Numerical study on pull-out capacity of torpedo anchors in clay

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This paper reports the results from dynamic finite-element analyses undertaken to provide insight into the behaviour of torpedo anchors during pullout in non-homogeneous clay. The results were validated against field data prior to undertaking a parametric study, exploring the relevant range of parameters in terms of installation effect, impact velocity, anchor geometry, padeye position, pullout angle. Installation method (e.g. quasi-static and dynamic installation) has shown significant influence on the vertical pullout capacity, but that influence reduced to marginal for lateral pullout. The inclined pullout capacity was dictated by the anchor weight and fully mobilised anchor-soil contact surface area and consequently the capacity increased with increasing the number of fins. Due to the lower weight and total surface area, the 45° (at the padeye) pullout capacity of the finless anchor was 29% of that of the considered four-fin anchor. The results showed that this percentage can be increased to 83% by shifting the padeye location of the finless anchor to 0.56 times the anchor shaft length from the top. Anchor capacity under inclined pullout loading was presented as failure envelopes expressed in terms of dimensionless vertical and horizontal components, which lied between the envelopes developed for piles and embedded foundations.

KEYWORDS: anchors & anchorages; clays; numerical modelling

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NOTATION

A_p	anchor shaft and fins projected area
A_s	total surface area of the anchor
D_A	anchor shaft diameter
D_p	anchor projected area equivalent diameter (including fins)
$d_{e,t}$	anchor tip embedment depth
d_t	anchor tip penetration depth
F_{Hult}	maximum horizontal pull-out capacity
F_i	inclined pull-out capacity
$F_{i,H}$	horizontal component of inclined pull-out capacity
$F_{i,h}$	dimensionless horizontal pull-out capacity
$F_{i,max}$	maximum inclined pull-out capacity
F_{Vult}	maximum vertical pull-out capacity
$F_{i,v}$	vertical component of inclined pull-out capacity
$F_{i,v}$	dimensionless vertical pull-out capacity
h_{min}	minimum element size
L	suction caisson length
L_A	anchor shaft length
L_F	fin length
L_S	anchor shaft length
L_T	anchor shaft tip length
S_t	soil sensitivity
s	horizontal distance between the centre of anchor and a specific node
s_u	undrained shear strength
$s_{u,ref}$	reference undrained shear strength
T_{max}	limiting shear strength at soil-anchor interface
t_F	fin thickness
u	drag distance of the anchor
v	penetration velocity
v_i	anchor impact velocity
w_F	fin width
W_d	anchor dry weight
W_s	anchor submerged weight

z	depth below soil surface
z_p	padeye depth from anchor tail
$\dot{\gamma}$	shear strain rate
$\dot{\gamma}_{ref}$	reference shear strain rate
δ_{rem}	remoulded strength ratio
θ_a	pull-out angle at padeye
θ_0	pull-out angle at mudline
μ	rate parameter
μ_c	coulomb friction coefficient
ξ	cumulative plastic shear strain
ξ_{95}	cumulative shear strain required for 95% remoulding

INTRODUCTION

Dynamically installed anchors (DIAs) are released from a designed height above the seabed, allowing them to gain velocity as they fall freely through the water column before impacting and embedding within the sediments. The mooring lines are then attached to the floating facility, imposing monotonic or cyclic loading to the anchors during the design life. Torpedo anchors equipped with zero to four fins at the trailing edge are the most commonly used DIAs – typically 12–17 m long (L_A), 0.8–1.2 m in diameter (D_A) and 230–1150 kN dry weight (W_d) (Fig. 1; Brandão *et al.*, 2006). This study focuses on the behaviour of torpedo anchors subjected to monotonic pull-out loading in clay.

Numerical investigations on the pull-out performance of torpedo anchors are very limited. de Sousa *et al.* (2011) carried out a series of finite-element (FE) simulations using Ansys. To avoid mesh distortion from large deformation, they adopted ‘enhanced strain hexahedral and prismatic isoparametric FE’. Lieng *et al.* (2000) performed wished-in-place analysis with small strain theory using Abacus/Standard. Large-deformation FE (LDFE) analyses on pre-embedded anchors (or wished-in-place) in ideal Tresca material ignoring the influence of installation were carried out by Wang & O’Loughlin (2014) and O’Beirne *et al.* (2015). They noted that conventional small strain FE analyses result in ever-increasing load magnitudes where the

Manuscript received 13 July 2016; first decision 26 September 2016; accepted 27 September 2016.

Published online at www.geotechniqueletters.com on 14 October 2016.

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Fig. 1. Schematic diagram of installed anchor and subsequent pull-out

calculation is forced to stop due to severe entanglement of soil elements, necessitating to set some mandatory criteria to define the ultimate capacity such as the capacity achieved prior to such severe entanglement.

Although they tried to consider a specific element type, it is not a true LDFE method unless the distorted soil elements solve, with all of them being either limited to small strain analyses (Lieng *et al.*, 2000; de Sousa *et al.*, 2011) or pre-embedded anchors (or wished-in-place) in ideal Tresca material ignoring the influence of installation (Wang & O’Loughlin, 2014; O’Beirne *et al.*, 2015).

In this study, integrated three-dimensional (3D) dynamic installation followed by immediate monotonic pull-out LDFE analyses were undertaken on torpedo anchors, consisting of a circular shaft, to which zero and four rectangular fins were attached. The LDFE analyses were carried out using the coupled Eulerian–Lagrangian approach in the commercial FE package Abacus/Explicit (DS, 2012). Kim *et al.* (2015a, 2015b) provided an extensive information with regard to 3D dynamic installation and hence are not repeated here. The pull-out of the anchor at various angles at the padeye (θ_a), accounting for the soil conditions disturbed by the installation phase, was carried out applying an inclined displacement, rather than an inclined force, to obtain ultimate capacity.

A typical mesh is shown in Fig. 2. A ‘very fine mesh zone’ and ‘fine mesh zone’ was generated to accommodate the anchor trajectory during the entire installation and pull-out, respectively. On the basis of a convergence study, the typical minimum soil element size (h_{min}) was adopted as $0.019D_A$ in the ‘very fine mesh zone’ and $0.047D_A$ in ‘fine mesh zone’. The anchor was simplified as a rigid body.

The installation and pull-out of DIAs in clay are completed under undrained conditions. The soil was therefore modelled as an elastic perfectly plastic material obeying a Tresca yield criterion, but extending to capture strain-rate and strain-softening effects using the Einav & Randolph (2005) models for quasi-static pull-out according to

$$s_u = \left[1 + \mu \log \left(\frac{\text{Max}(|\dot{\gamma}|, \dot{\gamma}_{ref})}{\dot{\gamma}_{ref}} \right) \right] \times \left[\delta_{rem} + (1 - \delta_{rem}) e^{-3\dot{\gamma}/\dot{\gamma}_{95}} \right] s_{u,ref} \quad (1)$$

The definitions are given under notation list, with the details reported by – for example, Hossain & Randolph (2009) and

Fig. 2. Typical mesh used in LDFE analysis

Zheng *et al.* (2015). Reference strain rate $\dot{\gamma}_{ref} = 0.015 \text{ h}^{-1}$ (Lunne & Andersen, 2007), rate parameter $\mu = 0.1$ (Low *et al.*, 2008), fully remoulded ratio $\delta_{rem} = 1/S_r = 1/3$ and ductility parameter $\zeta_{95} = 20$ (i.e. 2000% shear strain; Randolph, 2004) were considered. The elastic behaviour was defined by a Poisson’s ratio of 0.49 and Young’s modulus of $500s_u$ throughout the soil profile. Total stress analyses were carried out adopting a uniform effective unit weight of 6 kN/m^3 over the soil depth. The soil–anchor interface was modelled as frictional contact, using a general contact algorithm and specifying a (total stress) Coulomb friction law together with a limiting shear stress (τ_{max}) along the anchor–soil interface. The Coulomb friction coefficient was set to a high value of $\mu_c = 50$, in order to allow the value of τ_{max} to govern failure (Ma *et al.*, 2014). For each case, the limiting interface friction was determined by setting τ_{max} equal to an interface friction ratio, α times the average s_u along the anchor length, with α taken as the inverse soil sensitivity, $1/S_r$.

RESULTS AND DISCUSSION

The details of failure mechanisms associated with dynamic installation of different torpedo anchor configurations have been documented by Kim & Hossain (2015) and Kim *et al.* (2015a, 2015b). Results in the following sections will focus on the pull-out behaviour of torpedo anchors, starting with a validation exercise against field test data.

Validation against field data

A series of load tests were carried out in soft normally consolidated clay ($s_{u,ref} \approx 5 + 2z \text{ kPa}$) in the Campos basin (Medeiros, 2002). The finless anchors (anchor A1; Table 1) were pulled from the installed depth of $d_{e,t} = 20 \text{ m}$ (for impact velocity $v_i = 12 \text{ m/s}$) at an angle at the midline of $\theta_0 = 0^\circ$ to the horizontal, and after two set-up periods of 0 days (instant pull-out) and 10 days after installation.

Table 1. Torpedo anchor details

Description	Symbol: unit	A1*	A2	A3	A4	A5	A6	A7	A8	A9	B1†
Total anchor length	L_A : m	12	12	12	12	12	12	12	12	12	17
Anchor shaft length	L_S : m	11.34	11.34	11.34	11.34	11.34	11.34	11.34	11.34	11.34	15
Anchor tip length	L_T : m	0.66	0.66	0.66	0.66	0.66	0.66	0.66	0.66	0.66	2
Number of fins	—	0	0	0	0	0	0	0	0	0	4
Fin length	L_F : m	—	—	—	—	—	—	—	—	—	10
Fin thickness	t_F : m	—	—	—	—	—	—	—	—	—	0.08
Fin width	w_F : m	—	—	—	—	—	—	—	—	—	0.9
Anchor shaft diameter	D_A : m	0.76	0.76	0.76	0.76	0.76	0.76	0.76	0.76	0.76	1.07
Anchor frontal projected area equivalent diameter	D_p : m	0.76	0.76	0.76	0.76	0.76	0.76	0.76	0.76	0.76	1.23
Anchor projected area	A_p : m ²	0.45	0.45	0.45	0.45	0.45	0.45	0.45	0.45	0.45	1.19
Total surface area	A_s : m ²	27.08	27.08	27.08	27.08	27.08	27.08	27.08	27.08	27.08	118.82
Anchor submerged weight	W_s : kN	188	188	188	188	188	188	188	188	188	850
Padeye depth ratio	z_p/L_s	0	0.33	0.5	0.54	0.56	0.58	0.66	0.7	1	0
		(padeye A‡)	(B)	(C)	(D)	(E)	(F)	(G)	(H)	(I)	(A)

*Prototype finless anchor tested in the field (Medeiros, 2002).

†Prototype four-fin anchor tested in the field (Brandão *et al.*, 2006).

‡Different padeye locations (Fig. 1).

LDFE analyses were carried out using these data. The pull-out angle at the padeye (θ_a) was adopted as 41° following the Neubecker & Randolph (1995) approach.

Figure 3 shows the pull-out resistances from field measurements and LDFE analyses. The computed maximum inclined pull-out resistance ($F_{i,max}$) falls within the range of measured pull-out capacities. The reasonable consistency with the test data confirms the capability and accuracy of the numerical model in assessing the pull-out resistance of DIAs in clay soil.

Parametric study

To examine the effect of various factors on pull-out capacity, an extensive parametric study was carried out, as summarised in Tables 1 and 2. For anchors A1–A9, the shape and weight were chosen identical to the finless anchors installed in the Campos basin (Medeiros, 2002), with the padeye location varied for parametric analyses (Fig. 1). Anchor B1 mimicked the field test reported by Brandão *et al.* (2006) on a four-fin anchor.

Effect of installation method. To investigate the effect of installation techniques on anchor pull-out loads, first dynamic installation ($v_i = 12$ m/s) was simulated, giving the tip installed depth of $d_{e,t} = 20$ m. Subsequently, a quasi-static penetration ($v = 0.1$ m/s) analysis was carried out up to a penetration of 20 m. The anchors were vertical, with no tilt, after these installation processes. The anchors were then pulled out at different pull-out angles at the padeye ($\theta_a = 30^\circ$ and 90°). Figure 4 shows the load–displacement curves ($s_{u,ref} = 5 + 2z$ kPa; group I, Table 2). For the vertical pull-out cases ($\theta_a = 90^\circ$), the results indicate that quasi-static installation produces around 1.5 times higher pull-out capacity than dynamic installation due to a lower soil disturbance adjacent to the quasi-static installed anchor (as discussed below) and hence higher shaft friction. However, for the low pull-out angle ($\theta_a = 30^\circ$), the effect of installation method reduces to marginal, with the difference being under 5%. From a series of centrifuge tests conducted on finless and four-fin anchors, Richardson *et al.* (2009) quantified the effect of installation method on the vertical pull-out capacity as about 1.6 times for a set-up period of 20–27 days and nearly negligible for higher set-up periods $> \sim 240$ days.

Fig. 3. Comparison between LDFE result and field data

No data were reported for the immediate (0 set-up period) pull-out capacity.

Figure 5 shows the soil strength distributions at three radial distances from the anchor surface of $s = 0.52D_A$, $1.05D_A$ and $1.65D_A$ and at three soil depths of $z = 8, 14$ and 20 m during dynamic and quasi-static installation process. Overall, it is seen that the soil strength increases sharply as the anchor tip approaches the selected depth (e.g. $z = 8$ m) due to the effect of higher strain rate, and then drops abruptly as the anchor tip passes through the depth, which is associated with the domination of remoulding or softening. The degree of decrease is much higher than the degree to enhancement. For the selected closest distance from the anchor ($s = 0.52D_A$; Fig. 5(a)), the percentage of strength reduction is more pronounced for dynamic installation (47.6–51.2%) compared with those (26.2–39.3%) for quasi-static installation. However, for both dynamic and quasi-static installations, this percentage reduces gradually with radial distance from the anchor and becomes negligible for $s = 1.65D_A$ (Figs 5(b) and 5(c)). This means that the

Table 2. Summary of 3D LDFE analyses performed

Group	Anchor	$s_{u,ref}$: kPa	v_i : m/s	$d_{e,t}$: m	θ_a : deg	$F_{i,max}$: kN	Note
I	A1	$5 + 2z$	—	20	30	1010	Effect of installation method (quasi-static penetrating velocity, $v = 0.1$ m/s)
	A1	$5 + 2z$	12	20	30	958	
	A1	$5 + 2z$	—	20	90	650	
	A1	$5 + 2z$	12	20	90	395	
II	A1	$5 + 2z$	15	22.9	45	950	Effect of installed depth and anchor geometry
	A1	$5 + 2z$	20	26.8	45	1275	
	A1	$5 + 2z$	25	29.6	45	1608	
	B1	$5 + 2z$	15	29.1	45	5400	
	B1	$5 + 2z$	20	32.2	45	6302	
III	B1	$5 + 2z$	25	34.9	45	7407	Effect of padeye location
	A1	$5 + 2z$	12	20	45	921	
	A2	$5 + 2z$	12	20	45	1631	
	A3	$5 + 2z$	12	20	45	2440	
	A4	$5 + 2z$	12	20	45	2602	
	A5	$5 + 2z$	12	20	45	2696	
	A6	$5 + 2z$	12	20	45	2740	
	A7	$5 + 2z$	12	20	45	2355	
	A8	$5 + 2z$	12	20	45	2215	
	A9	$5 + 2z$	12	20	45	1310	
	A1	$5 + 2z$	25	29.6	45	1608	
	A2	$5 + 2z$	25	29.6	45	2910	
	A3	$5 + 2z$	25	29.6	45	4005	
	A4	$5 + 2z$	25	29.6	45	4315	
	A5	$5 + 2z$	25	29.6	45	4480	
	A6	$5 + 2z$	25	29.6	45	4300	
	A7	$5 + 2z$	25	29.6	45	3780	
	A8	$5 + 2z$	25	29.6	45	3360	
	A9	$5 + 2z$	25	29.6	45	2002	
IV	A1	$5 + 2z$	12	20	0	1003	Effect of inclined pull-out angle
	A1	$5 + 2z$	12	20	70	836	
	A1	$5 + 2z$	12	20	80	566	
	A7	$5 + 2z$	12	20	0	2514	
	A7	$5 + 2z$	12	20	30	2407	
	A7	$5 + 2z$	12	20	70	2166	
	A7	$5 + 2z$	12	20	80	1841	
	A7	$5 + 2z$	12	20	90	393	
	B1	$5 + 2z$	15	29.1	0	5834	
	B1	$5 + 2z$	15	29.1	30	5658	
	B1	$5 + 2z$	15	29.1	70	4735	
	B1	$5 + 2z$	15	29.1	80	2298	
	B1	$5 + 2z$	15	29.1	90	1944

Fig. 4. Effect of installation method on anchor pull-out capacity (group I, Table 2)

radial extent of disturbance is confined to a narrow band around the anchor, regardless of the installation methods, leading to significant influence on the vertical pull-out capacity, while there is marginal effect on the inclined pull-out capacity.

Effect of installed depth and anchor geometry. The effect of installed depth is displayed in Fig. 6 ($s_{u,ref} = 5 + 2z$ kPa) plotting inclined ($\theta_a = 45^\circ$) pull-out resistance for a range of v_i from 15 to 25 m/s for anchors A1 and B1 (group II, Table 2). As expected, the installed depth $d_{e,t}$ increases with increasing impact velocity v_i (Table 2), and the higher pull-out resistance corresponds to deeper installed depth with the mobilisation of higher undrained shear strength.

Generally, the inclined pull-out capacity of an anchor is a function of anchor weight, total anchor–soil contact surface area, anchor embedment depth and undrained shear strength of the surrounding soil. For the finless anchor, the anchor submerged weight (W_s) and total surface area (A_s) are significantly lower compared with the four-fin anchor (Table 1), resulting in a 71% lower pull-out capacity where both anchors were pulled from similar

Fig. 5. Soil strength distributions during installation process (in group I, Table 2): (a) $s = 0.52D_A$; (b) $s = 1.05D_A$; (c) $s = 1.65D_A$

Fig. 6. Effect of installed depth and anchor geometry on anchor pull-out capacity (group II, Table 2)

installed depths of $d_{e,t} = 29.1$ and 29.6 m (Table 2) – that is, the undrained shear strength of the surrounding soil was also similar.

Effect of padeye location. To optimise the padeye location for the finless anchor, nine different pull-out loading points (padeye A–I; Fig. 1) were considered. The anchors were installed to two depths ($d_{e,t} = 20$ and 29.6 m) and then pulled out at $\theta_a = 45^\circ$ ($s_{u,ref} = 5 + 2z$ kPa; groups III, Table 2). Figure 7 shows typical pull-out resistance profiles, as a function of drag distance (u), for $d_{e,t} = 29.6$ m. From the results, it confirms that the shifting of the padeye location from A to E can increase the pull-out capacity by up to 266%. For a shallow installed depth ($d_{e,t} = 20$ m), this per cent of capacity enhancement has slightly increased (289%; Table 2).

Fig. 7. Pull-out resistance profiles for different padeye locations (in group III, Table 2)

Figure 8 depicts the instantaneous (resultant) velocity vectors during pull-out of the anchors with different padeye locations. It shows the soil failure mechanisms at five different padeye locations after a dragging distance of 0.5 m ($u = 0.5$ m).

For anchor A5 (padeye E), the soil adjacent to the anchor shaft moves laterally. This indicates that the anchor translates with no rotation (or a little backward rotation; Fig. 8(c)). With this failure mode, the resisted surface area of the anchor can be the maximum (and hence resistance; Fig. 7). This is the optimised padeye location (around $z_p = 0.56L_s$). However, the anchor with the traditional padeye location A (anchor A1) rotates in the loading direction (forward rotation) and then lifts up (Fig. 8(a)). In that case, the resisted surface area is relatively smaller, resulting in a much lower pull-out resistance.

Overall, the anchor movement is dictated by the forward rotational movement, if the loading point is higher than

Fig. 8. Soil failure mechanism in different padeye locations (in group III, Table 2): (a) anchor A1; (b) anchor A2; (c) anchor A5; (d) anchor A7; (e) anchor A9

Fig. 9. Effect of padeye location on anchor pull-out capacity (group III, Table 2)

the optimum padeye location (Figs 8(a) and 8(b)). When the loading point lies below the optimum padeye location, the anchor movement is dominated by backward rotation (Figs 8(d) and 8(e)). A similar variation in inclined pull-out capacity of suction caisson for various padeye locations has been reported by Supachawarote (2007). The normalised inclined pull-out capacities for different padeye locations are presented in Fig. 9, as a function of normalised padeye depth, z_p/L_s . Although there are some differences in terms of soil failure mechanisms mobilised with suction caisson (with the lid always at the mudline) and torpedo anchor (with the top fully embedded in the seabed) pull-out, the trend of normalised pull-out capacity is consistent.

Fig. 10. Normalised anchor capacity under inclined pull-out loading (groups I–IV, Table 2)

Figure 7 also includes the pull-out resistance profiles for the four-fin anchor (with $d_{e,t} = 29.1$ m) for comparison. It is seen that the maximum inclined pull-out capacity ($F_{i,max}$) of the finless anchor with conventional padeye location (anchor A1) is $\sim 29\%$ of the four-fin anchor – that is, the gap is 71%. The shifting of the padeye location to $z_p = 0.56L_s$ (padeye E; anchor A5 in Table 1) reduces the gap to 17%. Note, the four-fin anchor is much heavier and has a larger anchor surface area than the finless anchor (Table 1). This indicates that this concept may allow the offshore industry to achieve the required capacity using reduced number of fins (i.e. steel and hence cost). Although, in that case, corresponding hydrodynamic analysis is required to be conducted for dropping the anchor vertically through a water column, it is achievable as has been applied for the OMNI-Max anchor (Zimmerman *et al.*, 2009).

Effect of inclined pull-out angle. The pull-out angle at the padeye (θ_a) was so far kept constant at 45° . To investigate the effect of pull-out load inclination, a series of simulations were carried out varying θ_a as 0, 30, 45, 70, 80 and 90° (group IV, Table 2). The holding capacities for all cases are tabulated in Table 2. It is seen that, for vertical pull-out (with traditional padeye position A), the anchoring capacity is 1.85–2.3 times the dry anchor weight (W_d). For $\theta_a = 0$, this increased to $\sim 5.5W_d$, which is compatible with the range and trend obtained from centrifuge model tests (Hossain *et al.*, 2014). The inclined pull-out capacity (F_i) was divided into horizontal, $F_{i,H}$, and vertical, $F_{i,V}$, components according to the loading angle, θ_a . Figure 10 represents the maximum states normalised by the maximum uniaxial loads (i.e. by F_{Vult} , the ultimate load obtained from uniaxial vertical loading analysis or F_{Hult} , the ultimate load obtained from uniaxial horizontal loading analysis), $F_{i,v} = F_{i,V}/F_{Vult}$ and $F_{i,h} = F_{i,H}/F_{Hult}$, indicating the shape and relative size of the failure envelope. All the normalised data from LDFE simulations in Fig. 10 form a relatively tight band. de Sousa *et al.* (2011) and O’Beirne *et al.* (2015) reported anchor capacities for a geometrically similar torpedo anchor from FE analyses (as discussed at the onset of the paper) and these data are included in the figure for comparison. However, it is worth noting that these numerical analyses did not consider the effect of installation (pre-embedded anchor) and the effects of strain softening and rate dependency of shear strength (considered ideal Tresca soil). As such, the immediate vertical pull-out capacity was relatively high ($F_{Hult} = 1.1F_{Vult}$; O’Beirne *et al.*, 2015).

Gourvenec (2008) examined the effect of the embedment ratio on the V–H failure envelope of shallow strip foundations in clay. A power law was proposed to describe the shape of the normalised failure envelope for normalised embedment ratios (embedment depth relative to foundation size) ≥ 0.5 as $F_{i,v} = (1 - F_{i,h})^{0.25}$. Failure envelopes, established using the results from FE analyses, were also proposed for short piles and hybrid pile foundations in clay (with length to diameter ratios of ~ 2) by El-Marassi (2011) and Fan & Meng (2011), respectively. Although these envelopes are for different foundation geometries, the maximum states were normalised by the corresponding maximum uniaxial loads, justifying the comparison with the envelopes for torpedo anchors. The envelopes are included in Fig. 10, where it can be seen that the data from this study fall mostly between the envelopes proposed by Gourvenec (2008) and El-Marassi (2011).

It should be that, in the field, an anchor is attached with a chain and the load is induced through the chain. The pull-out angle at the padeye (θ_a) therefore varies continually as the anchor chain profile through the soil evolves and the anchor rotates during the pull-out process (Neubecker & Randolph, 1995; Wang *et al.*, 2011; Kim *et al.*, 2015c). The effect of an embedded anchor chain profile was not considered in this study. Nevertheless, the failure load for a given load padeye inclination can be predicted from the non-dimensional failure envelope.

CONCLUDING REMARKS

Integrated dynamic installation–monotonic pull-out analyses have been carried out on torpedo anchors through large-deformation analyses, accounting for strain-softening and strain-rate dependency of the undrained shear strength. The following key conclusions can be drawn from the results presented in the paper.

- Installation method – for example, quasi-static and dynamic installation, has shown significant influence on

vertical pull-out capacity, but negligible influence on the lateral pull-out capacity.

- Due to the lower weight and total surface area, the 45° (at the padeye) pull-out capacity of the finless anchor was 71% lower than that of the considered four-fin anchor. This gap can be reduced to 17% by shifting the padeye location at $z_P = 0.56L_S$.
- Anchor capacity under inclined pull-out loading lies between the envelopes developed for piles and embedded foundations.

ACKNOWLEDGEMENTS

The research presented here was undertaken with support from the Australian Research Council through the Discover Early Career Researcher Award DE140100903. The work forms part of the activities of the Centre for Offshore Foundation Systems (COFS), currently supported as a node of the Australian Research Council Centre of Excellence for Geotechnical Science and Engineering, through Centre of Excellence funding from the State Government of Western Australia and in partnership with The Lloyd’s Register Foundation. This support is acknowledged, as is the benefit of discussion with Dr Dong Wang.

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