Development of a real-time hybrid testing method in a centrifuge

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This paper presents a pioneering real-time hybrid testing method for geotechnical centrifuges. The method was used to investigate the behaviour of a jack-up leg reinstalled near an existing footprint, a problem that is highly non-linear, stress dependent and involves complex soil–structure interactions. By physically modelling only the footprint–leg interaction and numerically modelling the rest of the jack-up structure, the method enables a realistic account of all the parameters involved in the interaction, including the footprint geometry, soil heterogeneity and structural properties of the jack-up unit. The paper also details the three-degree-of-freedom actuators developed to model the interaction, which features controlled loads and motions along the vertical, horizontal and moment directions, and the real-time algorithm that bridges the physical and numerical models. The algorithm allows the stiffness on each axis to be varied. Testing performed at 1g and in the centrifuge, modelling jack-ups with different stiffness on the horizontal and moment axes, is presented to validate the apparatus and methodology. The hybrid apparatus and real-time testing method were found to produce much more realistic boundary constraints than previous fixed-system apparatuses, and this allowed the test results to be considerably more informative, accurate and useful.

1. Introduction

Real-time hybrid testing has seen increased popularity in testing complex structural systems but has seen very limited application in geotechnics. In real-time hybrid testing, a physical model of a critical element is combined with a numerical model of the remainder of the system. In this way, an accurate test on the element of interest can be physically established but with integrated behaviour of the entire system still accounted for.

Typically, actuators apply displacements to a physical model, and the physical test interacts with a numerical model in real time by means of a feedback control loop. In this way, any rate-dependent components within the system are tested. The loop starts by applying external excitation to the numerical model. The response at the common point (CP) between the

physical model and the numerical model is evaluated using a time-stepping scheme. The displacements at the CP are imposed on the physical model by the actuator in real time (i.e. over the same period of time that the numerical model is stepping). The restoring forces are measured directly from the physical model at the end of the time step. These forces are fed back and imposed on the CP of the numerical model. The response of the numerical model to external load and feedback forces is evaluated for the end of the next time step. Looping continues until the external excitation ceases.

Early developments in hybrid testing were not in real time but used at an expanded time scale. For instance, in the testing undertaken by Hakuno *et al.* (1969), an electromagnetic actuator was used to load the physical model and an analogue

computer analysed the numerical model. These were not fast enough to model the seismic response of a structure in real time. It was not until the introduction of digital computers and more powerful actuators that hybrid testing that interacted on a common time axis was first achieved by Nakashima *et al.* (1992). Recent advancements in real-time hybrid testing have concentrated on understanding experimental error, improving delay compensation algorithms and substructuring techniques. However, applications of the method remain in modelling of the dynamic behaviour of complex structural and mechanical systems, such as bridges, buildings and aircraft (Ayari, 2008; Blakeborough *et al.*, 2001; Van der Auweraer *et al.*, 2008; Wagg *et al.*, 2008).

The use of hybrid testing within geotechnics remains rare. The first application of hybrid testing in geotechnical engineering occurred in 1975, wherein the dynamic performance of a pile foundation was investigated (Mochizuki, 1975). Subsequent development concentrated on evaluating the potential for one-dimensional liquefaction. In such studies, the liquefiable layer was physically modelled by a soil element test, whereas a numerical model was applied to the remaining layers (Adachi *et al.*, 1998; Fujii *et al.*, 2000; Katada and Hakuno, 1982; Kusakabe and Morio, 1995; Sento *et al.*, 2002). In these studies, the soil deformation caused by liquefaction both during and after an earthquake was evaluated. Various researchers have concluded that hybrid testing provides an accurate prediction of the dynamic soil response without the use of idealised constitutive relations (e.g. Kazama *et al.*, 2004; Kwon *et al.*, 2007; Takahashi *et al.*, 2003).

The application of hybrid testing to the study of soil–structure interaction is even more limited. Five studies are known to the authors. Mochizuki (1975) physically tested the soil and the pile, with the rest of the building structure modelled numerically as lumped masses. Kobayashi *et al.* (2002) also physically modelled the pier and pile foundation in the laminar box of a shake table, with the rest of the bridge structure modelled numerically. In other studies, it was actually the soil and foundation system that were modelled numerically by means of a finite element or lumped mass model (Ohtomo *et al.*, 2008; Spencer *et al.*, 2006; Wang *et al.*, 2009). To the authors' knowledge, there has been no use of real-time hybrid testing within a geotechnical centrifuge.

The purpose of this paper is to describe the underlying principles and the design criteria required to apply real-time hybrid testing within a geotechnical centrifuge. These principles and criteria were used to design a new vertical–horizontal rotation actuator system (named the VHM actuator) to be used within a beam centrifuge to investigate the behaviour of a jack-up platform foundation during penetration nearby a footprint. Details of its design, validation and application

are provided. The control of the hybrid tests was found to be critical and the real-time control system developed is presented in detail. Results of experimental validation of the system at both 1g and in the centrifuge are provided to illustrate the effectiveness of the real-time hybrid testing methodology.

2. Motivation

The real-time hybrid testing method has been developed to test the reinstallation of a mobile jack-up platform next to existing seabed footprints.

Jack-ups are self-elevating mobile units operating in offshore oil and gas fields that are situated in water depths of up to approximately 120 m. A typical jack-up unit consists of a floatable hull platform and three independent retractable truss-work legs, each resting on a spudcan footing. The spudcans are typically between 10 and 20 m in diameter (e.g. Menzies and Roper, 2008; Zhang *et al.*, 2011). During the removal of a jack-up unit from a site, the extraction of spudcans leaves significant seabed depressions. In the offshore industry, these are referred to as footprints. Jack-up units often have to return to the same site and be installed near or into existing footprints (Figure 1). This is a problematic operation because the spudcan located near the footprint is subjected to eccentric and/or inclined loading conditions. This can lead to structural failures within the jack-up legs. Furthermore, movements can occur in the spudcan and jack-up legs, with potential risks that the jack-up hull may hit the nearby fixed platform.

The physical testing of this reinstallation process is extremely complex, with interacting features of soil and structural mechanics. The system response of the jack-up unit could be studied experimentally by modelling a full three-legged jack-up model, as was adopted to measure the combined loading of a jack-up subjected to pushover (storm) forces in a centrifuge by Dean *et al.* (1996) and Bienen *et al.* (2006, 2009), and on the laboratory floor by Vlahos *et al.* (2005, 2008, 2011) and Cassidy *et al.* (2010). However, in these cases, the jack-up remained in one configuration with the entire model initially loaded vertically (installation) and then laterally (storm pushover). Installation into a footprint is more complex, with usually only one spudcan installed at a time. One common industrial practice is to orient the jack-up unit so that only one leg is located near the footprint (Figure 1). The other two legs are installed first to increase the rigidity of the system and help restrain movement. Moving legs of a full three-legged jack-up unit independently is extremely difficult to model physically, particularly to automate at high gravitational levels in the confined space of a geotechnical centrifuge. Furthermore, even if it was possible, a full three-legged jack-up model requires a relatively large testing space and restricts the modelling to one structural configuration (both limiting the number and scope of any parametric study).

Figure 1. Scenario of one jack-up leg reinstalled near a footprint (contour plot of footprint after Gan, 2009)

Due to these difficulties, all of the existing testing of the reinstallation of jack-ups next to footprints has been limited to testing of a single spudcan and leg. The effect of the surrounding jack-up structure has been effectively ignored. Conditions have been either fully fixed, with a rigid connection between the model leg and the vertically displacing actuator (Cassidy *et al.*, 2009; Gan *et al.*, 2012; Kong *et al.*, 2013; Stewart and Finnie, 2001), or fixed vertically and rotational but free-to-slide laterally, with a roller connection adopted in the horizontal direction (Gaudin *et al.*, 2007). As the former assumption ignores the finite structural flexibility of the jack-up unit, it precludes any measurement of jack-up movement during reinstallation tests and the measured loads therefore represent an overestimation of typical behaviour. On the other hand, the slider has no lateral flexibility (say from the other two pinned in legs, Figure 1) and is over-estimating movements and under-estimating loads. The use of a mechanical connection piece to represent the leg–hull connection and overall jack-up structure is also limited as the design for multiple degrees of freedom is complex and it is not mechanically feasible to change the stiffness due to the non-linear behaviour during reinstallation.

3. Real-time hybrid testing of jack-up platform reinstallation

The soil–structure interaction associated with the reinstallation of a jack-up leg near a footprint is a complex problem that can be simplified by using real-time hybrid testing. The critical component is the interaction of the leg/spudcan with the footprint. The interaction is highly non-linear and time dependent and is difficult to model numerically. Consequently, real-time testing was used so that the rate-dependent response and drainage conditions of the soil were correctly modelled and were consistent with the jack-up installation speed used offshore.

The remainder of the jack-up unit was numerically modelled and described by a load–displacement relationship. A real-time control algorithm was constructed so that the physical model interacted with the numerical model at a CP located along the jack-up leg, as illustrated in Figure 2. The test was initiated with a pure vertical displacement of the CP (simulating the start of the leg installation). Following that, and in each subsequent loop, the loading (combining vertical V , horizontal H and moment M loads as described later in the paper) at the CP was derived through measurements in the physical jack-up model. These measurements were inputted to the numerical model, which in turn calculated the vertical and horizontal displacements and rotations (v , h and θ) to be imposed on the physical jack-up leg during the next step.

4. Physical model of the jack-up unit

4.1 Jack-up model

The physical model consists of a model jack-up leg and spudcan foundation. A prototype jack-up unit that was 94 m tall was considered, the properties of which are shown in Table 1 and Figure 3. The CP that defined the junction between the physical and the numerical model was located at 23.5 m above the spudcan (235 mm in model dimensions). The spudcan was modelled as a flat-base footing 6 m in diameter and 0.7 m deep (solid aluminium model of 60 mm diameter and 7 mm thick). The flat-base footings were used to simplify the experiments by eliminating the variables related to the spudcan geometry, including the apex angle, central spigot and cone angle. The model leg and spudcan are shown in Figure 4.

A lower leg flexural rigidity (EI) than observed in field jack-ups was purposely used. This is because the diameter of

Figure 2. Concept of real-time hybrid testing in jack-up reinstallation near an existing footprint

Dimension	Model	Scaling factor	Prototype
Leg length, L	940 mm	N	94 m
Leg spacing, W	600 mm	N	60 m
Height to CP	235 mm	N	23.5 m
Second moment of inertia of leg, I	$6.5 \times 10^{-10} \text{ m}^4$	N^4	0.065 m^4
Cross-sectional area of leg, A	$2.5 \times 10^{-5} \text{ m}^2$	N^2	0.25 m^2
Diameter of spudcan, D	60 mm	N	6 m

For the scaled model at **1g**, a 1:100 numerical model was established

Table 1. Model and prototype dimensions of jack-up unit models

the spudcan was also smaller. With the consideration on the maximum number of test to be conducted within a soil sample and the correctness of scaled spudcan size, the diameter of spudcan adopted in this model was 6 m, which is around three times smaller than the typical dimension of a spudcan used in practice. The moment acting on the testing spudcan would therefore be 27 times smaller than on an 18 m spudcan (with moment

proportional to the cubic of footing diameter). Since the main purpose of this study was to investigate the load–displacement responses during jack-up reinstallation, the leg flexural rigidity was also scaled down proportionally to maintain the bending properties. The leg flexural rigidity adopted was of a similar reduced ratio, with $EI = 1.3 \times 10^{10} \text{ N m}^2$, approximately 5–100 times lower than the range of values reported in other publications (e.g. $EI = 7.12 \times 10^{10} \text{ N m}^2$ and $EI = 1.44 \times 10^{12} \text{ N m}^2$ in Murff *et al.* (1991) and Vlahos (2004), respectively).

During penetration near an existing footprint, the jack-up leg experiences combined vertical V , horizontal H and moment M loads and motions. To accommodate and monitor these loads and motions, the jack-up leg is connected to a purposely designed VHM actuator, and instrumented with axial gauges and bending gauges, the readings of which are converted into loads H_{CB} , V_{CP} and M_{CP} at the CP (left-hand side of Figure 2). The sign conventions and definitions of terminologies adopted in the control algorithm, as also relative to the footprint, are presented in Figure 4. A positive horizontal force at the load reference point (LRP) indicates that the footing was resisting a movement towards the footprint and a positive moment indicates that the footing was resisting a counter-clockwise rotation away from the footprint.

Figure 3. Numerical model of the simplified jack-up unit assumed in the experiments

4.2 Design of the VHM actuator

An actuator with three degrees of freedom was required to allow the jack-up model leg and spudcan to move independently in the vertical, horizontal and rotational direction. As the existing two-axis linear actuator at University of Western Australia (UWA) only provides horizontal and vertical movement, an additional angular actuator that can be attached to the existing linear actuator was developed.

The limited space in the existing linear actuator unit precluded the use of a pair of actuators to generate the angular movement (such as used in the design of three-degrees-of-freedom VHM actuators for beam centrifuge by Dean *et al.* (1997) and Punrattansin *et al.* (2003) and more recently in the UWA drum by Zhang *et al.* (2013, 2014a, 2014b, 2014c)). Therefore, an innovative pulley system was developed for use in this study. As shown in Figures 5 and 6, the rotational motion of the motor was converted to angular motion at a pivot point through a pulley system. The main advantage of this system is that there is only one pivot point in the system and the single motor module is aligned with the line of action. This generates precise angular movement from a relatively slim configuration.

Figure 7 shows the angular actuator's key components: the motor module, the cable and pulley, the pivot point and the

actuator box. The new angular actuator has outer dimensions of 100 mm × 79 mm × 411 mm (width × thickness × height) and a weight of 2.57 kg. After accommodating the angular actuator, the stroke in the linear actuators was reduced to 180 mm (vertical) and 70 mm (horizontal). A detailed discussion of the design and fabrication of the key components is presented in Appendix 1, as well as in Kong (2012).

Table 2 shows the specification of the VHM actuator. The angular actuator was designed to have a maximum angular stroke of 3° in both the clockwise and anti-clockwise directions. This limit was set because tilting of a jack-up unit to more than 2° at the platform level would not normally be allowed for offshore (due to possible damage of the leg-hull connection, as advised by Purwana, Personal Communication (2010)). In this study, it was conservatively assumed that all tilting at the platform level resulted from angular movement of the jack-up leg installed near the footprint.

Another major advantage of the VHM actuator is that its centre of rotation can be prescribed anywhere along the vertical axis. When the angular actuator rotates around the pivot point ($\Delta\theta$), the linear actuators can also move the pivot point in vertical and horizontal directions (Δx and Δy), offsetting the linear movement so that there is pure rotational movement at any prescribed point along the vertical axis.

Figure 4. Model jack-up leg and footing used in real-time hybrid testing (all units in mm)

4.3 Numerical model

While the section of the jack-up leg located near the footprint was modelled physically, the remainder of the jack-up unit was simplified and modelled numerically (right-hand side of Figure 2). The jack-up leg and the hull were modelled as beam elements, and the leg–hull connection was simplified and modelled as a fully fixed node. The two pre-embedded footings were modelled as a series of linear springs, with stiffness determined according to the Boussinesq elastic solutions set out in the Society of Naval Architects and Marine Engineers (SNAME) industry guidelines (SNAME, 2002). The soil properties and foundation stiffness adopted in the analyses are shown in Table 3.

To reduce the complexity of the initial testing programme, the numerical model was assumed to be linear elastic. The

numerical model could therefore be reduced to a simple flexibility (inverse stiffness) matrix at the CP. The governing load–displacement relationship in the general form is presented in Figure 8. The matrix was evaluated by simply applying unit forces at the CP into the structural model, with the resultant displacement and angular movement at the CP divided by the unit load to determine the flexibility. Structural analysis program SPACE-GASS was used for this numerical calculation (Integrated Technical Software Pty Ltd, 2010). Further details, including example calculations, are available in Kong (2012). Jack-up units with different flexibilities could therefore be easily modelled by introducing different flexibility matrices of the following form

$$1. \quad \begin{pmatrix} \Delta h \\ \Delta v \\ \Delta \theta \end{pmatrix} = \begin{pmatrix} C_{Hh} & 0 & C_{Mh} \\ C_{Hv} & 0 & C_{Mh} \\ C_{H\theta} & 0 & C_{Mh} \end{pmatrix} \times \begin{pmatrix} \Delta H_{cp} \\ 0 \\ \Delta M_{cp} \end{pmatrix} + \begin{pmatrix} 0 \\ \Delta v_{step} \\ 0 \end{pmatrix}$$

Figure 5. The two-axis linear actuator with the additional rotational unit

The following assumptions were made to further simplify the problem

- the vertical load–displacement cross-coupling terms C_{Vh} , C_{Vv} and $C_{V\theta}$ were eliminated, and the installation of the jack-up leg was modelled by the application of a constant vertical displacement step Δv_{step}
- the current real-time hybrid testing method considered constant jack-up flexibility throughout reinstallation, so the non-linear jack-up load–displacement responses due to the $P - \Delta$ term were ignored
- in theory, as the physical model leg penetrates into the soil, the length of the jack-up leg in the numerical model must increase by the same length. This change in jack-up leg length (and therefore jack-up flexibility) was not considered in the numerical model.

These assumptions were necessary for the development of a relatively simple control algorithm. However, the test results still provided fundamental information regarding the effect of the jack-up's structural properties on the reinstallation response. However, the prototype being simulated still differs from an offshore installation, as the numerical model did not include any plasticity in the spudcan response, any redistribution of loads, including due to a change of the jack-up's centre of gravity, and structural dynamics effects were ignored. However, the current control algorithm could be modified to

update jack-up flexibility in real time in a future study. This would require a dynamic structural analysis code to update continually the flexibility at the CP.

4.4 Real-time control algorithm

In the real-time hybrid test, the VHM actuator applied displacements v , h and θ to the physical jack-up leg model, and the physical test interacted with a numerical model in real time by means of a feedback control loop, allowing the complete jack-up unit to be tested. Figure 9 outlines the system and the communication of forces and displacements to the hardware, which controls the flow loop outlined in Figure 2 and allows the hybrid tests to be run. The control loop had the following features

- the loads acting on the physical jack-up model were measured by strain gauges (Figure 4)
- the load readings were transferred to the DigiDAQ Box, which is an analogue-to-digital converter system (Gaudin *et al.*, 2009)
- the load readings were then transferred to the flight computer mounted on the central platform of the centrifuge (flight PC) through an ethernet connection
- in the flight PC, the load reading was streamed to two programmes
 - the data logging system DigiDAQ
 - the real-time control algorithm and numerical model programme
- the numerical model programme computed the demand displacement and angular movement (demand)
- the demand was fed into DigiDAQ for logging, transferred to the modified main actuator control software PACS (packaged actuator control system; refer to De Catania *et al.* (2010) for details) and subsequently transferred to the Servo Controller (National Instrument NI7344-PCI)
- the demand (in the form of an analogue signal) was amplified through a linear power amplifier
- the VHM actuator moved with an inner control loop to achieve accurately the demands v , h and θ .

The numerical jack-up model was written in LabVIEW and incorporated into the real-time control loop, minimising any potential decay in communication. The flight computer is continuously connected to the remote control PC in the control room. High-speed communication between the two PCs is achieved with optical fibre cables and slip rings, which are housed within the main axis of the beam centrifuge. The test was controlled using the remote control PC, and the data were displayed in real time for monitoring. The maximum operating frequency of this control system is 100 Hz.

The control loop starts by applying a constant vertical displacement Δv_{step} , simulating the installation of the jack-up leg at a

Figure 6. Details of the additional rotational unit
(dimensions in mm)

constant vertical rate (step 1 in Figure 10). The loads acting on the physical jack-up model are measured by the strain gauges (VHM_i in step 2) and fed into the numerical model. The measured load is first converted to loads at the CP between the physical model and the numerical model using a time-stepping scheme (VHM_{CPi} in step 3). The block average value, VHM_{avei} , is evaluated to filter the noise in the measured data (step 4). This block average is then compared with the previous measured maximum load to determine the location of the physical jack-up leg during a loading or unloading condition (steps 5, 6a, 6b). The demand displacement and angular movement are determined by solving the load-displacement relationship (step 7), and the demands are then imposed on the physical model by the actuator in real time (i.e. over the same period of time that the numerical model is stepping). At the end of the time step, the loads and the actuator position are measured and fed back into the numerical model, thus closing the loop (step 9a). This loop is repeated until the physical

model jack-up leg reaches the designated maximum penetration depth (step 9b).

The load readings from the bending gauges introduced considerable high-frequency noise content to the signal being fed back into the numerical model. With the load-displacement relationship, this high-frequency loading can result in high-frequency cyclic motion. This becomes of particular concern as the soil sample may be undesirably remoulded at relatively shallow penetration where loading is small and the movement was dominated by the noise. This problem, and a solution of mitigation by using a filtering system, has been reported by, among others, Wagg *et al.* (2008). In this study, the average loadings are evaluated from block of load readings of sample size F , where F is defined here as the number of load reading per block. The demand displacement and angular movement are then calculated and implemented over the next block time step f , where f is defined as the time between the first

Figure 7. Key components of angular actuator's pulley system

and last readings of the block of load reading ($f = F - 1$). The high-frequency noise can effectively be filtered from the system.

There are two prime sources of delay in the system, one from the time-step delay in the feedback loop and the other from the inherent dynamics of the actuators. Effort has been made to reduce the second source of delay. The loading required to drive the scaled physical model is relatively small. Therefore, the fast response servo-motor actuator was adopted instead of

the hydraulic actuator. The proportional–integral–derivative control (refer to Goodwin *et al.*, 2000) of each VHM actuator was tuned according to National Instruments (2009) to minimise delay and generate as accurate a motion as possible. The filter system introduces additional delay into the loop. The load reading was stored until obtaining F number of load readings to calculate the demand displacement and angular movement for the next block time step f . The block sample size F was, therefore, carefully selected balancing the delay and the noise level in the system.

	Vertical	Horizontal	Angular
Maximum g	250 g^a	250 g^a	100 g^b
Maximum stroke	180 mm	70 mm	$\pm 3^\circ$
Maximum speed	3 mm/s	30 mm/s	3.96°/s
Maximum force/moment	$V_{\max} = 6500$ N	$H_{\max} = 2000$ N	$M_{\max} = 180$ N m at pivot point
Movement precision	2.84×10^{-5} mm/count	2.84×10^{-5} mm/count	1.49×10^{-5} °/count

^aThe original two-axis linear actuator is designed to operate up to 250 g

^bThe angular actuator is designed to operate up to 100 g

Table 2. General specifications for the VHM actuator

Parameters	1g model tests	100g beam centrifuge tests
s_u at $z/D = 0.25$	5.0 kPa	4.7 kPa
OCR at $z/D = 0.25$	3000	4.8
K_v	111.9 kN/m	52583.3 kN/m
K_h	86.3 kN/m	40542.5 kN/m
K_{rot}	0.07 kN m/rad	351165.0 kN m/rad

- The spring stiffness of the embedded footings is defined by the vertical stiffness (K_v), the horizontal stiffness (K_h) and the rotational stiffness (K_{rot}) and is based on Boussinesq elastic solutions. The depth factor, defined by k_{dv} , k_{dh} and k_{dro} , was applied according to SNAME (2002) to incorporate the footing embedment depth. The footing embedment depth ratio z/D (depth of footing over diameter of footing) was assumed to be 0.25 and no backflow was assumed. Therefore, values of $k_{dv} = 1.15$, $k_{dh} = 1.33$ and $k_{dro} = 1.28$ were used. This is a conservative assumption as the embedment depth of the two footings varies greatly depending on the site condition and the embedment depth can be up to $2D$ in practice.
- The shear modulus of the soil was estimated to be $G = I_s s_u = (600/OCR^{0.25}) s_u$, based on Cassidy *et al.* (2004) and the SNAME guidelines.
- A Poisson ratio of 0.5 was assumed to represent undrained clays.
- The s_u at $z/D = 0.25$ is the upper bound values estimated from the t -bar test results shown in Figure 11.
- The sample consolidated at 1g was under a pressure of 300 kPa, but only preconsolidated at 47 kPa before spinning in the centrifuge.

Table 3. Soil properties and foundation stiffness values (for 'FSNAME')

5. Examples of results

A series of 16 tests were performed, 10 tests at 1g and six in the centrifuge. The 1g tests used exactly the same set-up, but just without accelerating the centrifuge. All results are detailed in Kong (2012) and eight tests are presented hereafter as examples (with details in Table 4).

5.1 Soil sample

In both the 1g and centrifuge tests, an over-consolidated soil profile was targeted as it represents a profile in which deeper footprints can be created (due to higher surface strengths in comparison to normally consolidated soil). Kaolin clay soil that is used extensively at UWA was adopted in this test (Acosta-Martínez, Personal Communication (2008); Stewart, 1992). The Kaolin was mixed with de-aired water to form a slurry with a water content of 120% and consolidated in a hydraulic press. The maximum consolidation pressure in the

Figure 8. Linear-elastic numerical model of a jack-up unit

press was 300 kPa for the 1g tests and 47 kPa for the centrifuge samples. After removal from the consolidation press, the soil sample was left to swell for 7 d before testing at 1g. In the centrifuge, the sample was immediately spun at 120g for 2 d (before being ramped down to 100g for testing).

The undrained shear strength s_u of the soil samples was determined using a t -bar penetrometer developed by Stewart and Randolph (1994). The strength profiles of the soil samples adopted in the 1g and centrifuge test are shown in Figure 11. The magnitude of the undrained shear strength within the critical depth, which is within the footprint toe level ($z/D = 0.33$), was comparable between the 1g and centrifuge samples.

5.2 Creation of footprint and testing procedure

In the 1g tests, a footprint was cut from the clay sample using a v-shaped blade that was centrally rotated to remove soil and to form a cavity of conical shape 20 mm deep ($z_F/D = 0.33$), with a radius of 60 mm ($1D$). This caused minimal disturbance of the soil in the vicinity of the idealised footprint cavity, as reported by Kong (2012) and Kong *et al.* (2013).

A more realistic offshore footprint was created in-flight during the beam centrifuge tests. The footprint was created by penetrating the jack-up model spudcan vertically to a final depth $z_{FC} = 70$ mm ($z_{FC}/D = 1.16$) into the soil sample. The model was held at this depth for 10 min to simulate an operation time of 70 d at prototype scale. The physical jack-up model was then extracted from the soil, leaving a seabed depression surrounded by highly disturbed soil (and of geometric shape similar to the idealised cavity).

The full jack-up model prescribed by the real-time hybrid testing method was then reinstalled with a $1.0D$ offset from the footprint's centre (see Figures 4 and 12 for definition). In the centrifuge, this was performed 20 min after complete extraction of the spudcan that made the footprint, simulating an elapsed time of 140 d. The reinstallations were conducted to a

Figure 9. Outline of real-time hybrid testing with a geotechnical centrifuge for the modelling of jack-up reinstallation near existing footprints

maximum depth $z_{\max}$ of 100 mm ($z_{\max}/D = 1.67$), which was $0.5D$ below z_{FC} . Further details regarding the effects of operation and elapsed time on the footprint's soil strength can be found in Gan (2009) and Gan *et al.* (2012).

5.3 Example 1g tests: effect of including horizontal flexibility

The first phase of the 1g model tests considered the jack-up reinstallation response with a partially restrained connection at the model leg and actuator. The model jack-up had finite horizontal flexibility C_{Hh} and was allowed to slide according to the prescribed horizontal flexibility with the measured load.

This is equivalent to a horizontal spring connection between the model jack-up leg and actuator. The tests are labelled S-H-1 and S-H-2, with values of horizontal flexibility C_{Hh} provided in Table 4. Test S-R-1 was a rigid installation test. This refers to the reinstallation of the model jack-up leg rigidly connected to the actuator with no flexibility at the CP (similar to all tests previously performed by Cassidy *et al.*, 2009; Gan, 2009; Kong *et al.*, 2013; Stewart and Finnie, 2001).

Figure 13 shows the results of the three tests performed with different C_{Hh} , S-H-1, S-H-2 and S-R-1, in terms of evolution of vertical and horizontal loads with penetration, horizontal

Figure 10. Flowchart of combined numerical model and real-time control algorithm

displacement with penetration, moments at the load reference point and at the CP with penetration and rotation of the leg with penetration.

When the footing first came into contact with the soil, the soil reaction pushed the footing towards the footprint centre, and, with the horizontal flexibility considered in tests S-H-1 and S-H-2, the footing started to move horizontally towards the footprint centre. It continued to slide towards the footprint centre until the bottom of the footprint z_F was passed. With this horizontal movement, the soil reaction was released, after which H dropped significantly and became slightly negative.

The maximum horizontal displacements (h_{max}) increased from 0 to 3.3 mm ($h_{max}/D = 0.055$) to 9.5 mm ($h_{max}/D = 0.158$) as the horizontal flexibility increased between tests S-R-1, S-H-1 and S-H-2, respectively. The increase in h_{max} was proportional to the increase in horizontal flexibility (by a ratio of 3).

However, the tests confirmed that with horizontal movements there is a decrease in the horizontal loads (Figure 13). For instance, the maximum horizontal load on the footing (H_{max}) decreased by 22 and 14% in S-H-1 and S-H-2, respectively, compared with the rigid reinstallation test S-R-1 (peak values available in Table 5). The trends of change in horizontal load

Test name	1g or 100g	H flexibility	M flexibility	Flexibility matrix at CP, in model dimensions as Equation 1 was applied in experiment (units in mm/N, mm/N m, deg/N or deg/N m)	Assumption of foundation stiffness (see notes (b and c) and Table 2)
S-R-1	1g	—	—	$\begin{pmatrix} C_{Hh} & 0 & C_{Mh} \\ C_{Hv} & 0 & C_{Mv} \\ C_{H\theta} & 0 & C_{M\theta} \end{pmatrix} = \begin{pmatrix} 0 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 0 \end{pmatrix}$	Rigid
S-H-1	1g	✓	—	$\begin{pmatrix} C_{Hh} & 0 & C_{Mh} \\ C_{Hv} & 0 & C_{Mv} \\ C_{H\theta} & 0 & C_{M\theta} \end{pmatrix} = \begin{pmatrix} 1 \cdot 49 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 0 \end{pmatrix}$	F_{infinite}
S-H-2	1g	✓	—	$\begin{pmatrix} C_{Hh} & 0 & C_{Mh} \\ C_{Hv} & 0 & C_{Mv} \\ C_{H\theta} & 0 & C_{M\theta} \end{pmatrix} = \begin{pmatrix} 5 \cdot 49 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 0 \end{pmatrix}$	$F_{0.1 \times \text{SNAME}}$
S-R-2	1g	—	—	$\begin{pmatrix} C_{Hh} & 0 & C_{Mh} \\ C_{Hv} & 0 & C_{Mv} \\ C_{H\theta} & 0 & C_{M\theta} \end{pmatrix} = \begin{pmatrix} 0 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 0 \end{pmatrix}$	Rigid
S-M-1	1g	—	✓	$\begin{pmatrix} C_{Hh} & 0 & C_{Mh} \\ C_{Hv} & 0 & C_{Mv} \\ C_{H\theta} & 0 & C_{M\theta} \end{pmatrix} = \begin{pmatrix} 0 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 0 \cdot 532 \end{pmatrix}$	F_{infinite}
S-M-2	1g	—	✓	$\begin{pmatrix} C_{Hh} & 0 & C_{Mh} \\ C_{Hv} & 0 & C_{Mv} \\ C_{H\theta} & 0 & C_{M\theta} \end{pmatrix} = \begin{pmatrix} 0 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 4 \cdot 620 \end{pmatrix}$	$F_{0.1 \times \text{SNAME}}$
B-R-2	100g	—	—	$\begin{pmatrix} C_{Hh} & 0 & C_{Mh} \\ C_{Hv} & 0 & C_{Mv} \\ C_{H\theta} & 0 & C_{M\theta} \end{pmatrix} = \begin{pmatrix} 0 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 0 \end{pmatrix}$	Rigid
B-F-1	100g	✓	✓	$\begin{pmatrix} C_{Hh} & 0 & C_{Mh} \\ C_{Hv} & 0 & C_{Mv} \\ C_{H\theta} & 0 & C_{M\theta} \end{pmatrix} = \begin{pmatrix} 2 \cdot 280 & 0 & -0 \cdot 413 \\ 1 \cdot 250 & 0 & 9 \cdot 340 \\ -0 \cdot 024 & 0 & 1 \cdot 350 \end{pmatrix}$	$F_{0.1 \times \text{SNAME}}$

- (a) The nomenclature of the tests is in the form of 'test type-flexibility matrix type-test number'. For the first letter, 'S' stands for 1g-scaled model test and 'B' for a beam centrifuge test at 100g. For the different flexibility type adopted, the nomenclature is as follows: 'R' for zero flexibility as the model jack-up leg was fully fixed to the actuator, 'H' for horizontal flexibility only, 'M' for rotational flexibility only and 'F' a full flexibility matrix. The test number refers to the number of similar tests conducted in series.
- (b) A reinstallation test was conducted with the model leg fully fixed to the actuator connection and the model jack-up leg could only move vertically into the soil (tests S-R-1 and B-R-2). This is referred to as rigid reinstallation in subsequent discussions. This rigidity is different from the foundation rigidity defined by F_{infinite} . F_{infinite} is an assumption of the numerical models having two pre-embedded footings rigidly connected to the ground. The reinstalling leg could still slide or rotate during reinstallation because the jack-up itself has finite flexibility.
- (c) $F_{0.1 \times \text{SNAME}}$ is an assumption of the numerical models having two footings with stiffness calculated at 10% of the stiffness matrix calculated with the SNAME guidelines, with the 100% values given in Table 2.
- (d) This table is a subset of the ten 1g tests and six 100g tests reported in Kong (2012).

Table 4. Flexibility matrix at the CP for the tests presented

with penetration depth in S-H-1 and S-H-2 were similar and exhibited only an 8% difference in H_{max} values. Up until the peak M_{LRP} value the moment was insensitive to changes in horizontal flexibility, with the (M_{LRP}) values obtained for the three tests only showing approximately 3% difference (Table 5).

Contrary to the rigid reinstallation case, the values of horizontal load in S-H-1 and SH-2 did not peak at $z/D = 0.2$

as penetration continued. Instead, the values peaked at approximately $z/D = 0.3$, after which they remained nearly constant until penetration went below $z/D = 0.6$. Rather than showing an abrupt peak in M_{LRP} , as was the case for the rigid test, the moment at the footing took the penetration up to $z/D = 0.6$ to reduce. The ability for the spudcan to displace horizontally seems to maintain the forces for a longer depth of penetration.

Figure 11. Undrained shear strength profiles of soil samples

5.4 Example 1g tests: effect of including rotational flexibility

Three jack-up reinstallation tests were conducted at 1g to investigate the effect of including rotational flexibility in isolation. Finite rotational flexibility was prescribed so that the model leg could rotate about the CP of the jack-up model (see tests S-M-1 and S-M-2 in Table 4). This is equivalent to a hinge connection of finite stiffness. Figure 14 shows the effects of including rotational flexibility during reinstallation. In these tests, rotation at the CP also induced an equivalent horizontal displacement at the footing's LRP.

Initially during these reinstallation tests with rotational flexibility, the soil reaction simultaneously rotated and pushed the footing towards the footprint centre. At the CP, the negative moment induced by the horizontal load was larger than the positive moment, the LRP. Thus, the moment at the CP was negative, and the footing rotated towards the footprint centre. The soil load was then released with the rotation. Both moments were therefore reduced. The horizontal load was also reduced as the footing rotated into the footprint. This was because there was equivalent horizontal displacement at the footing level as the jack-up leg rotated. Nonetheless, the release of the soil reaction was insufficient to keep the soil from bending the footing towards the footprint centre, and the M_{CP} remained negative.

As penetration continued, the trends in horizontal load and moment at the CP for tests S-M-1 and S-M-2 began to significantly differ. In test S-M-1, the footing rotated from

Figure 12. Schematic diagram of footprint creation and jack-up reinstallation with terminology definitions

touchdown level such that the angle of rotation increased in a linear manner until reaching maximum M_{CP} near the footprint toe level ($z/D \approx 0.33$). The moment at the CP decreased sharply with further penetration.

In summary, when rotational flexibility was included the footing rotated towards the footprint centre. The maximum angle of rotation (θ_{max}) in S-M-1 and S-M-2 was 0.275° and 0.699° , respectively. H_{max} and $(M_{LRP})_{max}$ were reduced by 40 and 20%, when the rotational flexibility was increased from 0 to $C_{M\theta} = 0.532^\circ/\text{N m}$, respectively (S-R-2 to S-M-1, see Table 5). The rotational flexibility of test S-M-2 ($C_{M\theta} = 4.620^\circ/\text{N m}$) was 8.5 times larger than tests S-M-1. H_{max} and $(M_{LRP})_{max}$ were reduced by approximately 65 and 30%, respectively, when compared with S-R-2 (Table 5). Unlike the horizontal flexibility tests, the load-displacement relationships were highly non-linear, and the θ_{max} increased by 2.5 times with an 8.5 times increase in $C_{M\theta}$.

5.5 Example beam centrifuge tests: effect of including horizontal and rotational flexibility

The tests involved the reinstallation of the spudcan in an over-consolidated clay sample at a distance of $1.0D$ to a pre-existing footprint, with two different stiffness matrices. The first test, B-R-2, assumes a fully rigid stiffness (i.e. $C = 0$), whereas test B-F-1 assumes a stiffness matrix calculated from the SNAME (2002) guidelines (see Tables 3 and 4). While test B-R-2 is with full rigidity, test B-F-1 adopted a full flexibility matrix (vertical-horizontal-rotational cross-coupled) and the model jack-up was reinstalled at $1.0D$ from a real footprint. Therefore, all three governing parameters – that is, the footprint geometry, soil heterogeneity and structural properties of the jack-up unit were investigated in test B-F-1.

Figure 13. Reinstallation responses of $1g$ tests with different horizontal flexibility at CP

Figure 15 shows the results of the two tests. The overall trend of development of loads and displacements with penetration is different between the two tests, highlighting the influence of the stiffness matrix and validating the performance of the system.

In test B-F-1, the jack-up leg initially exhibits a sharp increase in horizontal displacement, associated with an increase in horizontal load, as the spudcan is sliding into the footprint. The maximum horizontal displacement $h_{\max}$ during shallow depth

Test	$H_{\max}$: N	$H_{\max}/As_u$	$M_{lrp \max}$: N m	$M_{lrp \max}/As_u D$	$M_{cp \max}$: N m	$M_{cp \max}/As_u D$	$h_{\max}$: mm	$\theta_{\max}$: deg
S-R-1	2.84	0.34	0.33	1.03	-0.78	-1.57	—	—
S-H-1	2.26	0.20	0.29	0.84	-0.61	-0.77	3.30	—
S-H-2	2.42	0.23	0.30	0.84	-0.58	-0.95	9.51	—
S-R-2	3.78	0.44	0.32	0.97	-0.92	-1.70	—	—
S-M-1	2.25	0.23	0.26	0.73	-0.61	-0.89	—	-0.28
S-M-2	1.38	0.24	0.22	0.71	-0.32	-0.43	—	-0.70

Table 5. Summary of peak responses at shallow depths for $1g$ tests

reinstallation was 508 mm and the $\theta_{\max}$ was 0.8° . Once the penetration has reached the depth of the footprint, the horizontal load decreases significantly, resulting in a horizontal displacement away from the footprint. The moment at the load reference point shows a similar trend, associated with a change in angular rotation. As penetration continued into stronger soil (near z_{FC}), the loading at footing level gradually increased and pushed the footing away from the footprint centre.

At touchdown level, the footing moved rapidly, sliding and rotating towards the footprint centre. The jack-up leg also rose up to 200 mm (prototype). This negative vertical movement was induced from the relatively large vertical-moment coupling flexibility term (C_{Mv}) (see Figure 8 and Table 5) and the negative M_{CP} . As the footing rose up, the soil loading reduced rapidly and fluctuated around zero. As penetration continued, the loading at the footing level gradually increased. The vertical movement resulting from the H and the M_{CP} cancelled each other, and the jack-up leg penetrated the soil at an almost constant rate. The footing started to move in the reverse direction (sliding and rotating away from the footprint centre) for penetration below the footprint toe level. At $z/D = 0.8$, the strong soil layer near z_{FC} resulted in a relatively large negative H and positive M_{LRP} and triggered movement in the vertical direction. The jack-up leg dropped down by 300 mm (prototype). As a result, the vertical, horizontal and moment loads increased rapidly. However, with further penetration, the horizontal force and moment gradually returned to their values before the reduction.

By allowing the jack-up leg to slide, rotate and rise/penetrate, the shallow depth reinstallation peak response was eliminated as the horizontal force and moment decreased to almost zero. The footing initially slid and rotated towards the footprint centre. The stronger soil near z_{FC} helped to push the footing back to its original position. Although the loading still peaks near z_{FC} , the maximum horizontal load at depth and the moment at the CP were reduced by 43 and 16%, respectively, compared with the rigid reinstallation conditions (peak values provided in Table 6).

In practice, if a jack-up could sustain the movement (and loading) during shallow depth reinstallation, with further penetration, the reverse loading at deep reinstallation could correct the movement back to almost its original position. Alternatively, a larger footing (or skirted footing) could be adopted to reduce the penetration depth requirement; this could avoid the large soil load near z_{FC} .

The parallel between the evolution of horizontal loads and displacements and moment and rotation, as well as the concomitance of the inflexion point demonstrate the performance of the system and notably its reactivity to a change in loading conditions. The difference in response between these tests demonstrates that the real-time hybrid system is capable of performing even for a complex load-displacement input and at the high gravity level in the beam centrifuge.

6. Conclusions

The real-time hybrid testing method pioneered in structural dynamic testing has been extended to a geotechnical application in a centrifuge. This paper provides a detailed description of the application of this powerful and novel technique in modelling the jack-up reinstallation problem.

Although they are known to be governing parameters, the effects of the structural properties of a full jack-up unit on the unit's reinstallation responses had never previously been investigated due to testing equipment limitations and the assumption that the jack-up unit was fully rigid at the leg-hull connection level, ignoring the finite structural flexibility of the unit. Therefore, this study was the first to investigate the reinstallation response of a full jack-up unit. Simplification of the jack-up was still required with the numerical component of the hybrid method assuming a static and elastic structure (and spudcans-soil interaction). However, the real-time hybrid method outlined can accommodate a changing flexibility matrix at the CP, and therefore, future studies could use a non-linear and dynamics numerical model to allow a more realistic prototype scaling to be tested.

Figure 14. Reinstallation responses of **1g** tests with different rotational flexibility at CP

Figure 15. Reinstallation response from beam centrifuge tests

Test	$H_{\max}$: MN	$H_{\max}/A_s u$	$M_{\text{Irp max}}$: MN m	$M_{\text{Irp max}}/A_s u D$	$M_{\text{cp max}}$: MN m	$M_{\text{cp max}}/A_s u D$	$h_{\max}$: mm	$\theta_{\max}$: deg
B-R-2	-0.07	-0.36	-0.55	-0.24	1.69	1.78	—	—
B-F-1	-0.04	-0.23	-0.29	-0.20	1.42	1.50	251.3	-0.21

Table 6. Summary of peak responses at deep depths for beam centrifuge tests

By using the real-time hybrid testing method in a beam centrifuge, all three key parameters in the jack-up reinstallation problem – the footprint geometry, soil heterogeneity and structural properties of the jack-up unit – were included, forming a complete model. The footing initially slid and rotated towards the footprint centre, but the stronger soil below the bottom of the footprint cavity helped to push the footing back to its original position. Major, significant differences between the results of using the hybrid apparatus and results of using less adaptive boundary constraints (a fixed system) are shown in this paper. This more realistic testing of complex non-linear conditions is the major advantage of the real-time hybrid testing method, and for the jack-up reinstallation problem the results are considerably more informative, accurate and useful.

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Appendix

A1 Detailed design, fabrication and calibration of the VHM actuator

This appendix provides details of the design, fabrication and calibration of the new VHM actuator. Further details are available in Kong (2012).

A2 Detailed description of the motors and cable

A DC servo motor module composed of a Maxon RE-40-150W Graphite Brushes Motor, a 546:1 Maxon GP42C Planetary GearHead gearbox and a 2000 count Maxon HEDS 5540 500CPT 3 channels encoder was selected to fulfil the speed, torque and precision requirements. The selected motor module with a precision of $1.49 \times 10^{-3}^\circ/\text{count}$ which is in the same order with the requirement was considered to be

sufficient. The precision requirement was the critical motor selection criteria.

The pulley cable was designed to withstand the design working load with a minimum factor of safety of 1.5, hence 225 N. The 1.0 mm diameter strained stainless steel wire with a maximum tensile capacity of 1700 N was selected. This was significantly higher than the design requirement. This is because the cable was selected for its flexibility to form a tight loop around the pulleys, rather than based on strength. The cable was pre-tensioned to 300 N before use, so as to reduce the potential slack in the pulley system.

This reduced the tensile capacity to 1400 N. With such a tight pulley system, the rotational movement at the motor output shaft could be accurately transferred to angular movement at the pivot point. To monitor and to adjust the pre-tension level in the cable, the cable was connected to a strain gauge at each end. The cable was firmly connected to the arm through a nut-screw connection at the end of the cable. The arm was designed to rotate around the pivot point. The pivot point comprised of a series of miniature ball bearings and needle bearings to provide smooth rotational motion under the design load. To prevent out-of-range movement, two limit switches were installed on the inner wall of the actuator box. If tripped, the motor immediately stops to ensure no damage to pulley systems. The output shaft of the motor module was attached to an actuator box that contains all the other components. The actuator box was designed with a transition piece allowing the actuator to be rigidly bolted to the tower of the existing linear actuator unit.

A3 Sources of error and calibration

Like any motor system, the selected motor module is subjected to backlash when the motor reverses the direction of driving. On the basis of the manufacturer's specification, the theoretical backlash at the pivot point is 0.026° . This is considered to be insignificant compared with the design angular stroke.

The stainless steel cable also extends when subjected to tension load. The theoretical extension of a 290 mm long cable under working load (141 N) is 0.1 mm. This corresponds to an angular movement of 0.06° at the pivot point. Again, this error is approximately 3% of the maximum angular movement and was considered to be acceptable.

Angular reading from motor driving: deg	Front encoder reading: deg	Difference : deg	Difference: %
+3	+3.020	0.020	0.67
-3	-2.995	0.005	0.17

Table A1. Calibration of angular reading from front encoder

To assess accurately the actual angular movement at the pivot point, an additional encoder (named the front encoder) was mounted on top of the pivot point. This front encoder is free from the backlash and cable extension error. The front encoder (80000 count Avago AED-3300-BE1 encoder) had a precision of $4.50^\circ/\text{count}$, which was the highest available in the market at the time of design. However, the precision is still substantially less than the design precision requirement ($1.4 \cdot 10^5$ /count). Therefore, the angular movement was still prescribed through the motor module with higher precision and the front encoder was only used for validation of the magnitude of error and for monitoring purposes.

The new actuator was calibrated for accuracy in prescribed angular movement. The vertical position, that of zero rotation ($\theta = 0$), was first determined. Slip gauges, which are stainless steel blocks with precisely known thickness, were used for this. The actuator was driven until the gap between the arm and the side of the actuator box was exactly the same at the two ends of the arm. At that position, the arm is in the exact vertical position and zero rotation at the pivot point can be assumed.

Following this, the accuracy of the rotation of the leg was assessed by physical measurement and by direct comparison with the front encoder reading. The maximum anticipated angular movement of approximately 2° was considered. The angular actuator was first driven from the absolute zero position to 1.9988° ; the sine bar, parallel bar and slip gauges were used to determine the height at the two ends of the arm. The physical angular reading was then determined using basic trigonometry. The difference between the angular reading from the motor encoder and the physically measured angle was less than 0.01° , or 0.6% of that recorded. The accuracy of the angular movement was subsequently assessed by evaluating the different readings from motor driving and the front encoder. The front encoder provides accurate angular reading as it is free from backlash and cable extension error.

To access the variation of accuracy over the full range of angular movement, the actuator was first driven from 0° to the maximum positive stroke of 3° . The actuator was then reversed and moved back to the zero position. Both the motor driving

and the front encoder reading were reset to zero to eliminate the effect of backlash. Finally, the actuator was driven from 0° to the maximum negative stroke of -3° . A slight difference between the motor driving and the front encoder readings was recorded and it increased with angle. However, as detailed in Table A1, the maximum difference is within 0.67% . The reading from the two encoders is therefore in excellent agreement.

The effect of backlash was assessed by driving and reversing the actuator in 0.25° increments, over the full range of angular stroke ($\pm 3^\circ$). The maximum difference during the reverse of angular movement direction was recorded as 0.025° , which is similar to the manufacture's specification of 0.026° .

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