) method applied to a set of four probes sampled at 100 Hz. Differently from the original Buccino et al. (2015b) study, where a set of pressure transducers were employed, the horizontal force signal has been acquired via a balance at 3 degrees of freedom, sampled at 100 Hz (Vicinanza et al., 2011)

RESULTS

Loading cases

The Figure 5 plots the values of ξ and L_{TP} calculated wave by wave from the incident train. Consistently with the visual inspection, most of data falls within the surging/collapsing zone and, accordingly, the dominant loading cases are “*pulsating*” or “*slightly breaking*” (Figure 6 a)). On the other hand, a small fraction of data belongs to the plunging domain, giving rise, potentially, to impact events. The percentage of impacts estimated via the graphs of Figure AA is compared to that achieved from the direct observation of the horizontal force signal (Figure 6 b) in Table 2.

The results are in general rather difficult to comment, either because the proportion of impacts is very low (the max predicted value equals 4%) or because the balance may have cut some events with small impulse. However, the most interesting outcome is that of the *Test 5*, where an impact percentage of nearly 2% has been observed and no wave data falls within the plunging area. This suggests that impulsive events may have been generated by different mechanisms, e.g. through collapsing breakers with large momentum.

	Predicted	Observed
Test 1	4.34%	0.30%
Test 2	4.28%	1.82%
Test 3	0.34%	0.07%
Test 4	0.07%	1.46%
Test 5	0.00%	1.99%

Cumulative distribution Functions (CdFs)

The Figure 7 compares the predicted and the measured *CdFs* of the horizontal force peak. Along with the “expected” distribution, also the extreme *cdFs* are reported; the latter have been achieved calculating for each individual wave, the forces with a probability level of 0.025 and 0.975 in the Eqq. (5)-(7). As expected, the experimental distributions lie for the most part within the extreme *CdFs*. A relevant exception is found in the Tests 4 and 5, where some isolated points with a very low probability level are seen to exceed the bands; these points corresponds to the unpredicted impact events, partly discussed in the previous Section (Table 2).

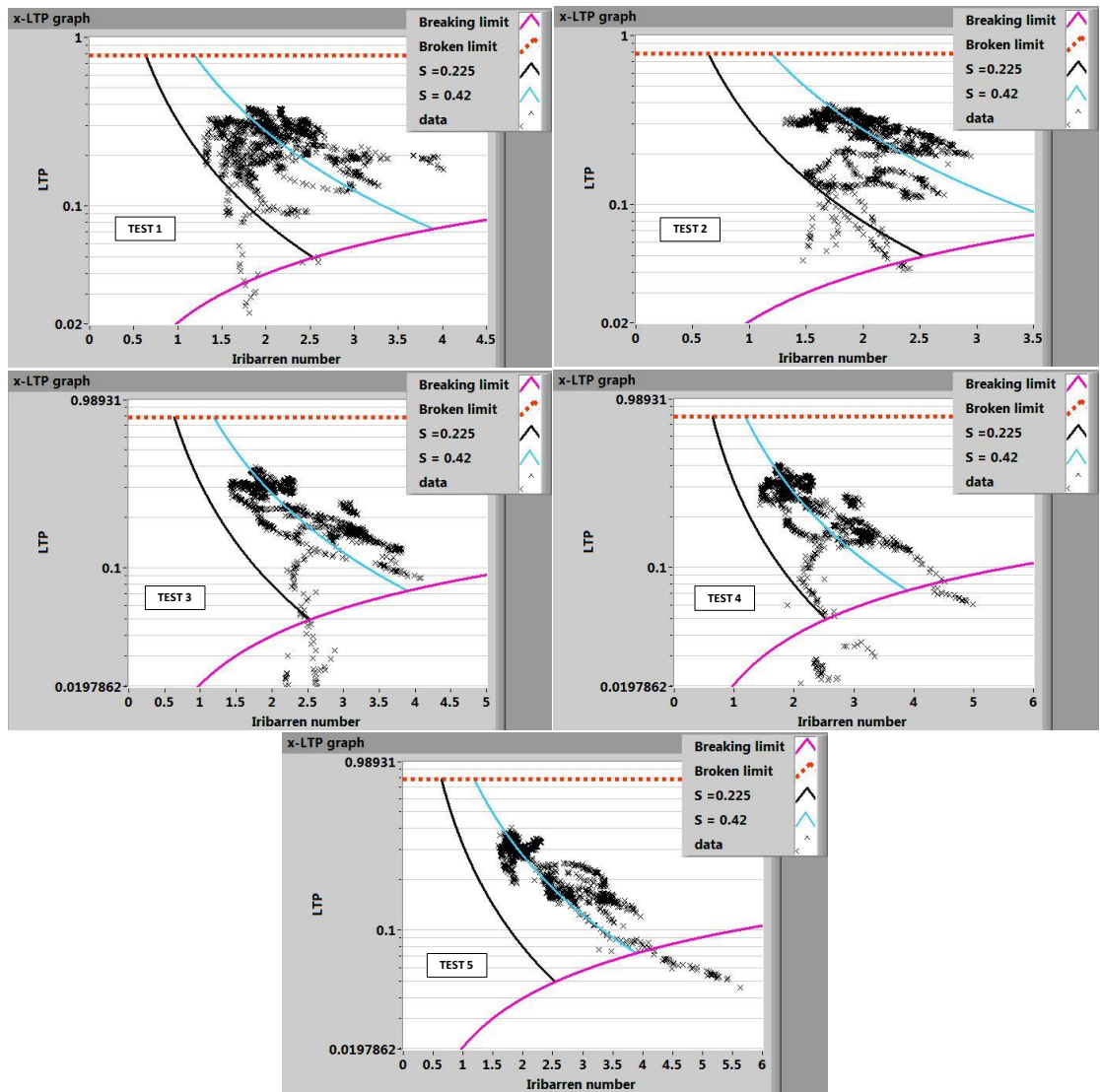


Figure 5. Individual wave data on the plane ξ , L_{TP} .

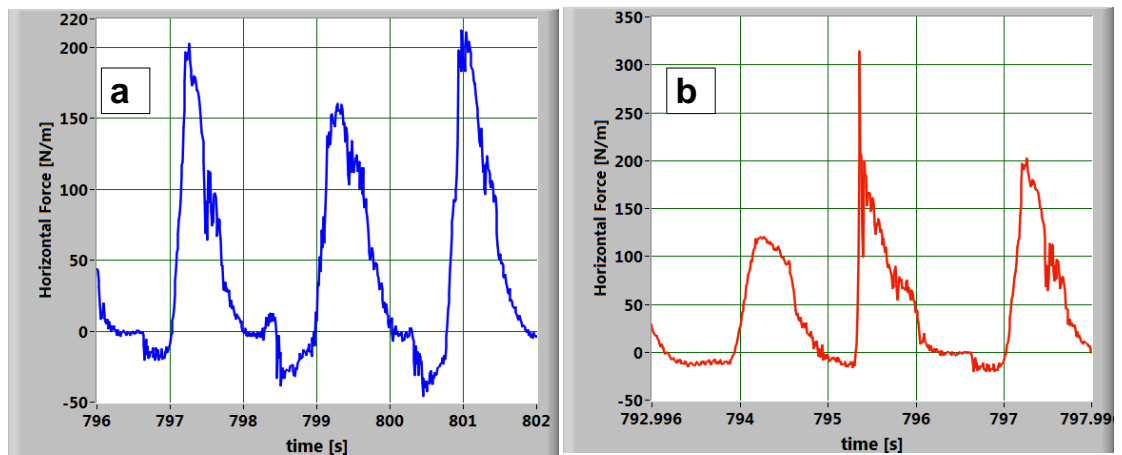


Figure 6. Observed loading cases (TEST 5). a) Pulsating/Slightly breaking; b) Impact.

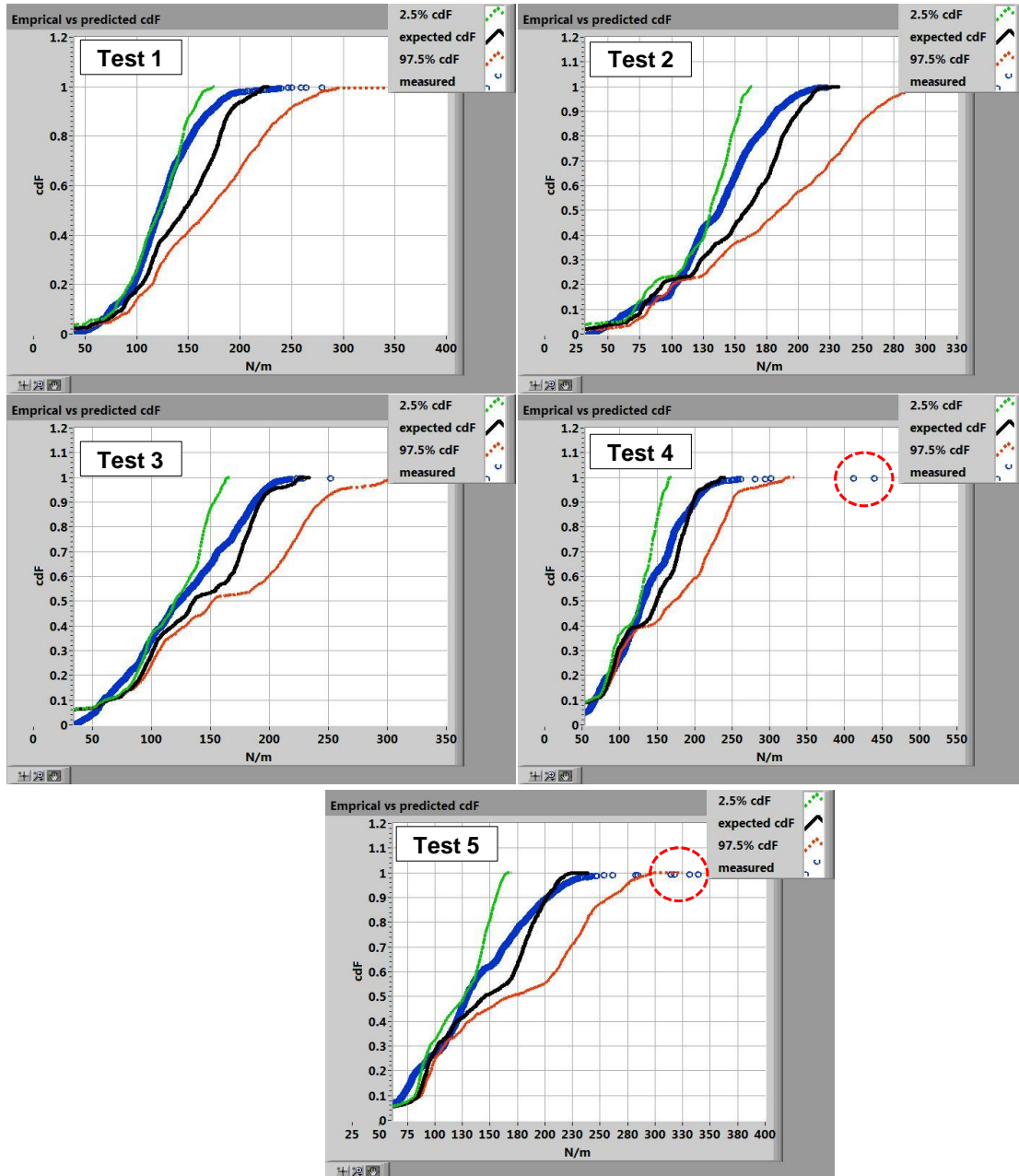


Figure7. Observed and predicted CdFs.

To give a measure of the agreement between the experimental and the “expected” *CdFs*, the maximum of the difference between the predicted and the measured Quantile functions has been calculated. The Quantile function is the inverse of *CdF* and represents the force associated to a given percentile.

To have a non dimensional index of comparison, the maximum difference between the *Qf* has been finally divided by the RMS of the measured force peaks.

As shown in Table 3, the maximum difference is less than 15% of the RMS force magnitude, which is of course acceptable for engineering purposes. Moreover, in three cases the error falls below 10%.

It is also worth to notice that the difference has been found always positive, indicating that the predicted *CdF* tends to overestimate the experimental force values.

	Predicted
Test 1	0.147
Test 2	0.113
Test 3	0.080
Test 4	0.037
Test 5	0.067

Analysis of deciles

As a further comparison, the relative error on the first 9 deciles of the distribution has been computed. The Figure 8 gives an overview for the “expected” *CdF*, whereas the Table 4 provides the minimum and the maximum error for each test (positive values indicate overpredictions).

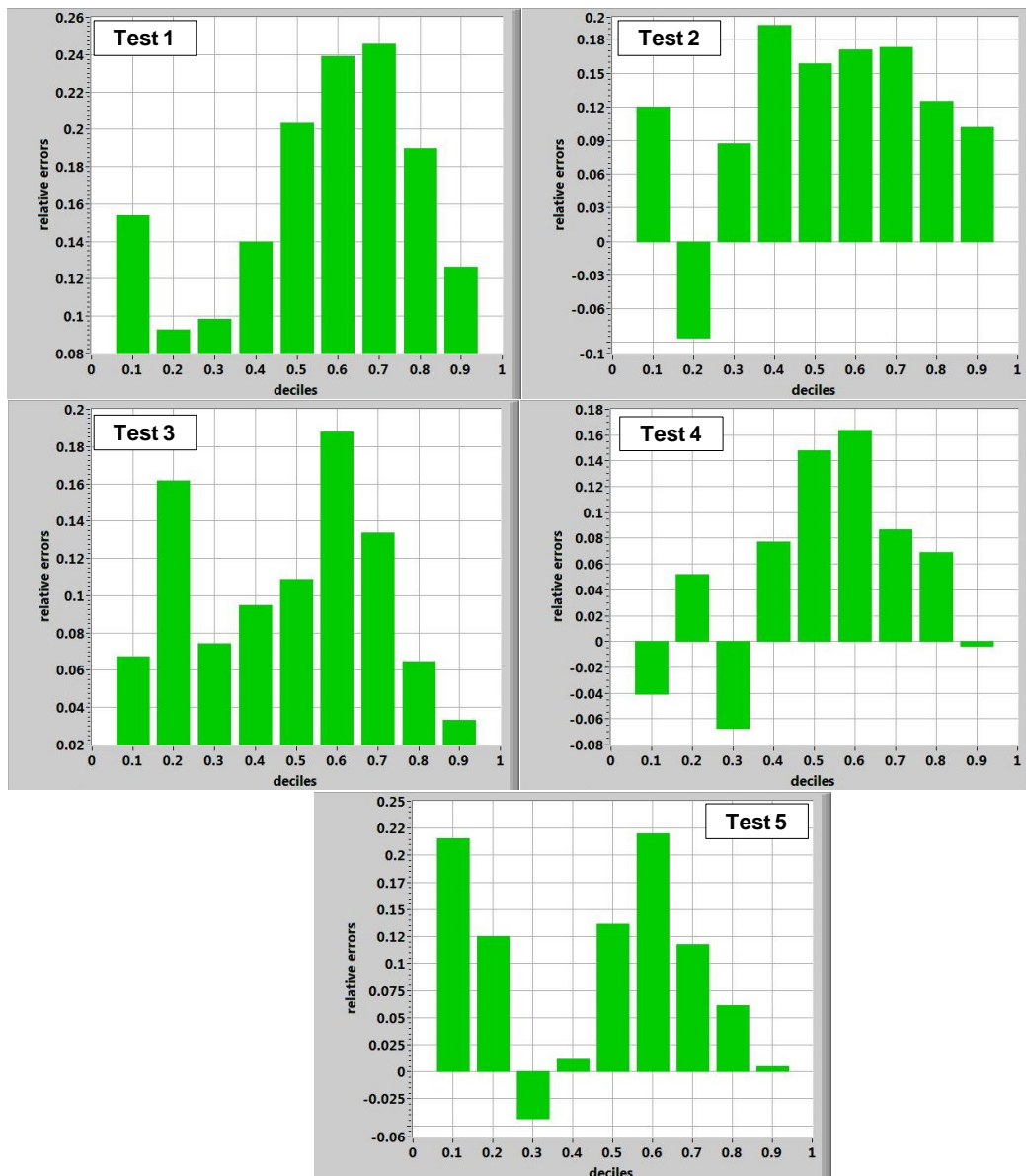


Figure 8. Relative errors on deciles for the “expected” *CdF* (positive values indicate overpredictions)

The comparison appears rather favorable; in most of cases the “expected” *CdF* is moderately conservative, with overpredictions that rarely exceed 20%. Underestimations are lower than 10% and generally concerns low deciles, which are not relevant for engineering applications.

The third and fourth column of Table 4 report the performance of the 97.5 *CdF*, which being by nature conservative, may be used as upper limit predictor. It is seen that a single small underprediction has been detected, around 2%, corresponding to the third decile of test 4. Maximum errors are uniformly of the order of 40%, which could be appropriate for design purposes.

Table 4. Min and Max relative errors on deciles (positive values indicate overprediction)

	Expected CdF		97.5 CdF	
	Min err.	Max err	Min err.	Max err
Test 1	0.093	0.246	0.204	0.488
Test 2	-0.086	0.192	0.000	0.462
Test 3	0.033	0.188	0.098	0.425
Test 4	-0.067	0.164	-0.021	0.424
Test 5	-0.043	0.219	0.018	0.488

Average of a given fraction of high forces

To complete the analysis, the average of a given fraction of high forces have been considered, since those statistics are widely used in engineering applications. In particular, the whole average (F_1), the average of one third ($F_{1/3}$), the average of one-tenth ($F_{1/10}$), one-twentieth ($F_{1/20}$), one-hundredth ($F_{1/100}$) and one-250th ($F_{1/250}$) have been selected. The results of comparison, in terms of relative errors, are summarized in Table 5 for the “expected” *CdF* and in Table 6 for the 97.5 *CdF*.

Table 5. Relative errors on the average of a given fraction of high forces. Expected CdF (positive values indicate overpredictions)

	F_1	$F_{1/3}$	$F_{1/10}$	$F_{1/20}$	$F_{1/100}$	$F_{1/250}$
Test 1	0.147	0.156	0.087	0.060	-0.075	-0.117
Test 2	0.113	0.114	0.071	0.053	0.035	0.043
Test 3	0.080	0.065	0.049	0.067	0.032	0.003
Test 4	0.034	0.024	-0.038	-0.061	-0.229	-0.367
Test 5	0.065	0.020	-0.065	-0.112	-0.284	-0.366

Table 6. Relative errors on the average of a given fraction of high forces. 97.5 CdF (positive values indicate overpredictions)

	F_1	$F_{1/3}$	$F_{1/10}$	$F_{1/20}$	$F_{1/100}$	$F_{1/250}$
Test 1	0.370	0.460	0.434	0.423	0.411	0.511
Test 2	0.350	0.439	0.415	0.409	0.411	0.433
Test 3	0.280	0.346	0.371	0.420	0.397	0.359
Test 4	0.235	0.303	0.262	0.268	0.062	-0.126
Test 5	0.267	0.296	0.228	0.177	-0.037	-0.143

The “expected” distribution produces very good estimates up to $F_{1/20}$, which has a probability exceedance less than 5%. Relevant underpredictions are instead detected for Tests 4 and 5 on $F_{1/100}$ (probability exceedance less than 1%) and $F_{1/250}$ (probability exceedance less than 0.4%). These statistics, based on few data (no more than 15 waves) are deeply influenced by the occurrence of impulsive loadings, which, as stated above, are not predicted by the method. It is not clear, though, if such low exceedance forces can actually affect the stability of the WEC; in any case, the use of the 97.5 *CdF* seems to reduce the magnitude of underpredictions to acceptable levels.

New research are however needed to understand how those impact are created and if any correction to the present method is necessary. A practical suggestion for safe design could be however to extend the

formulae for impact waves also to collapsing breakers with a high momentum flux (L_{TP} larger than 0.30). This leads the 97.5 CdF to return safe estimates for the whole data set.

CONCLUSIONS

A wave by wave approach for the calculation of wave loadings acting onto the front face of Seawave Slot-Cone generators has been verified through five 2-D small scale physical experiments, conducted at the University of Napoli Federico II. Good agreement has been found between predicted and measured CdFs, with the exception of forces with an extremely high percentile. This agrees with the results of CFD experiments presented in Vicinanza et al. (2015). These very intense loading are likely due to impulsive events generated by collapsing breakers with a high momentum flux.

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