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Calibration of Reliability-based Resistance Factors for Soil Anchors in Taipei Basin

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ABSTRACT: The goal of this research is to calibrate the reliability-based resistance factor of flush drilled soil anchors for their ultimate pullout capacities based on in-situ anchor pullout test data in the alluvial soil underlying the Taipei basin. Efforts are taken to quantify the uncertainties with a full probabilistic analysis approach. The resistance factor is calibrated based on the in-situ test results of 46 anchors with a rigorous theoretical approach which constructs the relationship between the resistance factor and failure probability. With this relationship, the reliability corresponding to the code regulation can be verified. From the results of the analysis, it is found that the borehole enlargement due to the flush drilling is quite significant: the actual diameter of the fixed anchor end may be much larger than the nominal diameter of the drilling casing. Consequently, the safety factor of three recommended by most anchor codes is found to be too conservative. The results should be valuable for reliability-based design of flush drilled soil anchors in the Taipei basin.

1 INTRODUCTION

Traditionally, soil anchor designs are usually achieved by the safety-factor approach, which accounts for the uncertainties in soil anchors (e.g.: uncertain soil parameters, uncertain mechanisms, uncertainties in the laboratory and in-situ tests, etc.) with an empirical basis. The safety-factor approach has been working reasonably well: as long as a large safety factor is adopted, the anchor system can usually be made quite reliable.

However, safety factor is not a consistent measure of uncertainties: it does not tell us how reliable the designed anchor system is. Mathematically, safety factor is not a consistent indicator of reliability: the safety factor of the same limit state may change depending on the mathematical expression of the limit state function (Phoon 1995). As a consequence, geotechnical engineers tend to be conservative in choosing safety factors, and sometimes they can be overly conservative in order to ensure reliable anchor designs. This over design does not fit in the nowadays needs in terms of economic and ecological considerations. In contrast, since safety factor does not directly provide information about reliability, it is possible that designers may choose a safety factor corresponding to unsatisfactory reliability.

Therefore, it is essential to verify if the safety factor adopted in the design code is appropriate: What is the corresponding reliability for the safety factor adopted in codes? Does the corresponding reliability meet the design requirements? Is the adopted safety factor too conservative? One way of answering those questions is through a probabilistic analysis that constructs the relationship between the factor of safety and reliability (one minus failure probability). In this paper, the term “resistance factor”, simply the reciprocal of safety factor, will be employed instead of factor of safety.

More specifically, the afore-mentioned verification will be only conducted for soil anchor designs in the Taipei basin in Taiwan through a reliability-based theoretic design approach. Reliability-based analysis and design methods have been proposed for deep foundations (Zhang et al. 2001; McVay et al. 2003; Paikowsky et al. 2004), for shallow foundations (Phoon et al. 2003), for retaining walls (Chalermyanont and Benson 2004), and for slope stability (Christian et al. 1994; Low et al. 1998) to name a few. For soil anchors, a systematic study of the possible implementation of a reliability-based design method is still lacking.

A database containing the in-situ test results of 46 soil anchors in the Taipei basin is compiled and analyzed to construct the relationship between the resistance factor and target reliability. This relationship will be used to verify the anchor design code in Taiwan. Since only local database of Taipei is used for the analysis, the conclusion may be only applicable to anchor designs in Taipei.

2 DATABASE

The database contains the in-situ pullout test results of 46 soil anchors in the Taipei basin. For each anchor, the following information is available: (a) the pullout force v.s. anchor head movement of the test anchors; (b) the soil profile nearby the anchor location, including soil classification, soil layer thickness, SPT-N value, unit weights, location of water table, and, in some cases, the undrained shear strength of the clayey layers; (c) the configuration of the anchor, including the free length and fixed length, the diameter of the drilling casing, the number of steel tendons, etc. The actual pullout capacity of each anchor is determined from the curve of in-situ pullout force v.s. anchor head movement using the method recommended by FIP (1982).

The boreholes of all test anchors were drilled with rotary casing and the debris was flushed out with water from outside of the casing. Since the amount of alluvial soils being flushed out is difficult to control, the process might result in a borehole with a larger diameter than that of the drilling casing and an irregular borehole surface. So the exact borehole diameter could not be determined and the diameter of the drilling casing was usually assumed as the nominal anchor diameter in the practice. After drilling was completed, the assembled tendons were installed into the casing and cement grout with a water/cement ratio 0.45-0.5 was injected into the borehole under a pressure of 200kPa. During grouting, the drilling casing was withdrawn gradually. After the casing for the fixed anchor end has been removed, a pressure of about 1000kPa was used to pressure-grout the anchor. In general, the grout takes for anchors were about double the volume calculated from the nominal dimension of anchors. Due to the high fines content, cement grout cannot permeate into the voids of the alluvial soils. The main purpose of pressure-grouting is to recompact the soils loosened during borehole drilling. The further details of anchor installation can be referred to Liao et al. (1996).

3 MODELS FOR PULLOUT CAPACITY

For the probabilistic analysis, a non-probabilistic predictive model for the anchor capacity must be provided. The following model is often adopted to compute the nominal pullout capacity of an anchor:

$$C^n = d\pi\left[\sum_{i=1}^{N_c} s_u^{(i)} l_c^{(i)} + \sum_{i=1}^{N_s} K^{(i)}\sigma^{(i)}\tan\left(\phi^{(i)}\right) l_s^{(i)}\right] \tag{1}$$

where d is the nominal diameter of the anchor fixed end, often taken as the diameter of the drilling casing; N_c and N_s is the total number of clayey and sandy soil layers within the fixed end; $l_c^{(i)}$ (or $l_s^{(i)}$) is the fixed length within the i-th clayey (or sandy) layer, while $S_u^{(i)}$ (or $\phi^{(i)}$) is the undrained shear strength (or effective friction angle) of the i-th clayey (or sandy) layer; $\sigma^{(i)}$ is the average vertical effective stress in the i-th sandy layer; $K^{(i)}$ is the earth pressure coefficient in the i-th sandy layer, i.e. the effective normal stress on the anchor shaft divided by $\sigma^{(i)}$. It is assumed here that the soil around the fixed end is in at-rest state, so $K^{(i)}$ is roughly 1-sin($\phi^{(i)}$) if the anchor is vertical and is 1 if the anchor is horizontal. In the case that the anchor is inclined with an angle equal to ($\theta = 90^{\circ}$ is vertical), a simple Mohr-Coulomb analysis is used to find $K^{(i)}$.

Equation (1) assumes that the developed shear resistance along the fixed end is uniform within the same soil layer. However, it is reported by Hawkes and Evans (1951) that the distribution of developed shear resistance along the fixed anchor length is not uniform even in a homogeneous soil layer: the developed shear resistance is fully developed at the top of the fixed end but is only partially mobilized away from the top. This non-uniformity is more pronounced when the surrounding soils are stiff. Hawkes and Evans (1951) proposed the following equation to account for such decay:

$$\frac{\tau_x}{\tau_0} = e^{-\alpha x/d} \tag{2}$$

where τ0 is the shear resistance developed at the top of the fixed end; τx is the shear resistance developed at a distance of x from the top of the fixed end; α is called the decay parameter, whose value usually depends on the stiffness of the surrounding soils.

In reality, the α parameters of different soil layers should be different, but in this paper, the α parameters of different soil layers are assumed to be identical since the soil stiffnesses within the fixed end do not drastically change. Under such a simplifying assumption, a more sophisticated model can be derived as follows:

$$C^{\alpha} = d\pi\left[\sum_{i=1}^{N_c}\left(\int_{L_c^{(i)}}^{U_c^{(i)}} s_u^{(i)}(x)\cdot e^{-\alpha x/d}dx\right)+\sum_{i=1}^{N_s}\left(K^{(i)}\tan\left(\phi^{(i)}\right)\int_{L_s^{(i)}}^{U_s^{(i)}}\sigma(x)\cdot e^{-\alpha x/d}dx\right)\right] \tag{3}$$

where Uc(i) and Lc(i) are the distances (along the fixed anchor length) to the top of the fixed end from the upper and lower interface of the i-th clayey layer, and similar for Us(i) and Ls(i) ; σ(x) is the effective vertical stress at a distance of x from the top of the fixed end, Su(i) (x) is proportional to σ(x), and d is the diameter of the drilling casing.

4 MODELING OF UNCERTAINTIES

The basic uncertainties include the unit weight, friction angle, ratio of undrained shear strength to vertical effective stress of each soil layer as well as the effective diameter D and the decay parameter α. Uncertainties like σ(x), $K^{(i)}$, and $S_u^{(i)}(x)$ are not basic since they are functions of the basic ones.

To quantify the uncertainties, it is desirable to first specify the joint prior probability density function (PDF) (Ang and Tang 1975) of all basic uncertainties. The term "prior" PDF denotes the PDF before new data, i.e. the actual capacities of the 46 anchors, is available. Later we will mention the "posterior" PDF (Ang and Tang 1975) of the basic uncertainties, which denotes the PDF after the new data is available. We will constantly use the following notation for uncertain variables: $X_j^{(i)}$ is the X variable of the i-th soil layer in the fixed end of the j-th anchor, and $X_{1:j}^{(1:i)}$ denotes $\{X_1^{(1)},\ldots X_1^{(i)},\ldots, X_j^{(1)},\ldots, X_j^{(i)}\}$. Let us first divide the basic uncertainties into two categories:

(a) Uncertain soil parameters, including friction angles $\phi_{1:46}^{(1:N_j^s)}$ and unit weights $\gamma_{1:46}^{(1:N_j^s)}$ of all sandy layers (N_j^s is the number of sandy layers in the fixed end of the j-th anchor) and ratios between undrained shear strengths and effective vertical stresses $\beta_{1:46}^{(1:N_j^c)}$ and unit weights $\gamma_{1:46}^{(1:N_j^c)}$ of all clayey layers (N_j^c is the number of clayey layers in the fixed end of the j-th anchor). If the soil is above water table, $\gamma_{s,j}^{(i)}$ and $\gamma_{c,j}^{(i)}$ mean the unsaturated unit weights of the i-th sandy and clayey soil layers, respectively; otherwise, they are the saturated ones.

(b) Uncertain model parameters, including the effective diameter $D_{1:46}$ and decay parameter $\alpha_{1:46}$.

When specifying the joint prior PDF of the basic uncertainties, the following assumptions are taken:

(a) The α parameters of all anchors are assumed to be uncertain but identical, i.e. $\alpha_1=\ldots=\alpha_{46}=\alpha$. This assumption should be mild because all anchors are in the Taipei basin, so the decay behavior along the fixed end of all anchors may be similar. An obvious limitation is that α must be positive. Moreover, according to Hawkes and Evans (1951), $\alpha \fallingdotseq 0.1$ for rock anchors, which implies α for soil anchors should be less than 0.1. Other than these, there is not much prior information about the decay parameter. Therefore, the prior PDF of α is taken to be uniform over [0, 0.1].

(b) The effective diameters of the j-th anchor D_j in the database is assumed to be equal to the diameter of the drilling casing, denoted by d_j, multiplied by an enlargement factor ρ_j, and it is assumed that the $\rho_{1:46}$ factors of all anchors are independent but identically distributed. It is reasonable to assume the $\rho_{1:46}$ factors to be independent because the enlargement effect of one anchor does not seem to provide information about the effect of another anchor. They are assumed to be identically distributed because the drilling and grouting procedure for all the anchors is roughly the same. The $\rho_{1:46}$ factors are assumed to be lognormal with mean value μ_p and coefficient of variation δ_p (c.o.v.: standard deviation divided by mean) where μ_p and δ_p are two uncertain parameters. The prior PDF of μ_p is taken to be uniform constrained by $\mu_p \geqq 1$, while that δ_p is taken to be uniform constrained by $\delta_p \geqq 0$ Note that this arrangement makes $D_{1:46}$ out of the list of the basic uncertainties, but now the basic uncertainties include μ_p and δ_p.

(c) In our database, borehole data of the unit weight is available for each soil layer. Despite the availability of the borehole data, the unit weight is, in principle, still uncertain. The reported value of unit weight in the borehole data is taken to be its mean value, denoted by $\mu_{\gamma s,j}{}^{(i)}$ for sandy soils and $\mu_{\gamma c,j}{}^{(i)}$ for clayey soils , while their c.o.v., denoted by $\delta_{\gamma s,j}{}^{(i)}$ and $\delta_{\gamma c,j}{}^{(i)}$, are taken to be 10% based on the c.o.v. of the unit weight suggested by Phoon (1995). All unit weights are assumed to be independent and lognormal since the knowledge of one unit weight does not seem to provide information on another.

(d)Based on the report published by Moh and Associates (1987), the mean value of $\beta_j{}^{(i)}$, denoted by $\mu_{\beta,j}{}^{(i)}$, is taken to be 0.21 and c.o.v., denoted by $\delta_{\beta,j}{}^{(i)}$, is taken to be 30% if the i-th clayey layer of the j-th anchor is below 12m; otherwise, its mean value and c.o.v. are taken to be 0.36 and 20%, respectively. The $\beta_j{}^{(i)}$ values of different layers and for different anchors are assumed to be independent and lognormally distributed.

(e) The mean value of $\phi_j{}^{(i)}$ is estimated based on the SPT-N value of the i-th sandy layer of the j-th anchor. Using the results reported in Moh and Associates (1987), the mean value and c.o.v. of $\phi_j{}^{(i)}$,denoted by $\mu_{\phi j}{}^{(i)}$ and $\delta_{\phi j}{}^{(i)}$, can be identified from the SPT-N value. The $\phi_j{}^{(i)}$ values of different layers and for different anchors are assumed to be independent and lognormally distributed.

Perhaps, the most important uncertainty in the problem is the uncertain pullout capacity of an anchor. In most literature, the uncertain (actual) capacity of the j-th anchor C_j is usually taken to be the product of the predicted capacity with a "model factor", which characterizes the model uncertainty. However, for our model, the ρ_j parameter plays a similar role to the model factor. It seems reasonable not to use an extra factor but treat ρ_j as a combined parameter characterizing both the borehole enlargement effect and the model uncertainty. If this approach is taken, it is clear that the uncertain capacity of the j-th anchor C_j can be expressed as

$$\begin{aligned} C_j &= \rho_j C^{\alpha}\left(Y_j\right) \\ &= \rho_j d_j \pi \left[\sum_{i=1}^{N_j^c} \left(\int_{L_j^{c(i)}}^{U_j^{c(i)}} \beta_j^{(i)}(x) \cdot \sigma_j(x) \cdot e^{-\alpha x / d_j} dx \right) + \sum_{i=1}^{N_j^s} \left(K_j^{(i)} \tan\left(\phi_j^{(i)}\right) \int_{L_j^{c(i)}}^{U_j^{c(i)}} \sigma_j(x) \cdot e^{-\alpha x / d_j} dx \right) \right] \end{aligned} \tag{4}$$

where $Y_j = \left\{ \gamma_{S,j}^{(1:N_j^s)}, \gamma_{C,j}^{(1:N_j^c)}, \phi_j^{(1:N_j^s)}, \beta_j^{(1:N_j^c)} \right\}$ denotes the collection of the uncertain soil parameters for the j-th anchor in the database.

4.1 Joint prior PDF of all uncertainties

Once the prior PDF of each individual basic uncertainty is specified, it is possible to combine all prior PDFs into a single joint prior PDF of all uncertainties. For the ease of future discussion, let us denote the model parameters $\{\mu_p, \delta_p, \alpha\}$ by M and denote the collection of all uncertainties $\{M, Y_{1:46}, C_{1:46}\}$ by Z, then the joint prior PDF of Z is

$$
f(Z) = f(M, Y_{1:46}, C_{1:46}) = \left[\prod_{j=1}^{46} f(C_j \mid M, Y_j) f(Y_j)\right] f(M)
$$

$$
= \begin{cases} \propto \prod_{j=1}^{46} \left[f(C_j \mid M, Y_j) \prod_{i=1}^{N_j^s} f(\gamma_{S,j}^{(i)}) \prod_{i=1}^{N_j^c} f(\gamma_{C,j}^{(i)}) \prod_{i=1}^{N_j^s} f(\phi_j^{(i)}) \prod_{i=1}^{N_j^c} f(\beta_j^{(i)}) \right] \\ \qquad \text{if } 0 \le \alpha \le 0.1 \ \& \ \mu_\rho \ge 1 \ \& \ \delta_\rho \ge 0 \\ = 0 \\ \qquad \text{otherwise} \end{cases}
$$

$$
= \begin{cases} \propto \prod_{j=1}^{46} \left[\begin{array}{c} \dfrac{e^{\frac{-1}{2\log(1+\delta_\rho^2)}\left[\log(C_j) - \log\left(\frac{\mu_\rho C^\alpha(Y_j)}{\sqrt{1+\delta_\rho^2}}\right)\right]}}{C_j \sqrt{\log(1+\delta_\rho^2)}} \cdot \prod_{i=1}^{N_j^s} \dfrac{e^{\frac{-1}{2\log(1+\delta_{\gamma_S,j}^2)}\left[\log(\gamma_{S,j}^{(i)}) - \log\left(\frac{\mu_{\gamma_S,j}}{\sqrt{1+\delta_{\gamma_S,j}^2}}\right)\right]}}{\gamma_{S,j}^{(i)} \sqrt{\log(1+\delta_{\gamma_S,j}^2)}} \cdot \prod_{i=1}^{N_j^c} \dfrac{e^{\frac{-1}{2\log(1+\delta_{\gamma_C,j}^2)}\left[\log(\gamma_{C,j}^{(i)}) - \log\left(\frac{\mu_{\gamma_C,j}}{\sqrt{1+\delta_{\gamma_C,j}^2}}\right)\right]}}{\gamma_{C,j}^{(i)} \sqrt{\log(1+\delta_{\gamma_C,j}^2)}} \cdot \\ \prod_{i=1}^{N_j^s} \dfrac{e^{\frac{-1}{2\log(1+\delta_{\phi,j}^2)}\left[\log(\phi_j^{(i)}) - \log\left(\frac{\mu_{\phi,j}}{\sqrt{1+\delta_{\phi,j}^2}}\right)\right]}}{\phi_j^{(i)} \sqrt{\log(1+\delta_{\phi,j}^2)}} \cdot \prod_{i=1}^{N_j^c} \dfrac{e^{\frac{-1}{2\log(1+\delta_{\beta,j}^2)}\left[\log(\beta_j^{(i)}) - \log\left(\frac{\mu_{\beta,j}}{\sqrt{1+\delta_{\beta,j}^2}}\right)\right]}}{\beta_j^{(i)} \sqrt{\log(1+\delta_{\beta,j}^2)}} \end{array} \right] \\ \qquad \text{if } 0 \le \alpha \le 0.1 \ \& \ \mu_\rho \ge 1 \ \& \ \delta_\rho \ge 0 \\ = 0 \\ \qquad \text{otherwise} \end{cases} \tag{5}
$$

This joint prior PDF of all uncertainties Z is the basis of the Bayesian analysis, which is employed in this study to calibrate the resistance factor.

5 CALIBRATION OF RESISTANCE FACTOR

Given the in-situ measured pullout capacities of the 46 anchors, it is possible to calibrate the reliability-based resistance factor for anchor designs in the Taipei basin. The reliability-based design can be achieved by restricting the failure probability of a future design conditioning on the past data, i.e. consider the following equation:

$$
P\left(C_{new} \le C^* \mid C_{1:46}\right) = P_F^* \tag{6}
$$

where C_{new} is the uncertain capacity of a new anchor, and C^* is the design load of the anchor; the $C_{new} \leqq C^*$ is the failure event; $C_{1:46}$ is the capacity data in our database; $P(C_{new} \leqq C^* | C_{1:46})$ is the failure probability conditioning on the capacity data, which, in principle, can be computed with Bayesian analysis; P_F^* is the target failure probability. Equation(6) can be rewritten as

$$
P\left(C_{new} \le C^* \mid C_{1:46}\right) = P\left(\rho_{new} C^\alpha \left(Y_{new}\right) \le C^* \mid C_{1:46}\right) = P_F^* \tag{7}
$$

where ρ_{new} is the model factor of the new anchor, which is lognormally distributed with mean value equal to μ_p and c.o.v. equal to δ_p (recall that it is assumed that all model factors are independent identically lognormally distributed with mean equal to μ_p and c.o.v. equal to δ_p); Y_{new} contains all uncertain soil parameters of the new anchor site. Note that $C^\alpha(Y_{new})$ is uncertain because Y_{new} is uncertain.

Let us assume that the PDF of the soil properties Y_{new} is known from site investigation and appropriate uncertainty characterization. Therefore, the relationship between the resistance factor and target failure probability can be derived as follows:

$$\begin{aligned}&P\left(\rho_{new}C^{\alpha}\left(Y_{new}\right)\Big/C^{D}\left(Y_{new}^{n}\right)\le C^{*}\Big/C^{D}\left(Y_{new}^{n}\right)\Big|C_{1:46}\right)\\&=P\left(\rho_{new}C^{\alpha}\left(Y_{new}\right)\Big/C^{D}\left(Y_{new}^{n}\right)\le\eta\Big|C_{1:46}\right)=P\left(G\left(\rho_{new},\alpha,Y_{new}\right)\le\eta\Big|C_{1:46}\right)=P_F^{*}\end{aligned}\tag{8}$$

where ${Y_{new}}^n$ is the nominal value of Y_{new}, e.g. mean value of Y_{new}; $C^D({Y_{new}}^n)$ can be any predictive model chosen by the designer; $\eta = C^*/C^D({Y_{new}}^n)$ is exactly the resistance factor, which is the reciprocal of the safety factor. One can see that the resistance factor can be simply estimated as the $100 \cdot {P_F}^*$ percentile of $G(\rho_{new},\alpha,Y_{new})$ conditioning on the data $C_{1:46}$. If one can obtain stochastic simulation samples of $G(\rho_{new},\alpha,Y_{new})$ (see Ching et al. (2007) for details), denoted by $\{G^1,\ldots,G^N\}$, conditioning on the data $C_{1:46}$, the relationship between resistance factor and target failure probability can be estimated as the following according to the Law of Large Number:

$$P_F^* \approx \frac{1}{N}\sum_{k=1}^{N} 1\left(G^k \le \eta\right) \tag{9}$$

where 1(.) is the indicator function: it is unity if the inside statement is true and is zero otherwise. Equivalently, η can be estimated as the $100 \cdot {P_F}^*$ largest number in $\{G^1,\ldots,G^N\}$. Once this relationship is obtained, the calibration of the resistance factor is achieved since one can determine the required resistance factor from the target failure probability. Note that the η v.s. ${P_F}^*$ relationship depends on the configuration of the new anchor, the soil profile at the new anchor site, and the adopted capacity model for design C^D.

6 ANALYSIS RESULTS AND VERIFICATIONS

6.1 Samples of stochastic simulations

Ten thousand samples are drawn from $f(\rho_{new},\alpha|C_{1:46})$. These samples contain interesting information about the behaviors of the 46 anchors. For instance, the samples of the α parameter, as shown in Fig..1, are distributed as $f(\alpha|C_{1:46})$, so the histogram of the sample values, also shown in the figure, basically gives the relative degree of plausibility of the α value learned from the data. From Fig..1, it is clear that the decay parameter is mostly in the range of [0.001, 0.004], and the mean value and c.o.v. of the samples of the decay parameter are 0.0025 and 0.253, respectively. Moreover, the possibility of $\alpha = 0$ is close to zero, i.e. compared to the C^{α} model, the C^n model is basically rejected.

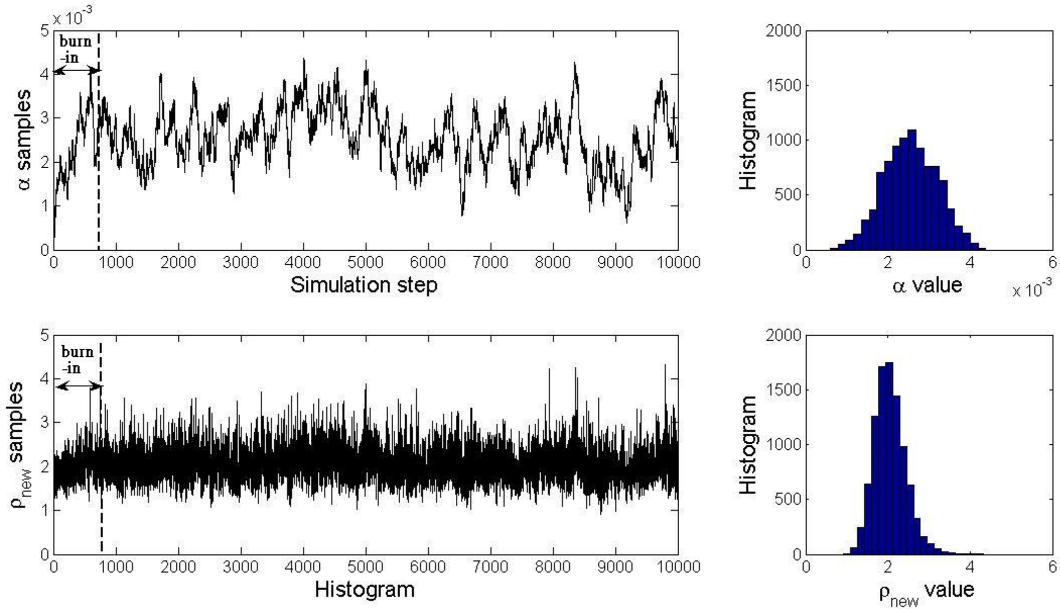

Fig.1 The Markov chain samples of α (upper plot) and ρ_{new} (lower plot) and the histograms

The samples from $f(\rho_{new}|C_{1:46})$, as shown in Fig..1, gives us the possible range of the model factor ρ learned from the data. From the histogram in the figure, it is clear that the model factor is mostly between 1 and 3. This means the actual capacity of an anchor in Taipei is roughly two times larger than the capacity predicted by Eqn. (3) because of the combined effect of the enlargement and model bias. Moreover, the mean value and c.o.v. of the samples of the model factor are 2.049 and 0.186, respectively.

Fig..2 shows the comparison between the actual capacities and the ones predicted by Eqn. (3) multiplied by 2.049, where the uncertain soil parameters are held at their nominal values and the decay parameter is held at 0.0025. It is clear that the predicted capacities unbiasedly reflect the actual ones, indicating that the analysis results are consistent.

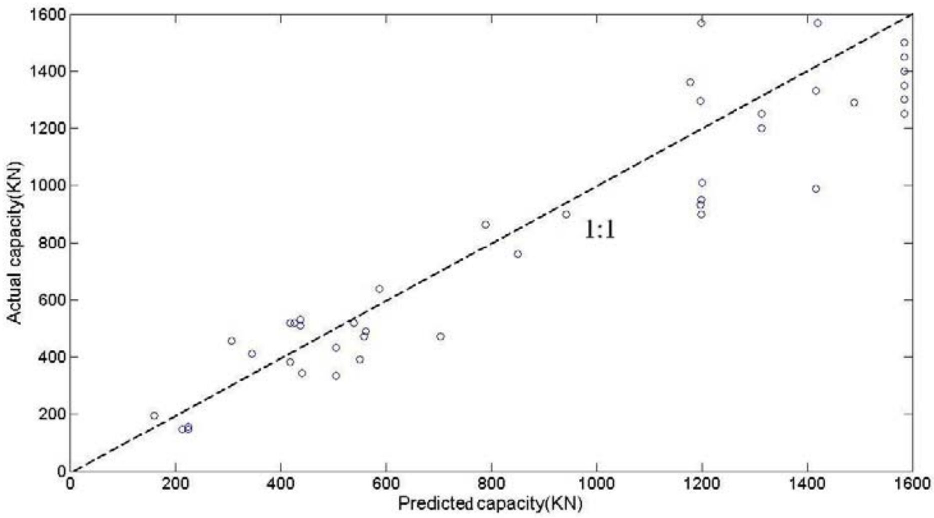

Fig.2 The actual v.s. predicted pullout capacities for the anchors in the database

6.2 Verification of the analysis results

In order to further verify the analysis results in a more rigorous manner, the following approach is taken. Remove the j-th anchor data from the database, i.e. the remaining data only contains $\{C_1,\ldots,C_{j-1},C_{j+1},\ldots,C_{46}\}$, denoted by $C_{1:46\backslash j}$. Then draw samples from $f(\rho_{new},\alpha|C_{1:46\backslash j})$, and denote the samples by $\{\rho_{new\backslash j}{}^{k},\alpha_{\backslash j}{}^{k}\}$: $k = 1,\ldots,N\}$. Also draw Y_j samples from their prior PDFs, denoted by $\{Y_j^k$: $k = 1,\ldots,N\}$. Conceptually, the samples $\{\rho_{new\backslash j}{}^{k}C^{\alpha\backslash jk}(Y_j^k)$: $k = 1,\ldots,N\}$ are the samples of the actual capacity of the j-th anchor C_j conditioning on the data $C_{1:46\backslash j}$, and their histogram can be regarded as the predicted PDF of C_j conditioning on the data $C_{1:46\backslash j}$. If the analysis framework is reasonable, the percentage ranking of the actual C_j among the predictive samples $\{\rho_{new\backslash j}C^{\alpha\backslash jk}(Y_j^k)$: $k = 1,\ldots,N\}$, defined as the ranking of the actual C_j among the samples divided by the total number of samples N, should be roughly uniformly distributed over [0,1].

The afore-mentioned percentage ranking is calculated for all anchors in the database, and Fig..3 shows the empirical cumulative distribution function of the 46 percentage rankings. The dashed line indicates the cumulative distribution for a uniform distribution. It is observed that the distribution of the 46 percentage rankings is roughly uniform. In fact, hypothesis testing based on Kolmogorov-Smirnov test concludes that the uniform-distribution hypothesis is not rejected at a significance level of 0.05. These results indicate that the analysis is consistent.

6.3 Relationship between resistance factor and target failure probability

The procedure introduced in a previous section is employed to estimate the relationship between resistance factor and target failure probability. The estimated relationship is not unique: it depends on the configuration of the anchor, the soil profiles, and the chosen capacity design model C^D. This non-uniqueness imposes a difficulty, but it can be solved by just selecting a representative set of anchor design scenarios in Taipei basin and estimating the η-P_F^* relationships for all scenarios to obtain the possible range of the relationship. For example, one can first determine the target failure probability P_F^* for anchor designs within Taipei basin, then the range of the required resistance factor can be found.

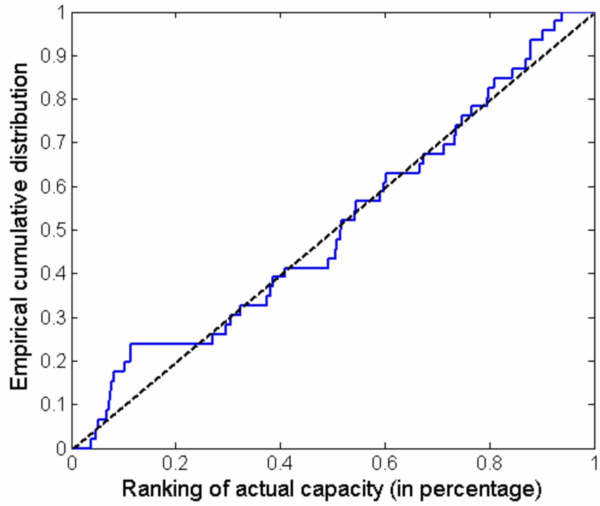

Fig.3 Empirical cumulative distribution of the 46 percentage rankings

In this paper, the sites of the 46 anchors in the database are taken as the representative set of soil profiles in the Taipei basin. Numerous scenarios of anchor designs are considered and the corresponding η-P_F^* relationships are computed. It is found that the η-P_F^* relationships strongly depend on the chosen design model C^D, the dominant soil types within the fixed end, and the length of the fixed end.

When the design model C^D is chosen to be C^n in Eq. (1), the η-P_F^* relationships will depend on the dominant soil types within the fixed end as well as the length of the fixed end. It is evident that if the soils within the fixed end are primarily sandy, the calibrated resistance factors are larger, while the resistance factors are smaller if the soils are mostly clayey. The dependency of the η-P_F^* relationship on the soil types is due to the fact that undrained shear strengths of clayey soils are more uncertain than the shear strength of sandy soils.

Moreover, the calibrated resistance factors are smaller for anchors with long fixed ends and are larger for those with short fixed ends. The dependency on the fixed anchor length is due to the fact that the C^n model does not take the decay behavior of developed shear resistance along the fixed end into account.

Therefore, the calibrated resistance factor will be smaller for long fixed end than for short fixed end.

When the C^n model is adopted for designs, the ranges of required resistance factors corresponding to various target failure probabilities, fixed anchor lengths, and soil types are compiled in Fig..4. Note that the required safety factors for target failure probability 10^{-2} and 10^{-3} rarely exceed 3, the safety factor required by most anchor codes. For target failure probability 10^{-2}, the required resistance factor is around 0.6 to 1.2 (safety factor is around 0.9 to 1.7), depending on the soil type and fixed-end length. For target failure probability 10^{-3}, the required resistance factor is around 0.4 to 1 (safety factor is around 1 to 2.5). The safety factor of three recommended by anchor codes corresponds to failure probability less than 10^{-4}, which is somewhat over-conservative for flush drilled anchors in Taipei. Note that this conclusion may not apply to different types of anchors nor to anchors outside Taipei.

One must be careful that the recommended resistance factors and safety factors in Fig..4 for fixed anchor length is among 30m-40m are extrapolation results because the longest fixed end in our database is 32m. So, cares must be taken when adopting the resistance factors and safety factors if the designed fixed end is longer than 32m.

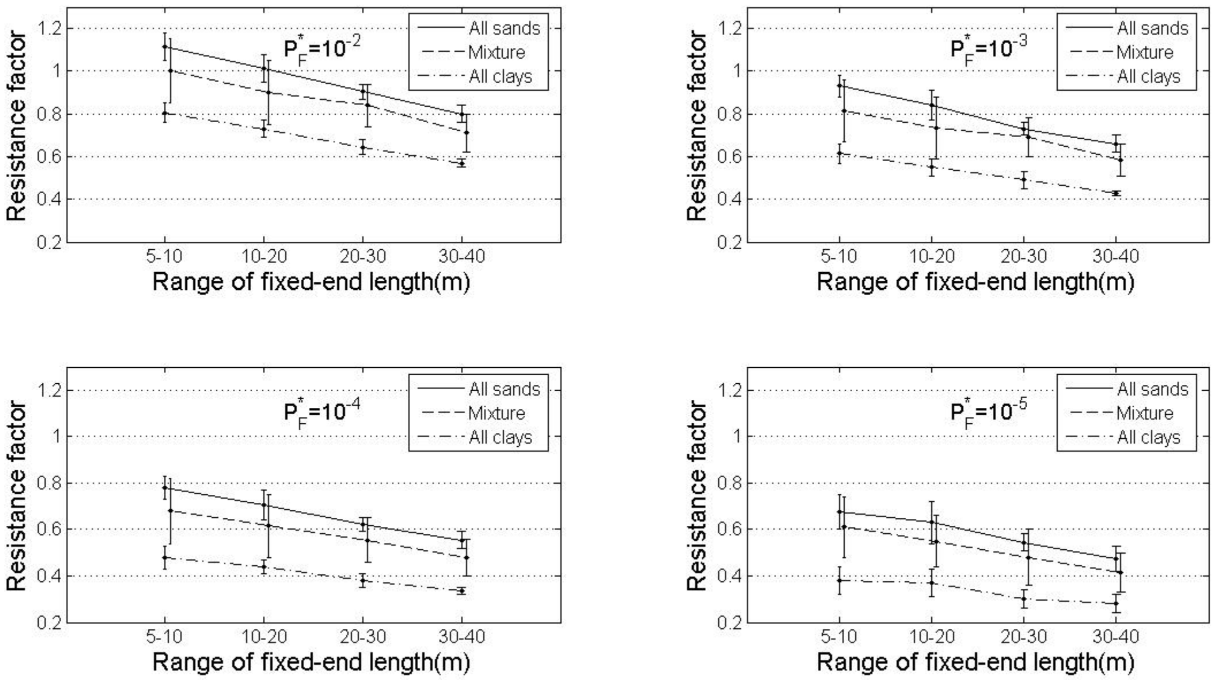

Fig.4 The recommended ranges of the resistance factors for different design scenarios and target failure probabilities based on the Taipei database when C^n is adopted

7 CONCLUSION

A database of 46 anchors in the Taipei basin is analyzed in this study to construct the relationship between the resistance factor and target failure probability of flush drilled anchors. It is found that the safety factor of three recommended by anchor codes is somewhat over-conservative for flush drilled anchors in Taipei. This conclusion may not apply to different types of anchors nor to anchors outside Taipei. Again, cares must be taken when adopting the above resistance and safety factors if the design fixed anchor length is longer than 32m.

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