Asociación Argentina de Mecánica Computacional

Mecánica Computacional Vol XXIX, págs. 9023-9036 (artículo completo)
Eduardo Dvorkin, Marcela Goldschmit, Mario Storti (Eds.)
Buenos Aires, Argentina, 15-18 Noviembre 2010

SAFETY FACTORS EVALUATION FOR TORPEDO ANCHORS DESIGN

Luis V.S. Sagrilo[a], José R.M. Sousa[a], Edison C.P. Lima[a], Elizabeth C. Porto[b], Jane V.V. Fernandes[b] and Diego Foppa[b]

[a] *Laboratory of Analysis and Reliability of Offshore Structures, Civil Engineering Department, COPPE/UFRJ, Cidade Universitária, Centro de Tecnologia, Bloco I-2000, Sala I-116, Ilha do Fundão 21945-970, Rio de Janeiro, Brazil.*

[b] *PETROBRAS Research and Development Center, Department of Structures, Parque Tecnológico da UFRJ, Ilha do Fundão 21945-970, Rio de Janeiro, Brazil.*

Keywords: Torpedo anchors, reliability-based design, safety factors

Abstract. The use of powerful numerical tools based on the finite element method has been improving the prediction of the ultimate bearing capacity of fixed anchors applied in the offshore oil industry. One of the main achievements of these numerical tools is the reduction of the uncertainty related to the bearing capacity prediction of these anchors. Therefore, it is possible to reduce the design safety factors values that have been calibrated based on prediction models with higher uncertainty, without impairing the original level of the structural safety. This paper presents a reliability-based safety factors calibration study for the design of torpedo anchors considering the statistical model uncertainty evaluated using the results from some experimental tests performed by PETROBRAS and their correspondent finite-element based numerical estimates. Both Working Stress Design (WSD) and Load and Resistance Factors Design (LRFD) design methodologies are investigated.

1 INTRODUCTION

Torpedo anchors, Fig. 1, are a very cost-effective and practical solution for anchoring taut mooring lines of floating units such as the one pointed out in Fig. 2. Due to its "rocket" shape, the load position and the inclination with respect to its flukes, traditional methods as proposed by the API (2005) often cannot be employed directly to estimate the ultimate bearing capacity of these anchors.

However, numerical procedures, based on the finite element (FE) method, have recently been proposed (Brandão et al., 2006; Aguiar et al., 2009) and the experimental tests performed by PETROBRAS have demonstrated their good accuracy (Porto et al., 2009). An important outcome of these FE-based models is that they have lower uncertainty than the predictions based on traditional methods as proposed by API (2005). Therefore, it is possible to reduce the design safety factors values that have been calibrated based on prediction models with higher uncertainty, without impairing the original level of the structural safety.

(a)

(b)

Fig. 1 – Typical torpedo anchor with four flukes: (a) conical tip; (b) top with detail of the padeye.

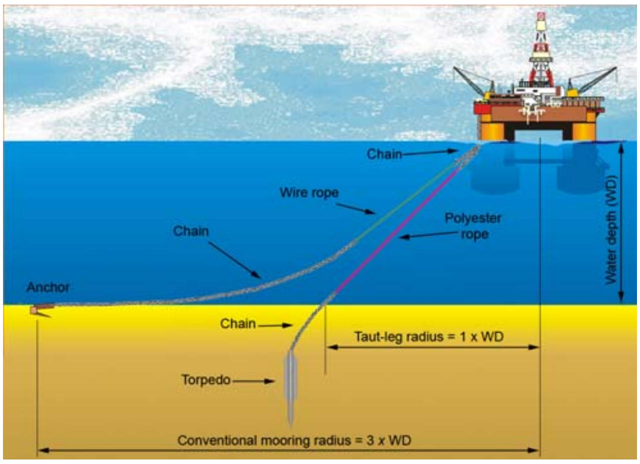

Fig. 2 – Torpedo anchor for mooring lines of floating units.

This paper presents a reliability-based safety factors calibration study for the ultimate limit state design of torpedo anchors. One important aspect of this study is the availability of loading capacity tests results for six torpedo anchors installed in Campos Basin, offshore Brazil (Porto et al., 2009), which made possible to assess the model uncertainty statistics associated with the FE based model proposed by Aguiar et al. (2009).

Both Working Stress Design (WSD) and Load and Resistance Factors Design (LRFD) methodologies are investigated. Concerning the traditional WSD methodology, it is shown that, for the same actual design safety level of the traditional offshore piles, its single safety factor can be significantly lowered. However, the use of WSD design methodology results in designs with very different safety levels. Aiming at overcoming this drawback, a LRFD methodology calibration is also proposed in this paper. The results show that the structural safety levels of LRFD-based designs are more uniform than the WSD-based ones.

2 NUMERICAL PREDICTION OF ANCHOR BEARING CAPACITY

2.1 General overview

Aguiar et al. (2009) have recently proposed a FE model devoted to predict the undrained load capacity of torpedo anchors. This model employs isoparametric solid finite elements to model both the soil and the anchor. These elements are capable of representing the physical nonlinear behavior of the soil and of the anchor itself. Large deformations may also be accounted for. Soil-anchor interaction is assured by surface to surface contact elements placed on the external surface of the anchor and the surrounding soil. A general overview of this model is presented in Fig. 3. In the following, its main aspects are briefly reviewed.

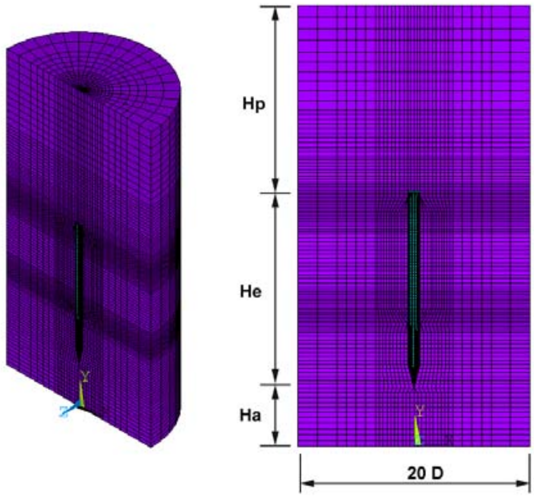

Fig. 3 – General view of the FE model.

2.2 Soil modeling: constitutive matrix

In the model proposed by Aguiar et al. (2009), the soil is assumed to be a perfectly elasto-plastic isotropic material with physical properties variable with depth. Hence, the relation between stresses and strains is given in the form:

$$[\boldsymbol{\Delta\sigma}] = \lfloor \mathbf{D_{ep}} \rfloor \cdot [\boldsymbol{\Delta\varepsilon}] \tag{1}$$

where $[\boldsymbol{\Delta\sigma}] = [\Delta\sigma_{xx} \quad \Delta\sigma_{yy} \quad \Delta\sigma_{zz} \quad \Delta\tau_{xy} \quad \Delta\tau_{xz} \quad \Delta\tau_{yz}]^T$ is the incremental total stress vector; $[\boldsymbol{\Delta\varepsilon}] = [\Delta\varepsilon_{xx} \quad \Delta\varepsilon_{yy} \quad \Delta\varepsilon_{zz} \quad \Delta\gamma_{xy} \quad \Delta\gamma_{xz} \quad \Delta\gamma_{yz}]^T$ is the incremental total strain vector; and $\lfloor \mathbf{D_{ep}} \rfloor$ is the constitutive elasto-plastic matrix (Potts and Zdravkovic, 1999):

$$\left[\mathbf{D_{ep}}\right]=\left[\mathbf{D}\right]-\frac{\left[\mathbf{D}\right]\cdot\left\{\frac{\partial P(\left[\boldsymbol{\sigma}\right],\left[\mathbf{m}\right])}{\partial\boldsymbol{\sigma}}\right\}\cdot\left\{\frac{\partial F(\left[\boldsymbol{\sigma}\right],\left[\mathbf{k}\right])}{\partial\boldsymbol{\sigma}}\right\}^{T}\cdot\left[\mathbf{D}\right]}{\left\{\frac{\partial F(\left[\boldsymbol{\sigma}\right],\left[\mathbf{k}\right])}{\partial\boldsymbol{\sigma}}\right\}^{T}\cdot\left[\mathbf{D}\right]\cdot\left\{\frac{\partial P(\left[\boldsymbol{\sigma}\right],\left[\mathbf{m}\right])}{\partial\boldsymbol{\sigma}}\right\}} \tag{2}$$

where $\left[\mathbf{D}\right]$ is the elastic total stress constitutive matrix; $P(\left[\boldsymbol{\sigma}\right],\left[\mathbf{m}\right])$ is the plastic potential function; $F(\left[\boldsymbol{\sigma}\right],\left[\mathbf{k}\right])$ is the yield function; $\left[\boldsymbol{\sigma}\right]$ is the stress state in the element; $\left[\mathbf{m}\right]$ and $\left[\mathbf{k}\right]$ are state parameters related to the plastic potential and yield functions, respectively.

The elastic total stress constitutive matrix can be split in two parts, as follows:

$$\left[\mathbf{D}\right]=\left[\mathbf{D_{eff}}\right]+\left[\mathbf{D_{pore}}\right] \tag{3}$$

where $\left[\mathbf{D_{eff}}\right]$ is the effective stress constitutive matrix and $\left[\mathbf{D_{pore}}\right]$ is the pore fluid stiffness, which, considering an isotropic material loaded under undrained conditions, are given by (Potts and Zdravkovic, 1999):

$$\left[\mathbf{D_{eff}}\right]=\begin{bmatrix} K_s+\frac{4}{3}\cdot G & K_s-\frac{2}{3}\cdot G & K_s-\frac{2}{3}\cdot G & 0 & 0 & 0 \\ & K_s+\frac{4}{3}\cdot G & K_s-\frac{2}{3}\cdot G & 0 & 0 & 0 \\ & & K_s+\frac{4}{3}\cdot G & 0 & 0 & 0 \\ & \text{symmetric} & & G & 0 & 0 \\ & & & & G & 0 \\ & & & & & G \end{bmatrix} \tag{4}$$

$$\left[\mathbf{D_{pore}}\right]=K_{pore}\cdot\begin{bmatrix} 1 & 1 & 1 & 0 & 0 & 0 \\ 1 & 1 & 1 & 0 & 0 & 0 \\ 1 & 1 & 1 & 0 & 0 & 0 \\ 0 & 0 & 0 & 0 & 0 & 0 \\ 0 & 0 & 0 & 0 & 0 & 0 \\ 0 & 0 & 0 & 0 & 0 & 0 \end{bmatrix} \tag{5}$$

where K_s is the bulk modulus of the soil, G is the effective transverse modulus of the soil and K_{pore} is the bulk modulus of the fluid, which are given by:

$$\begin{aligned} K_s &= \frac{E}{3\cdot(1-2\cdot\upsilon)} \\ G &= \frac{E}{2\cdot(1+\upsilon)} \\ K_{pore} &= 1000\cdot K_s \end{aligned} \tag{6}$$

in which E and υ are the soil effective elastic modulus and Poisson coefficient, respectively.

In order to represent the nonlinear material behavior of the soil, the Drucker-Prager model was chosen. This model approximates the irregular hexagon of the Mohr-Coulomb failure surface by a circle and, consequently, the Drucker-Prager yield function is a cylindrical cone.

The yield function and plastic potential function for this model are given respectively by (Chen and Baladi, 1985):

$$F([\boldsymbol{\sigma}],[\mathbf{k}]) = \sqrt{J_2} + k_{DP}^{(1)} \cdot I_1 + k_{DP}^{(2)} \tag{7}$$

and

$$P([\boldsymbol{\sigma}],[\mathbf{m}]) = \sqrt{J_2} + m_{DP}^{(1)} \cdot I_1 + m_{DP}^{(2)} \tag{8}$$

where I_1 and J_2 are, respectively, the first and the second invariant of the stress tensor; $k_{DP}^{(1)}$, $k_{DP}^{(2)}$, $m_{DP}^{(1)}$ and $m_{DP}^{(2)}$ are stress state parameters. These parameters depend on the adopted approximation of the Mohr-Coulomb hexagon and are functions of the internal friction angle, ϕ, the dilation angle, ψ, and the cohesion, c, of the soil. Fig. 4 presents three possible approximations and the values of the stress state parameters for each case is presented in Aguiar et al. (2009).

As pointed out before, the soil is modeled with hexahedral and prismatic isoparametric solid elements. These elements have eight nodes and each node has three degrees of freedom: translations in directions X, Y and Z. An overview of the main dimensions of the soil mesh is shown in Fig. 3. The proposed mesh is a cylinder with a base diameter of 20D, where D is the diameter of the torpedo anchor including its flukes. The height of the cylinder is given by the sum of the penetration of the torpedo anchor, H_p, the length of the anchor, H_e, and the distance of the tip of the torpedo anchor to the bottom of the FE mesh, H_a. Each "slice" of the cylinder has its own physical properties and, consequently, variable strength, density, longitudinal and transverse module and Poisson coefficients may be assumed.

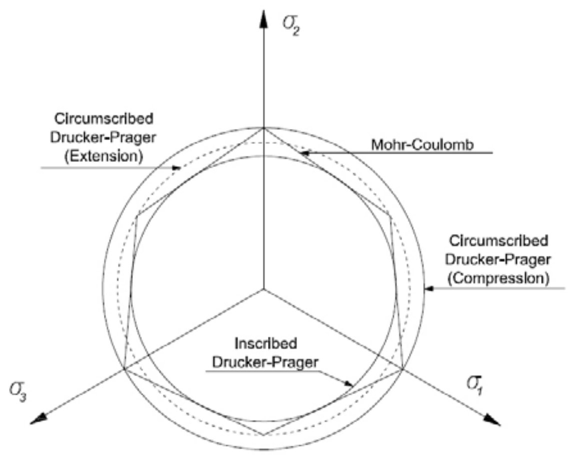

Fig. 4 – Drucker-Prager and Mohr-Coulomb yield surfaces in the deviatoric plane.

2.3 Anchor modeling

The torpedo anchor is modeled with eight nodes isoparametric solid elements analogous to the ones used in soil representation. These elements, as pointed out before, are capable of considering both material and geometrical nonlinearities. A typical FE mesh of a torpedo anchor is shown in Fig. 5.

It is worth mentioning that neither the padeye at the top of the anchor nor the mooring line is represented in the proposed model. The load from the mooring line is applied at the gravity center of the padeye, where a node is placed and rigidly connected to the top of the anchor by rigid bars, as presented in Fig. 6.

(a) (b)

Fig. 5 – General view of a FE mesh for a torpedo anchor: (a) top; and (b) bottom part of the flukes.

2.4 Anchor-soil interaction

The model proposed by dAguiar et al. (2009) employs surface to surface contact elements that allow relative large displacement and separation between the surfaces in contact. These elements are placed on the external surface of the torpedo anchor and on the surrounding soil. A contact detection algorithm based on the pinball technique and contact forces evaluated with the augmented Lagrangian method are employed in the formulation of these elements.

Relative sliding between the anchor and the soil is allowed when the shear stresses along the outer wall of the anchor exceed the values estimated with the Mohr-Coulomb friction model, which is given by (API, 2005):

$$\tau_{max}(z) = \alpha(z) \cdot S_u(z) + K_0(z) \cdot p_0(z) \cdot \tan(\delta) \tag{9}$$

where τ_{max} is the maximum allowable shear stress; α is the adhesion factor; p_o is the effective overburden pressure; S_u is the undrained soil strength and δ is the friction angle between the soil and the anchor wall and may be stated as (API, 2005):

$$\delta = \phi - 5^\circ \tag{10}$$

The adhesion factor may be calculated by the formulas proposed by API (2005):

$$\alpha(z) = \begin{cases} 0.5 \cdot \psi(z)^{-0.5}, \; \psi(z) \le 1.0 \\ 0.5 \cdot \psi(z)^{-0.25}, \; \psi(z) > 1.0 \end{cases} \tag{11}$$

where:

$$\psi(z)=\frac{S_u(z)}{p_o(z)} \tag{12}$$

2.5 Solution procedure and implementation

The FE analysis is divided in three different steps. In the first one, the initial stress state, i.e., the stresses in the soil prior to the imposition of any kind of structural load to the anchor, is generated. The model proposed by Aguiar et al. (2009) does not simulate the anchor penetration in the soil and it is assumed, by hypothesis, that the anchor is loaded with the soil stress state undisturbed by the installation process. The anchor, thus, is supposed to be “wished in place”. This undisturbed stress state is thus imposed to the soil in the first analysis step.

In the second step, the self weight of the anchor is imposed. The analysis is restarted with the stress state from the first step and gravity acts on the anchor. Finally, in the third step, the total load is applied with adaptable increments since, as the surrounding soil progressively fails, the stability of the solution is affected and lower load increments are required. Typically, this last load step starts with an initial load increment of 5% of the total load applied and, as the analysis progresses; it can be reduced to 1% of the total load.

At each load step, convergence is achieved if the Euclidian norm (L2) of the residual forces and moments is less than 0.1% of the absolute value of the total forces and moments applied.

The model was implemented in a program called ESTACAS. This software generates FE meshes to be analyzed with ANSYS® program, where the following finite elements are employed: SOLID185 in order to model the soil and the torpedo anchor; CONTA174 and TARGE170 are used to simulate the contact between the soil and the anchor.

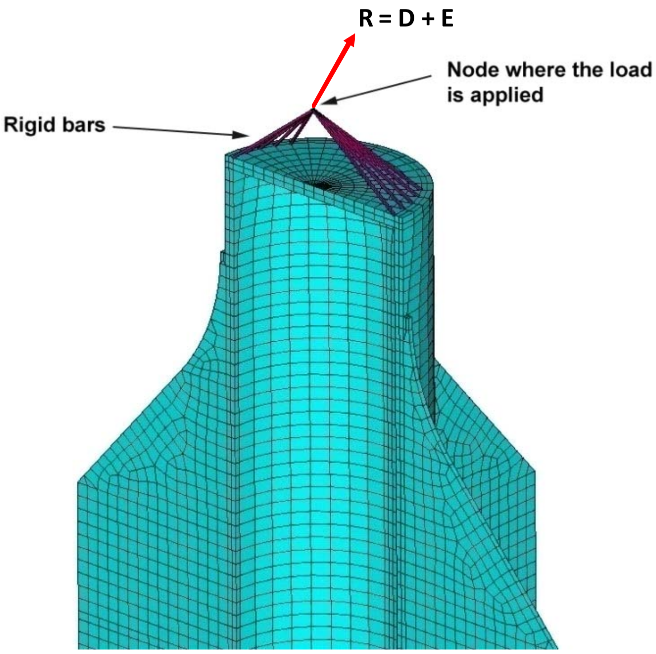

Fig. 6 – Load application.

2.6 Comparison with experimental measurements

Aiming at validating the torpedo anchor geotechnical and structural design methodology, PETROBRAS conducted six full scale tests in Campos Basin, offshore Brazil (Porto et al. 2009). These six full scale tests were divided into two sets.

In the first set of tests, three T35 torpedoes, each one weighing 35t, were installed at a location with a total water depth of 500m. At this location, soil was characterized as being pure clay after CPT tests had been performed. The torpedoes embedment depths varied from 8m up to 11m. Besides, the torpedoes were pulled out with load inclinations of about 40° (Porto et al., 2009). The second set of tests consisted of pulling out three T43 torpedoes (weight of 43t). The water depth at this second location is 150m and the soil comprises both clay and sand layers. The torpedoes final embedment depths, in this case, varied from 6m to 8m and the load was imposed with inclinations varying from 50° to 67°.

Table 1 presents the relation between the experimental measured pull out loads and the ones estimated with the model proposed by Aguiar et al. (2009).

Despite all uncertainties related to the model and the geotechnical parameters involved, Table 1 indicates that the pull out loads estimated with the FE model agree quite well to the measured loads. Hence, these results will serve as a basis to the reliability based design that is presented in what follows.

3 RELIABILITY-BASED DESIGN

Modern probabilistic design codes aim at designing new structures with a specified target probability of failure. This can be achieved by full probabilistic or deterministic codes (Madsen et al., 1986). The latter are the most common ones for the engineering design. In these codes the target probability of failure is not implicitly stated but it is achieved by means of safety factors. These safety factors are calibrated by using standard structural reliability methods (Madsen et al. 1986; Melchers, 1999), where all random variables (and their uncertainties) are represented by means of probability distributions.

The two main deterministic design methodologies for ultimate limit sate (ULS) are the Working Stress Design (WSD) and Load and Resistance Factor Design (LRFD). Focusing specifically on the case of torpedo anchors for mooring system, see Fig. 6, under the WSD methodology the design equation can be written as

$$\frac{R_k}{SF} \geq D_k + E_k \tag{13}$$

where R_k is the characteristic (or nominal) resistance of the anchor, D_k and E_k are the functional and environmental load-effects applied to the anchor, respectively, and SF is a safety factor. The functional load effect is associated with the initial mooring line pre-tension applied to the anchor and the environmental load effect is the total tension acting at the anchor, when environmental loads are applied, minus the functional one. According to this, one can see that the functional load effect has smaller uncertainties than the environmental one.

In the case of the LRFD methodology the design equation can be written as:

$$R_k \geq \gamma_D D_k + \gamma_E E_k \tag{14}$$

where γ_D and γ_E are partial safety factors applied to functional and environmental load-effects, respectively.

The safety factors of Eqs. (13) and (14) are obtained with a calibration process. The main idea of the calibration process is to obtain safety factors that guarantee a certain target failure probability pf_T for the design. The target probability of failure may be stated as:

$$pf_T = \Phi(-\beta_T) \tag{15}$$

where $\Phi(.)$ is the standard normal cumulative distribution and β_T is the so-called reliability index associated to pf_T.

The calibration process is undertaken by means of reliability analyses considering the various cases with different E_k / D_k ratios and also different statistical descriptions of loadings and anchor resistances that are expected to be found in practice. Then, the safety factors are defined, for instance, for LRFD methodology through an optimization process to solve the following problem:

$$\min g(\gamma_D, \gamma_E) = \frac{1}{N}\sum_{i=1}^{N}\left(\beta_T - \beta_i(\gamma_D, \gamma_E)\right)^2 \tag{16}$$

where N is the total number of cases considered in the calibration process and $\beta_i(\gamma_D, \gamma_E)$ is the reliability index of i^{th} case designed using the current set of safety factors (γ_D, γ_E). Similar procedure applies to the WSD methodology.

The reliability index β_i of a single torpedo anchor can be obtained by a reliability analysis method, e.g., FORM, SORM or Monte Carlo Simulation (Madsen et al., 1986; Melchers, 1999), which is applied to solve the following multi-dimensional integral

$$pf = \Phi(-\beta) = \int_D f_{\mathbf{X}}(\mathbf{x})d\mathbf{x} \tag{17}$$

where D is the failure domain $G(\mathbf{X}) \leq 0.0$, $G(\mathbf{X})$ is the limit state function:

$$G(\mathbf{X}) = R - D - E \tag{18}$$

and $\mathbf{X}$ is the vector containing the random variables associated with the torpedo anchor bearing capacity (R), functional load (D) and environmental load (E) and $f_{\mathbf{X}}(\mathbf{x})$ is the joint probability distribution of the random variables $\mathbf{X}$ for the case under consideration. The variable R can be represented by:

$$R = CR_M \tag{19}$$

where R_M is the predicted resistance evaluated by a given methodology of analysis and C is the modeling uncertainty that represent the statistical bias between the measured and predicted pile bearing capacity. Therefore, some experimental data is necessary to statistically define this random variable. In fact, C takes into account all uncertainties regarding to the soil parameters, model representation, etc.

In this work all reliability analyses have been performed with Monte Carlo Simulation approach.

4 SAFETY FACTOR CALIBRATION

4.1 Random Variables Modeling

PETROBRAS (Porto et al., 2009) has recently tested the bearing capacity of six torpedo anchors installed in clayly and clayly-sandy soils sites in Campos Basin offshore Brazil. The measured torpedo anchor load capacities have been divided by their corresponding predicted numerical results $R_{M,}$ according the model presented before, and are presented in Table 1. The mean and the standard deviation for the Table 1 data are equal to $\mu = 0.993$ and $\sigma = 0.118$, respectively. In order to properly take into account the limited number of experiments (n = 6) in the reliability analysis, it is necessary to apply some statistical modeling technique for reduced sample data as explained in Ditlevsen and Madsen (1996). Assuming that the C variable has a previous lognormal distribution, as indicated in Fenton (1997) this variable should be than modeled by the following Student's t-distribution to take into account the number *n* of experiments:

$$\mathrm{ft}(x) = \frac{1}{\xi\, x}\sqrt{\frac{d}{d+2}}\,\frac{\Gamma\left(\frac{d+1}{2}\right)}{\Gamma\left(\frac{d}{2}\right)\sqrt{d\,\pi}}\left(1+\frac{t(x)^2}{d}\right)^{-\frac{1}{2}(d+1)} \quad ; \qquad d = n-1 \tag{20}$$

where

$$t(x) = \frac{\ln(x)-\lambda}{\xi}\sqrt{\frac{d}{d+2}} \qquad ; \qquad n = 6 \tag{21}$$

$$\xi = \sqrt{\ln[1+\left(\sigma/\mu\right)^2]} = 0.1183 \quad ; \qquad \lambda = \ln(\mu) - 0.5\cdot\xi^2 = -0.0137 \tag{22}$$

Then, considering Table 1 data, the mean and standard deviation of this Student's t-distribution becomes:

$$\mu_C = 1.003, \quad \sigma_C = 0.193 \tag{23}$$

It is important to notice that due to the limited data sample σ_C is 64% higher than the sample standard deviation $\sigma = 0.118$. These figures also show that the FE model for the torpedo bearing capacity has a lower uncertainty when compared, for instance, with prediction methods reported in an API supported study (Fenton, 1997) for axial bearing capacity of single piles in clays. In this latter case, the mean and standard deviation for the modeling uncertainty variable C have been, respectively, 1.04 and 0.34.

Usually, the functional loading is modeled in the reliability analysis of marine structures by a normal distribution having a coefficient of variation (CoV or δ) in the order of 0.05 – 0.10. In this work a CoV (δ_D) of 0.07 has been assumed. The environmental load effect is usually represented by its annual extreme value distribution which, in most of the cases, corresponds to a Gumbel distribution. Considering the inherent random variability of the environmental parameters related to waves, wind and current and also modeling uncertainties in the estimation of these complex load effect (Vazquez-Hernandez et al., 2006), it is reasonable to assume a CoV (δ_E) in the order of 0.10 – 0.30 for this distribution.

	Experimental/Numerical Ratio	
Torpedo #	**T35**	**T43**
1	1.04	1.11
2	0.84	1.08
3	0.85	1.04

Table 1 – Relation between measured and numerical pull out loads.

4.2 Characteristic Design Parameters

The characteristic design parameters used in Eqs. (13) and (14) must be clearly defined in any calibration process or design code. Different values for these parameters are also related to different sets of safety factors.

In the present study, the characteristic value of the torpedo anchor bearing capacity, R_k, is defined as that predicted by the FE-based procedure described in the beginning of the paper. The characteristic value of the functional load effect, D_k, has been assumed as its estimated mean value, i.e., $D_k = \mu_D$. In the case of the environmental load effect, following the traditional design practice in offshore engineering, its characteristic value has been defined as the most probable value of the 100-yr extreme distribution. This value corresponds to that related to 99% fractile of the annual extreme cumulative distribution (Ang and Tang, 1984).

In the calibration process a broader range of location water depths for the floating units has been assumed by considering the ratio E_k / D_k to be between 1.0 and 5.50. The lower ratio is associated to deeper waters where the functional load at the anchor can be of the same order as the environmental one. The converse applies to the higher ratio. These ratios account for actual floating systems installed in water depths ranging approximately from 200m to 2500m (Vazquez-Hernandez, 2004).

4.3 Target Safety Index

In order to obtain the target safety index for the calibration process, firstly all cases investigated have been designed according to the API guidelines (API, 2005) considering an intact mooring system, i.e., using the WSD methodology with a safety factor (SF) equal to 2. Secondly, the reliability index of each case has been evaluated considering the uncertainty modeling mean and standard deviation parameters as 1.04 and 0.34, respectively. Finally, the target reliability index has been taken as the average of all these reliability indexes. Considering, specifically cases for $\delta_E = 0.10, 0.125, \cdots, 0.30$, the target reliability index has been found to be $\beta_T \approx 2.9$. Fig. 7 illustrates the results that have been obtained. In summary, the target reliability index assumed in this work is more or less the same that is implicitly assumed when a single pile in clay is designed to support an axial loading.

4.4 WSD Calibration

The optimized safety factor SF for the WSD methodology for the ultimate capacity design of torpedo anchors (see Eq. (13)) considering the previously discussed finite element model uncertainty has been found to be 1.45. Fig. 8 shows a comparison between the target reliability index and the reliability indexes of all cases investigated considering the optimized safety factor. When compared to the usual safety factor of 2.0 used by the offshore oil industry, the modeling uncertainty reduction obtained with the FE model for the prediction the ultimate bearing capacity of torpedo anchors causes a significant reduction in the safety factor.

Comparing Figs. 7 and 8, it is important to notice that a larger model uncertainty also results in more scattered reliability indexes among the design cases.

4.5 LRFD Calibration

As it is well known in literature (NCHRP, 2004), the use of a single safety factor in the WSD methodology leads to more scattered designs concerning their safety levels. The LRFD methodology, which attributes different safety factors for different loading sources (see Eq. (14)), is an alternative to reduce this scatter.

In order to obtain less scattered reliability indices for cases considered in the calibration process, two sets of safety factors (γ_D, γ_E) have been identified. The LRFD methodology design check for ultimate bearing capacity of torpedo anchors shall be read as:

$$R^k \geq \max(T_1^k, T_2^k) \tag{21}$$

where

$$\begin{cases} T_1^k = 1.35D^k + 1.43E^k \\ T_2^k = 1.72D^k + 1.27E^k \end{cases} \tag{22}$$

A comparison between the target reliability index and the reliability indexes of all cases investigated considering the LRFD design, Eq. (14), is shown in Fig. 9.

For both WSD and LRFD design methodologies the average of safety index values has been the target reliability index $\beta_T = 2.9$, however, their coefficients of variation have been, respectively, 4.5% and 2.5%. This shows that LRFD methodology gives designs with safety levels that are slightly less scattered around the target value established for the calibration process.

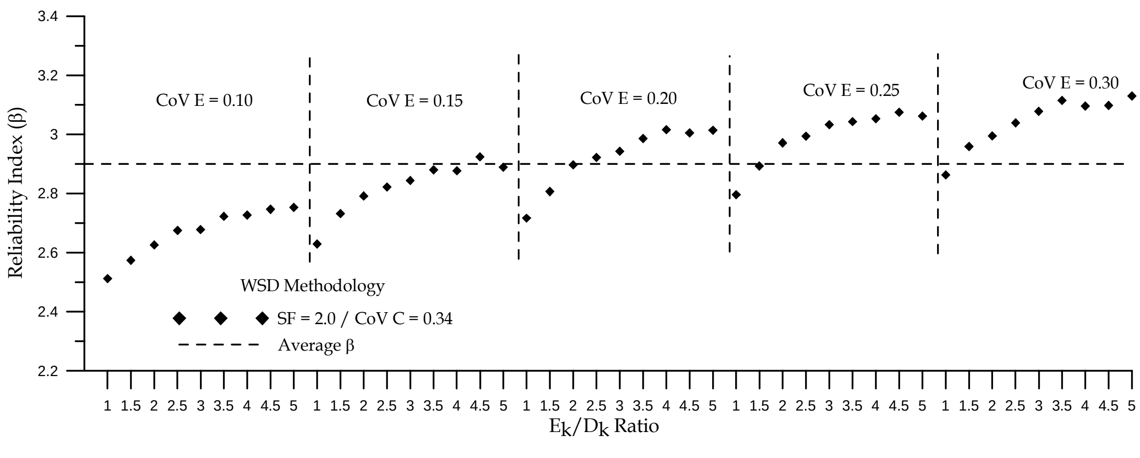

Fig. 7 – Reliability indexes of designs considering WSD methodology and $\delta_C = 0.34$.

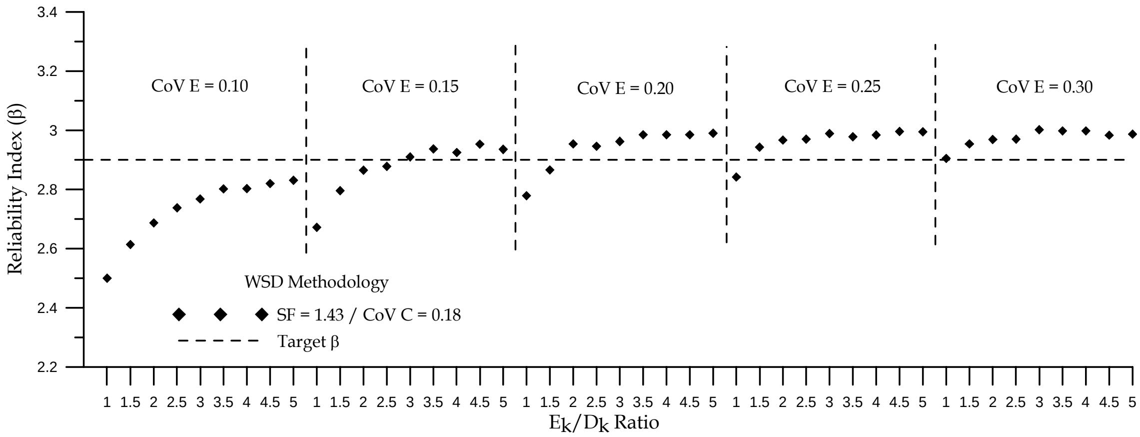

Fig. 8 – Reliability indexes of calibrated WSD designs considering δ_C=0.18 (experimental tests).

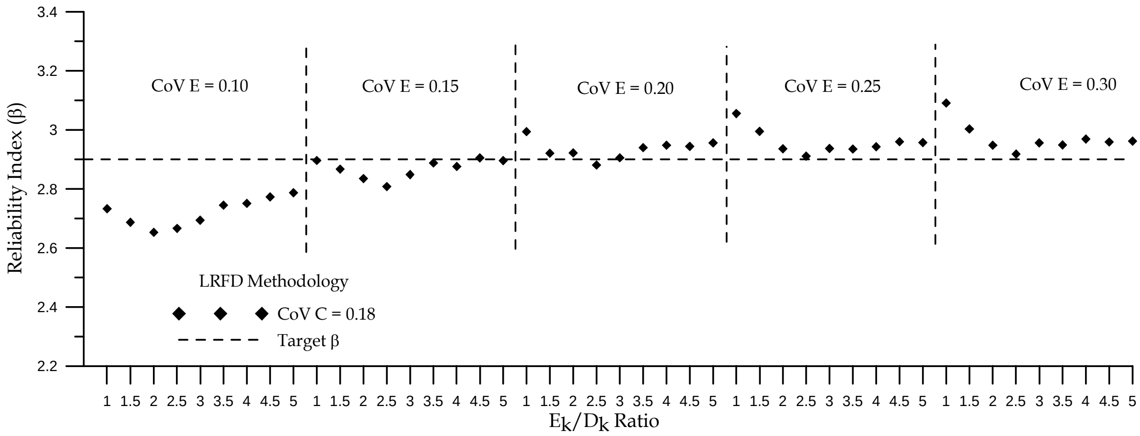

Fig. 9 – Reliability indexes of calibrated LRFD designs considering δ_C=0.18 (experimental tests).

5 FINAL REMARKS

This paper has presented a reliability-based design study for torpedo anchors considering that their ultimate bearing capacity is predicted numerically by a finite element model. The estimates predicted by this model when compared to experimental results presented a relatively low level of statistical uncertainty. Then, it has been shown that the same level of structural safety implied in the traditional design of offshore piles can be achieved with the use of lower safety factors. Considering the WSD design methodology the safety factor drops from 2.0 to 1.45. It is very likely that this factor can become even lower when more experimental results are available since a great part of the modeling uncertainty considered in the present work comes from the small number of experiments available.

Besides, a LRFD design methodology has also been calibrated for the ultimate limit state design of torpedo anchors considering the design methodology applied by PETROBRAS. The LRFD design methodology is able to produce designs having less scattered safety levels.

REFERENCES

Aguiar C.S., Sousa J.R.M., Ellwanger G.B., Porto E.C., Medeiros Júnior, C.J. and Foppa D. Undrained Load Capacity of Torpedo Anchors in Cohesive Soils, *Proceedings of the ASME 28th International Conference on Ocean, Offshore and Arctic Engineering*, OMAE Paper 79465, Honolulu, USA, 2009.

Ang A.H.S. and Tang W.H. *Probability Concepts in Engineering Planning and Design*, , John Willey and Sons, Vol. 2, 1984.

API - American Petroleum Institute. *Recommended Practice for Planning, Designing and Constructing Fixed Offshore Platforms – Working Stress Design (RP 2A-WSD)*, 20th ed., USA, 2005.

Brandão F.E.N., Henriques C.C.D., Araújo J.B., Ferreira O.C.G. and Amaral C.S. Albacora Leste Field Development – FPSO P-50 Mooring System Concept, *Proceedings of the Offshore Technology Conference*, OTC Paper 18243, Houston, USA, 2006.

Chen W.F. and Baladi G.Y. *Soil Plasticity: Theory and Implementation*, 1st ed., Elsevier Science Publishers B. V., 1985.

Ditlevsen O. and Madsen H.O. *Structural Reliability Methods*, John Wiley & Sons, 1996.

Fenton G.A. *Probabilistic Methods in Geotechnical Engineering*. Short Course Notes, 1997. Available in www.engmath.dal.ca/risk/publications.html.

Madsen H., Krenk S. and Lind N.C. *Methods of Structural Safety*, Englewood-Cliffs, 1986.

Melchers R.E. *Structural Reliability Analysis and Prediction*. John Willey and Sons, 2nd ed., 1999.

NCHRP - National Cooperative Highway Research Program, *Load and Resistance Factor Design (LRFD) for Deep Foundations*, Report 507, Edited by Paikowsky, S.G., 2004.

Porto E.C., Amaral C.S., Foppa D., Fernandes J.V.V. and Corrêa D.C. *Pull-out Tests and Holding Capacity Evaluation of T35 Torpedo Piles*, Technical Report EG 004/2009, CENPES/Petrobras, 2009.

Potts D.M. and Zdravkovic L. *Finite Element Analysis in Geotechnical Engineering – Theory*, Thomas Telford Publishing, 1st ed., 1999.

Vazquez-Hernandez, A. O. *Reliability-Based Safety Factors Calibration for the Design of Mooring Lines*, D.Sc. Thesis, Department of Civil Engineering, COPPE/UFRJ, Brazil, 2004. (In Portuguese).

Vazquez-Hernandez A.O., Ellwanger G.B. and Sagrilo L.V.S. Reliability-based Comparative Study for Mooring Lines Design Criteria. *Applied Ocean Research*, 28:398–406, 2006.

ACKNOWLEDGMENTS

The authors would like to thank PETROBRAS Research Center for allowing them to publish the present paper.