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Joint Kinematics and Sealing Capacity Assessment of Ductile Iron Pipes under Abrupt Transverse Ground Movements

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Abstract: Joint kinematic behaviour, i.e., joint rotation and axial translation, can partially help pipelines to accommodate abrupt ground movements, and cause leaking if joint service limit is exceeded, even without any structural failure. Kinematic behaviour of bell-spigot jointed ductile iron (DI) pipes and its influence on joint sealing capacity under abrupt transverse ground movements are investigated in this study. Firstly, a beam-on-spring finite element analysis on joint kinematics of DI pipes is conducted, in which different fault-pipe crossing positions are implemented. Based on simulated results, a modified joint kinematic solution incorporating pipe deflection and joint shear force under different fault-pipe crossing positions is proposed. Then, a Monte Carlo simulation (MCS)-based reliability assessment procedure for joint sealing capacity is developed. Sensitivity analysis is subsequently conducted to investigate the effects of uncertainties associated with initial axial translation, soil properties, and crossing positions on the joint sealing capacity, and the effects of different deterministic solutions are compared. The proposed method allows engineers to effectively evaluate how the joint sealing capacity of DI pipes changes with consideration of uncertainties when abrupt transverse ground movements are encountered.

Keywords: bell-spigot joint, ductile iron pipeline, strike-slip offset, joint leaking, reliability

1. Introduction

Buried water pipelines are geographically distributed systems that include pipelines of numerous kilometers in length, and are composed of segments assembled by flexible connections such as bell-spigot joints. Buried pipelines are inevitably affected by geohazards, e.g., fault offsets, landslide, tunnelling, excavation, and excessive ground water withdrawing (O'Rourke 2010; Wang et al. 2010; Wham et al. 2016). The adverse effects of these geohazards on buried pipelines can be categorized as permanent ground deformation (PGD), and transient ground deformation (TGD), or seismic waves (O'Rourke 2010). In small and isolated areas, the damage rate of pipelines subjected to PGD can be about 20 times higher than that due to TGD (O'Rourke and Liu 2012). This study focuses on the effect of PGD. Pipe joints can exhibit different kinematic behaviours, i.e., joint rotation and axial translation, to accommodate PGD (Saiyar et al. 2015). However, once the joint rotation and axial translation exceed the joint service limits, the sealing performance deteriorates and joint leaking occurs, which further leads to enormous loss of water resource (Sadeghioon et al. 2014), and form erosion voids near pipes, threatening the stability of the ground and infrastructures (Indiketiya et al. 2017; Tang et al. 2017; Peter et al. 2018).

Increasing attention has been paid to the kinematic behaviour of jointed pipelines. O'Rourke and Liu (2012) introduced a kinematic model for rigid pipes under strike-slip fault movements with crossing angles below 90° , as shown in Fig. 1. When the fault shear band crosses a pipe segment at the center (see Fig. 1a), named the even condition, only the segment is rotated, and the axial translation is equal on both ends of the segment. In the odd condition where the crossing position is located at the pipe joint (see Fig. 1b), rotations of the two segments adjacent to the shear band are the same. Therefore, the rotation of the crossed joint (i.e., relative rotation of the adjacent

segments) is 0. Besides, the axial translation only occurs at the crossed joint. When the length of each pipe segment is the same, and the elongation of the pipe segment is negligible, the kinematic solutions for the even and odd conditions are (O'Rourke and Liu 2012):

$$\begin{cases} \alpha = \arcsin \frac{\delta \sin \theta}{L} & \text{for even condition} \\ \alpha = \arcsin \frac{\delta \sin \theta}{2L} & \text{for odd condition} \end{cases} \quad (1)$$

and

$$\begin{cases} \lambda = \frac{\delta \cos \theta}{2} & \text{for even condition} \\ \lambda = \delta \cos \theta & \text{for odd condition} \end{cases} \quad (2)$$

in which α is the joint rotation angle, δ is the faulting offset, L is length of the pipe segment, θ is the crossing angle between the fault plane and the axial direction of the original pipeline, and λ is the joint axial translation. Saiyar et al. (2015) and Qin and Ni (2019) conducted centrifuge tests and numerical simulations, respectively, on jointed rigid pipes, and examined the effectiveness of using Eqs. 1 and 2 to estimate the joint kinematics of the rigid pipes subjected to normal fault offsets. Wham and O'Rourke (2016) examined the joint response of ductile iron (DI) pipes under large ground deformation and pointed out that the joint sealing performance was dominated by joint kinematics. Argyrou et al. (2019) carried out full-scale tests to investigate the joint kinematics and sealing performance of DI pipes under even strike-slip fault condition. Compared to rigid pipes such as concrete pipes and vitrified clay pipes, DI pipes are semi-rigid pipes with higher ductility and larger tensile strength, therefore they have a higher chance to survive than rigid pipes under abrupt ground deformations. DI pipes are widely used nowadays and can account for about 50% of newly installed water supply pipelines (Wham and O'Rourke 2016; Wham et al. 2017). However, previous studies mainly focus on two fault-pipe crossing positions, i.e., at the joint and the midpoint of barrel, whereas the effect of other possible crossing positions have not been fully

investigated. It is still unknown whether or not the two examined positions can be regarded as the worst scenario for joint kinematics and joint serviceability.

Furthermore, due to the existence of natural variabilities and the lack of sufficient or precise knowledge, some factors affecting the pipe-soil interaction are either difficult to predict or unable to solve (Imanzadeh et al. 2013). For instance, when a jointed pipeline goes across a geohazard area, the geological condition along the pipe route, such as the magnitude and orientation of the offset, and the fault-pipe crossing position relative to joint, are hard to predict precisely. Besides, the assembling quality of joints may also differ from the idealised condition. These uncertainties impose challenges on the application of deterministic methods to evaluate the performance of buried pipelines. Reliability-based methods considering effects of various uncertainties are therefore adopted for pipeline assessment in recent years, which provides an alternative to understand the pipe-soil interaction. The effects of variabilities associated with backfill properties (Babu and Rao 2005; Babu and Srivastava 2010), loading conditions (Babu et al. 2006; Tee et al. 2013), and corrosion and cracking (Tefamariam et al. 2006; Davis et al. 2007; Wang et al. 2015) on the structural safety of buried pipelines have been investigated. Probabilistic methods are also used to evaluate continuous pipelines under differential ground deformations (Imanzadeh et al. 2013; Cheng and Akkar 2017). These studies focus on pipelines' structural safety only, although joint leaking may occur even far before any structural failures (Wham and O'Rourke 2016; Garcia and Moore 2017). Therefore, further investigation is needed for the assessment of joint sealing capacity under abrupt ground movements considering the effect of uncertainties.

This study aims to study the kinematic behavior of bell-spigot jointed DI pipes and the joint sealing capacity under transverse abrupt ground movement, with particular consideration of fault-pipe crossing positions and parameter uncertainties. First, a beam-on-spring finite-element analysis is conducted to investigate the deterministic pipeline response under different fault-pipe crossing positions. Then, based on the simulated joint kinematic behaviour and pipe deformation pattern, an improved kinematic solution incorporating different fault-pipe crossing scenarios is proposed, which provides a basis for the reliability assessment of joint sealing capacity. Subsequently, a Monte Carlo simulation-based reliability assessment procedure for joint sealing capacity is developed. Finally, effects of uncertainties related to initial joint axial translation, soil properties, and fault-pipe crossing positions are examined, and influences of different deterministic computational solutions on joint sealing reliability assessment are investigated.

2. Numerical modeling of joint kinematics for DI pipes under strike-slip faulting

2.1 Beam-on-spring finite element model

To investigate the joint kinematics under various fault-pipe crossing positions and provide a basis for the subsequent study, a beam-on-spring finite element model is first established using a general-purpose finite element software called ABAQUS 6.13 (Dassault Systèmes 2013). An illustration of the beam-on-spring model is depicted in Fig. 2. The pipe barrel is modeled using beam elements (PIPE31), and soil springs in both axial and transverse directions are characterized using pipe-soil interaction elements (PSI34). One side of the PSI elements shares nodes with the beam elements that are used to model the pipeline. The nodes on the other side represent the ground and are subjected to ground displacement via boundary conditions. The vertical displacement along the pipeline is neglected because only strike-slip offset is studied. Joint rotation, axial

translation, and joint shear loading are simulated using three independent springs, i.e., rotation spring, displacement spring and shear spring, respectively, at each joint of the pipeline (see Fig. 2a). The fault-pipe crossing position is defined by the relative position, rp , which is the ratio of the pipe length in the stationary side, L_s , to the total length of the pipe segment, L (see Fig. 2b). L_m is the pipe length in the moving side. According to the definition, rp varies from 0 to 1.

Argyrou et al. (2019) conducted a series of full-scale laboratory tests to investigate the structural response and sealing performance of DI pipes under strike-slip offsets in even condition, i.e., $rp=0.5$, with a crossing angle of $\theta=50^\circ$ and a maximum faulting offset of approximately $\delta=150$ mm. The test parameters are adopted here in the simulation. The sand has a total unit weight of $\gamma=17.6$ kN/m³ and a friction angle of $\varphi=42^\circ$. The pipe has an outer diameter of $D=175$ mm and wall thickness of $t=7.6$ mm. In the numerical analysis, a simplified bilinear stress-strain relationship is implemented for the DI pipe, which has an elastic modulus of 186 GPa, yields at $\sigma=279$ MPa, $\varepsilon=0.15\%$ and fails at $\sigma=460$ MPa, $\varepsilon=10.4\%$ (Wham and O'Rourke 2016). The numerical pipeline model is 22.2 m in length with 6 segments (see Fig. 3), which is long enough to avoid boundary effects under the studied condition. The element size along the pipe is 0.037 m, about 1/100 of the segment length, which is fine enough to capture behaviour of the pipeline based on mesh sensitivity analysis. The offset is imposed on the ground end of PSI elements as a displacement boundary with a crossing angle θ and a fault offset δ , as shown in Figs. 3a and 3b.

A total of five different springs, two soil springs and three joint springs, are included in the model. The stiffness of springs controls the joint kinematics, and dominates the mechanical response of pipelines (Klar et al. 2008; Shi et al. 2016). The force-displacement relationships for transverse

movement, F_t - δ_t , and axial movement, F_a - δ_a , are depicted in Figs. 3c and 3d, respectively. According to Argyrou et al. (2019), the peak transverse resistance is $F_{tp} = N_{qh}\gamma H_c D = 30.1$ kN/m, in which the dimensionless maximum lateral force is $N_{qh} = 11.5$, the burial depth from ground surface to pipe centerline is $H_c = 850$ mm, and the displacement corresponding to the peak resistance is $\delta_{tp} = 0.014H_c = 11.9$ mm. The peak frictional force F_{ap} and the corresponding mobilization displacement δ_{ap} for axial displacement are usually derived based on at-rest condition. O'Rourke et al. (2016) proposed a modified peak frictional force per pipe unit length, F_{atp} , by considering the coupling influence of transverse displacements. Therefore, a trilinear relationship is used herein, with $F_{ap} = 4.7$ kN/m, $\delta_{ap} = 1$ mm, and $F_{atp} = 19.9$ kN/m, $\delta_{atp} = 11.9$ mm. Detailed derivations for these parameters can refer to Argyrou et al. (2019). The studied joint exhibits no rotation resistance when the rotation angle is below 5° , according to Wham and O'Rourke (2016). Therefore, the rotation stiffness is regarded as 0 here. The axial retraction force before the pulling out of spigot is consistently 0.9 kN, with a maximum pulling length of 0.05 m before the axial retraction force is completely lost (Wham and O'Rourke 2016; Argyrou et al. 2019). A very stiff shearing spring is applied, e.g., a shear stiffness of 1.85×10^8 kN/m, to reflect nearly no shear deformation for the studied joints.

2.2 Validation of finite element model

The effectiveness of the finite element model is evaluated by comparing the numerical results against the test results reported by Argyrou et al. (2019), as shown in Fig. 4. An even condition with a strike-slip fault plane crossing the pipeline at the distance of $\zeta = 12.9$ m is considered. Strain gauges were instrumented to measure the longitudinal bending behaviour of the pipeline. Fig. 4a shows that the numerical bending moment along the pipeline from this study is highly consistent

with the literature results. Under the offset distance of $\delta=140$ mm, the maximum moment from FEM in this study is about $11.8 \text{ kN}\cdot\text{m}$ at the distance of $\zeta=13.8$ m, which is quite close to the experimental measurement of $11.0 \text{ kN}\cdot\text{m}$ at $\zeta=13.9$ m. Figs. 4b and 4c show that both joint rotation and axial translation from this study also match quite well with the work of Argyrou et al. (2019). With the increment of offset distance δ , the joint rotation α from this study increases to 2.1° , and the corresponding result from Argyrou et al. (2019) are between 1.8 and 2.2° . The existing kinematic model (see Fig. 1) assumes that under the even condition, the joint axial translations on both ends of the rotated pipe segment are the same, whereas the results from this study and Argyrou et al. (2019) both show that the axial translation in $J3$ is much larger than that in $J4$. For instance, under $\delta=140$ mm, the axial translations for $J3$ and $J4$ are both about $\lambda=45$ mm according to Eq. 2, whereas the numerical results from this study are $\lambda=56$ mm for $J3$ and $\lambda=36$ mm for $J4$, quite close to the corresponding results of 62 mm and 31 mm from Argyrou et al. (2019). It is noteworthy that Argyrou et al. (2019) empirically implemented a lower axial resistance in $J3$ in their simulation; whereas in this study, all the joints have the same axial translation stiffness. Fig. 4c shows that it is unnecessary to decrease the axial displacement stiffness. Generally, Fig. 4 manifests the effectiveness of the numerical model used in this study.

2.3 Effect of fault-pipe crossing position

The effect of fault-pipe crossing position rp is investigated using the beam-on-spring finite element model, and the results of pipe displacements and joint kinematics are examined. The transverse and axial displacements of DI pipes under a strike-slip offset of $\delta=140$ mm and $\theta=50^\circ$ are depicted in Fig. 5, with rp changing from 0.0 to 1.0. The fault shear band is located between joints $J3$ and $J4$, and a total of 11 discrete rp values are simulated. It can be seen from Fig. 5a that when rp is

between 0.1 and 0.9, significant rotation only occurs on segment $P4$, and the transverse displacements change generally along segment $P4$ (i.e., the deformed pipe segments are not straight lines, but different curves). When the fault shear band crosses joints $J3$ and $J4$, i.e., $rp=0$ and $rp=1$, the two adjacent segments, i.e., $P3$ and $P5$, rotate in a similar angle to $P4$, respectively. Note that the existing solution (Eqs. 1 and 2) is proposed based on rigid pipes, in which only rigid body rotation is considered (see Fig. 1). Therefore, each segment in Fig. 1 is in a straight-line shape. DI pipes are categorised as semi-rigid pipes (Moore 2001), and the longitudinal flexural stiffness is lower than rigid pipes such as concrete and vitrified clay pipes. Under the effect of transverse soil resistance, the longitudinal deflection along DI pipes can be expected. With the change of rp , the transverse displacement and especially the longitudinal bending deflection also change correspondingly. Therefore, curved displacements of DI pipes in Fig. 5a can be attributed to the combination of the rigid body rotation and the longitudinal bending deflection induced by the transverse soil resistance.

Fig. 5b shows the axial displacement of DI pipes under various fault-pipe crossing positions. In the examined condition, the change of rp only influences the axial translation at joints $J3$ and $J4$. When $rp=0$, the axial translation only occurs at joint $J3$ with a value of about $\lambda=0.09$ m. When rp increases to 0.4, the axial translation at $J3$ decreases slightly and increases accordingly at $J4$. Remarkable changes occur when rp increases from 0.4 to 0.6. For instance, at $rp=0.5$, the axial translation at $J3$ decreases remarkably to 0.055 m while at $J4$ increases to about 0.037 m. When rp is larger than 0.6, the significant axial translation can only be observed at joint $J4$.

The effects of rp on joint kinematics are shown in Fig. 6. For comparison, test measurements by Argyrou et al. (2019) and estimates using Eqs. 1 and 2 (O'Rourke and Liu 2012) are also provided. Note that Eqs. 1 and 2 only provide results for odd and even conditions, i.e., $rp=0, 0.5$ and 1.0 , therefore the linear interpolation is used for other rp values, as shown in Fig. 6 with dashed lines. Take $J3$ for illustration. Fig. 6a shows that the numerical results of joint rotation for $J3$ increases rapidly from $\alpha=0.2^\circ$ to 2.4° when rp increases from 0 to 0.3, then decreases gradually to $\alpha=0.4^\circ$ when rp further increases to 1.0. As a comparison, Eq. 1 only gives that the joint rotations are $\alpha=0, 1.7^\circ$ and 0.85° , respectively, for $rp=0, 0.5$, and 1. It can be seen that Eq. 1 underestimates the maximum joint rotation, and misjudged the worst crossing position in terms of joint rotation. Similar results can also be observed for joint $J4$. Fig. 6b shows the effect of rp on joint axial translation λ . For joint $J3$, the maximum joint axial translation from FEM occurs at $rp=0$, about $\lambda=90$ mm. When rp increases from 0 to 0.4, λ decreases slightly from 90 to 82 mm. Then, λ decreases significantly from 82 to 7 mm when rp varies between 0.4 and 0.6. After that, λ gradually decreases to 0 when rp further increases to 1. Comparatively, Eq. 2 successfully captures the maximum joint axial translation at $rp=0$, but it underestimates the axial translation at $rp=0.5$ for $J3$. Fig. 6 reveals the shortcomings of the existing kinematic model, which neglects the effects of fault-pipe crossing positions and the longitudinal deflection along DI pipes, leading to the underestimation of joint kinematic behaviours under the abrupt ground movements. Note that the transverse deflection and ovalization of the semi-rigid DI pipes are neglected in this study. A modified kinematic model for DI pipes is therefore proposed in the next section.

3. Modified kinematic solution and sealing assessment of bell-spigot jointed DI pipes

3.1 Modified joint kinematic solution

Based on the numerical results, it can be found that the total rotation of joint $J3$ is composed of:

(a) the rigid body rotation of segment $P4$, and (b) the deflection rotations of segments $P3$ and $P4$.

The rigid body rotation is caused by transverse component of the offset movement. When rp changes between 0.1 and 0.9, the rigid body rotation of $P4$ is identical with that in the even condition, i.e., $rp=0.5$ (see Fig. 5a). The deflection rotation is induced by transverse soil resistance along DI pipes. For illustration, the numerical results of transverse soil resistance along the DI pipes under three offset distance, i.e., $\delta=60$ mm, 10 mm, and 140 mm, with $rp=0.1$, 0.3 and 0.5, respectively, are depicted in Fig. 7. It can be seen that when the crossing position is relatively close to joint $J3$ (i.e., $rp=0.1$), the transverse soil resistance on the stationary side of segment $P4$ is in a rectangular distribution, and on the moving side it is similar to a trapezoid shape (see Fig. 7a). When rp increases to 0.3 and 0.5, the transverse soil resistance on the stationary side of segment $P4$ also becomes a trapezoidal distribution with a peak value of $F_{tp}=30.1$ kN/m (see Fig. 7b and c). It can be attributed to the force-displacement relationship between the pipes and the surrounding soils (see Fig. 3c). Based on Fig. 1, it can be inferred that the largest relative pipe-soil displacement occurs at the crossing position, and it tends to decrease when approaching to the joints. When the relative pipe-soil displacement is smaller than the displacement δ_{tp} , the transverse soil resistance changes with the relative pipe-soil displacement. Once it exceeds δ_{tp} , the transverse soil resistance will be constant even if the relative pipe-soil displacement further increases.

Based on the previous analysis, the deflection pattern of DI pipes and the corresponding distribution of transverse soil resistance can be proposed, as shown in Fig. 8. Three segments are included, i.e., $P3$, $P4$, and $P5$. These segments are regarded as simply supported beams, with transverse soil resistance distributed along the pipe barrels (see Fig. 8a). The transverse soil

resistance on segment $P4$ is divided into two rectangular loads minus two triangular loads, as shown in Fig. 8b. The peak values of the rectangular loads are $F_{ts} = F_{tm} = F_{tp}$. Two supporting forces, i.e., the joint shear forces R_3 and R_4 , locate at the two end of segment $P4$. The distributed loads at the joints are f_3 and f_4 . The lengths of rectangular loads are L_s and L_m , and the lengths of triangular loads are x and y , respectively. The maximum values of the triangular resistances are $F_{ts}f_3$, and $F_{tm}f_4$, see Fig. 8b.

Under the effect of transverse soil resistance, the total displacement of the DI pipe is composed of the rigid body rotation and the longitudinal bending deflection, both can affect the relative pipe-soil displacement and the soil resistance. To estimate the joint kinematics of DI pipes, the effects of the rigid body rotation and the bending deflection should be considered. The deflections at the midspan of the segment $P4$ caused by the rectangular loads are:

$$\begin{cases} \omega_m = \frac{-F_{tm}L_m^5}{24EIL} \left(\frac{3L^3}{4L_m^3} - \frac{L}{2L_m} + \frac{L^5}{16L_m^5} \left(1 - \frac{2L_s}{L} \right)^4 \right) \\ \omega_s = \frac{F_{ts}L_s^5}{24EIL} \left(\frac{3L^3}{4L_s^3} - \frac{L}{2L_s} \right) \end{cases} \quad (3)$$

in which ω_m is caused by F_{tm} , and ω_s is caused by $-F_{ts}$.

The maximum deflection occurs at the midspan of the pipe. Since under the studied condition the deflection of the DI pipe is quite small when compared to the pipe length, the deflection from the joint to the midspan of the pipe is taken to change linearly for simplicity. Therefore, the deflections on the left half ($0 \leq l < 0.5L$) and the right half ($0.5L \leq l < L$) of segment $P4$ caused by the rectangular loads are calculated, respectively, as:

$$\begin{cases} \omega_{11} = \frac{2l\omega_m}{L} \\ \omega_{21} = \frac{2l\omega_s}{L} \end{cases} \quad (4a)$$

and

$$\begin{cases} \omega_{12} = \frac{2(L-l)\omega_m}{L} \\ \omega_{22} = \frac{2(L-l)\omega_s}{L} \end{cases} \quad (4b)$$

in which ω_{11} and ω_{21} are the deflections on the left half of segment $P4$, ω_{12} and ω_{22} are the deflections on the right half of segment $P4$, l is the distance of interest along the pipe (see Fig. 8b).

When $rp > 0$, the rigid body displacement sp along segment $P4$ is:

$$sp = \frac{l\delta \sin \theta}{L} \quad (5)$$

The total displacement of segment $P4$ can thus be written as:

$$\begin{cases} spt_1 = sp + \omega_{11} + \omega_{21} \\ spt_2 = sp + \omega_{12} + \omega_{22} \end{cases} \quad (6)$$

in which spt_1 is the total displacement on the left half of segment $P4$, and spt_2 is the total displacement on the right half.

The relative pipe-soil displacement on the stationary side is $spd_1 = spt_1$. Based on the equilibrium condition of $spd_1 + \delta_{ip} = 0$, the length of triangular load on the left side of segment $P4$, x , can be solved as

$$x = \frac{12EIL\delta_p}{F_{ts}L_s^5\left(\frac{1}{2L_s} - \frac{3L^2}{4L_s^3}\right) + F_{tm}L_m^5\left(\frac{3L^2}{4L_m^3} - \frac{1}{2L_m} + \frac{L^4}{16L_m^5}\left(\frac{2L_s}{L} - 1\right)^4\right) - 12EI\delta\sin\theta} \quad (7)$$

Note that $x \leq L_s$.

If $l \leq L/2$, the relative pipe-soil displacement on the moving side is $spd_{21} = spt_1 - \delta\sin\theta$. Based on

$spd_{21} - \delta_p = 0$, the length of triangular load on the right side of segment $P4$, y , can be solved as

$$y = L - \frac{12EIL(\delta\sin\theta + \delta_p)}{12EI\delta\sin\theta - F_{ts}L_s^4\left(\frac{1}{2} - \frac{3L^2}{4L_s^2}\right) - F_{tm}L_m^4\left(\frac{3L^2}{4L_m^2} - \frac{1}{2} + \frac{L^4}{16L_m^4}\left(\frac{2L_s}{L} - 1\right)^4\right)} \quad (8a)$$

Note that Eq. 8a is used for $y \leq L/2$.

If $L/2 < l \leq L_m$, then $spd_{22} = spt_2 - \delta\sin\beta$. Based on $spd_{22} - \delta_p = 0$, y can be solved as

$$y = - \frac{12EIL\delta_p}{12EI\delta\sin\theta + F_{ts}L_s^4\left(\frac{1}{2} - \frac{3L^2}{4L_s^2}\right) + F_{tm}L_m^4\left(\frac{3L^2}{4L_m^2} - \frac{1}{2} + \frac{L^4}{16L_m^4}\left(\frac{2L_s}{L} - 1\right)^4\right)} \quad (8b)$$

Eq. 8b is used for $L_m \geq y > L/2$. Thus, the lengths of triangular loads, x and y , are solved.

Based on the force equilibrium of segments $P3$ and $P5$, the joint shear force R_3 and R_4 can be

written as

$$\begin{cases} R_3 = \frac{f_3 L}{3} \\ R_4 = \frac{f_4 L}{3} \end{cases} \quad (9)$$

280 Then, based on the equilibrium conditions of bending moment and vertical force on segment $P4$,
 281 following equations can be obtained:

$$282 \begin{cases} R_3 L - F_{ts} L_s (L - \frac{L_s}{2}) + F_{tm} \frac{L_m^2}{2} - \frac{(-F_{ts} f_3)x}{2} (L - \frac{x}{3}) - \frac{(F_{tm} f_4)y^2}{6} = 0 \\ R_3 + R_4 + F_{tm} L_m - F_{ts} L_s - \frac{(-F_{ts} f_4)x}{2} - \frac{(F_{tm} f_4)y}{2} = 0 \end{cases} \quad (10)$$

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284 Thus, f_3 and f_4 can be solved as

$$285 \begin{cases} f_3 = (6F_{tm} L^3 + 6F_{tm} L^3 r p^2 + 6F_{ts} L^3 r p^2 - 12F_{tm} L^3 r p - 12F_{ts} L^3 r p - 2F_{ts} L x^2 \\ \quad + 6F_{ts} L^2 x - 8F_{tm} L y^2 + 9F_{tm} L^2 y - 3F_{ts} x y^2 - 3F_{ts} x^2 y + 6F_{tm} L r p y^2 - 18F_{tm} L^2 r p y \\ \quad + 6F_{ts} L r p y^2 - 18F_{ts} L^2 r p y + 9F_{tm} L^2 r p^2 y + 9F_{ts} L^2 r p^2 y + 9F_{ts} L x y) \\ \quad / (-4L^3 - 6L^2 x - 6L^2 y + 2L x^2 - 9L x y + 2L y^2 + 3x^2 y + 3x y^2) \\ f_4 = (6F_{tm} L^3 - 6F_{tm} L^3 r p^2 - 6F_{ts} L^3 r p^2 - 6F_{tm} L x^2 + 9F_{tm} L^2 x + 2F_{ts} L x^2 \\ \quad + 2F_{tm} L y^2 - 6F_{tm} L^2 y + 3F_{tm} x y^2 + 3F_{tm} x^2 y + 6F_{tm} L r p x^2 \\ \quad + 6F_{ts} L r p x^2 - 9F_{tm} L^2 r p^2 x - 9F_{ts} L^2 r p^2 x - 9F_{tm} L x y) \\ \quad / (-4L^3 - 6L^2 x - 6L^2 y + 2L x^2 - 9L x y + 2L y^2 + 3x^2 y + 3x y^2) \end{cases} \quad (11)$$

286

287 Under the four distributed loads (see Fig. 8b), the respective deflection rotations of joint $J3$ at the
 288 side of segment $P4$ can be written as

$$289 \begin{cases} \alpha_{3-1} = \frac{F_{tm} L_m^2 (2L^2 - L_m^2)}{24EIL} \\ \alpha_{3-2} = -\frac{F_{ts} L_s^2 (2L - L_s)^2}{24EIL} \\ \alpha_{3-3} = \frac{-(F_{tm} - f_4)y}{12EI} \left(\frac{107y^3}{2430L} - \frac{y}{3} L \right) \\ \alpha_{3-4} = -\frac{(F_{ts} + f_3)x}{12EI} \left(\frac{x^3}{10L} - \frac{x^2}{2} + \frac{2xL}{3} \right) \end{cases} \quad (12)$$

290 in which α_{3-1} and α_{3-2} are caused by the rectangular loads, α_{3-3} and α_{3-4} are caused by the triangular
 291 loads.

292

293 The rigid body rotation of segment $P4$, α_{3-5} , is estimated using Eq. 1. Thus, the total rotation of
 294 joint $J3$ at the side of segment $P4$ is

$$295 \quad \alpha_{J3-P4} = \alpha_{3-1} + \alpha_{3-2} - \alpha_{3-3} - \alpha_{3-4} + \alpha_{3-5} \quad (13)$$

296

297 In the meantime, the rotation of joint $J3$ at the side of segment $P3$ can be written as

$$298 \quad \alpha_{J3-P3} = \frac{-f_3 L^2}{45EI} - \arcsin\left(\frac{f_3 \delta_{ip} L}{F_{ip}}\right) \quad (14)$$

299 in which the first and the second items on the right-hand side are caused by the deflection and rigid
 300 body rotation of segment $P3$, respectively.

301

302 Therefore, the total rotation of joint $J3$ can be written as

$$303 \quad \alpha_{J3} = \alpha_{J3-P4} - \alpha_{J3-P3} \quad (15a)$$

304 or

$$305 \quad \alpha_{J3} = \arcsin \frac{\delta \sin \theta}{L} + \arcsin \frac{\delta_{ip} f}{F_{ip} L} + \frac{L^3 f_3}{45EI} + \frac{F_{tm} L_m^2 (2L^2 - L_m^2)}{24EIL} - \frac{F_{ts} L_s^2 (2L - L_s)^2}{24EIL} \\ - \frac{y(\frac{L^2 y}{3} - \frac{107y^3}{2430})(F_{tm} - f_4)}{12EIL} + \frac{x^2 (F_{ts} + f_3)(20L^2 - 15Lx + 3x^2)}{360EIL} \quad (15b)$$

306

307 This study focuses on the joint kinematics and joint leaking of DI pipes, the related offset distance
 308 is generally smaller than those considered in the safety analysis of continuous flexible pipelines,
 309 where the offset distances might be up to several meters (Melissianos et al. 2017b). Under the
 310 studied condition, the measured offset distance that caused leakage at the DI pipe joint is only 137

mm (Wham and O'Rourke 2016), which leads to a rotation angle less than 3° (see Fig. 6a). Since $\cos 3^\circ = 0.999$, the transverse resistance can be regarded to be perpendicular to pipe axis (e.g., Fig. 8b), and it accounts for at least $\cos 3^\circ = 0.999$ of the total transverse resistance. Besides, the crossing angle θ in the proposed method should also be below 90° , the same with the existing solution.

As to the axial translation, Fig. 6b shows that the numerical result of the maximum axial translation in joint $J3$ occurs at $rp=0$. Then it declines slightly when rp increases to 0.4. Therefore, when rp is between 0.1 and 0.4, λ_3 can be conservatively estimated using Eq. 2 for $rp=0$. When $rp=0.5$, the existing solution suggests that the axial translation is only the half of the value for $rp=0$, but the numerical result results and the test values both show that the axial translation of $J3$ at $rp=0.5$ is about 1.4 times of the recommended value (see Fig. 6b). Therefore, the translation calculation for λ_3 can be written as

$$\lambda_3 = \begin{cases} \delta \cos \theta & \text{for } rp < 0.5 \\ \frac{1.4 \delta \cos \theta}{2} & \text{for } rp = 0.5 \\ 0 & \text{for } rp > 0.5 \end{cases} \quad (16)$$

The estimated rotations of joint $J3$ using the proposed method are compared with the numerical results and test measurements in Fig. 9. It can be seen that, when rp changes between 0 and 1.0, the results from the proposed method match relatively well with the numerical results and test measurements. Both numerical results and the proposed method suggest that the rotation of joint $J3$ first increases with rp , then decreases gradually when rp is larger than 0.3. The maximum rotations from the proposed method are about $\alpha = 1.1^\circ$, 1.7° , and 2.3° , respectively, for $\delta = 60$ mm,

100 mm, and 140 mm, which are quite close to the corresponding numerical results of $\alpha=1.1^\circ$, 1.8° , and 2.4° .

The results of axial translations for joint $J3$ are shown in Fig. 10. It shows that, compared to Fig. 6b of using the existing solution, the proposed method can provide relatively safe estimates on axial translation for joint $J3$ when rp is between 0 and 0.5. As for $rp>0.5$, it can be inferred from Fig. 6 that joint $J4$ exhibits more obvious kinematic responses than joint $J3$. The kinematic response of joints $J3$ and $J4$ are nearly antisymmetric to each other when rp changes between 0 and 1.0. For brevity, only results for joint $J3$ with rp between 0 and 0.5 are discussed in the following analysis.

3.2 Joint sealing serviceability assessment

Wham and O'Rourke (2016) investigated the initiation of leaking for bell-spigot jointed DI pipes under large ground deformation. They found that the leaking behaviour was independent of loading path, and was only related to joint kinematics. Similar results were also reported by García and Moore (2017). Wham and O'Rourke (2016) then proposed a leaking boundary composed of α ($^\circ$) and λ (mm) for the studied DI pipes, which can be written as

$$\begin{cases} [\alpha]=16 & \text{for } 21\text{mm} < \lambda \leq 41\text{mm} \\ [\alpha]=\frac{928-16\lambda}{17} & \text{for } 41\text{mm} < \lambda \leq 58\text{mm} \end{cases} \quad (17)$$

Once the joint kinematics exceeds the boundary of Eq. 17, joint leaking can happen. Note that for the studied joint type, when the joint axial translation exceeds 58 mm, the joint will leak even without any joint rotation (Wham and O'Rourke 2016). Combine the leaking boundary with the

proposed joint kinematic solutions, the sealing status of DI pipe joints under strike-slip fault offsets can be assessed. Consider, for instance, the test condition of $rp=0.5$ (Wham and O'Rourke 2016). Figs. 9 and 10 show that under $\delta=60$ mm, 100 mm, and 140 mm, the joint kinematics based on the proposed method are $\alpha=0.89^\circ$, 1.52° , and 2.06° , and $\lambda=27$ mm, 42.6 mm, and 56 mm. Based on the leaking boundary of Eq. 17, the corresponding values of $[\alpha]$ are 16° , 14.49° , and 1.88° . It can be seen that α under $\delta=60$ mm and 100 mm does not exceed $[\alpha]$, but $\delta=140$ mm causes joint leaking. The results of offsets that cause joint leaking under different crossing positions and crossing angles are shown in Fig. 11. For comparison, the results based on the existing kinematic solution are also depicted. Note that the existing solutions of Eqs. 1 and 2 only give results for the odd and even conditions, thus for other crossing positions, the joint rotation and axial translation are based on the linear interpolation. The test measurement for $\theta=50^\circ$ from Wham and O'Rourke (2016) is provided as well. At $rp=0$, the offset that causes joint leaking, δ_L , from both methods are nearly the same. When rp increases, δ_L based on the existing solutions increases gradually, while the results based on the proposed method is nearly constant when rp is between 0 and 0.4, and only increases at $rp=0.5$. The results based on the existing solution tends to overestimate the joint sealing capacity. For instance, at $\theta=50^\circ$ and $rp=0.5$, δ_L based on the existing solutions is about 174 mm, whereas the test result is about 137 mm (Wham and O'Rourke 2016), and the estimate based on the proposed method is 125 mm. When a relatively larger θ is encountered, the difference between the two methods becomes more obvious. Generally, the proposed method can provide more accurate and relatively safe estimates on the offset that causes joint leaking compared to the existing method.

4. Monte Carlo simulation-based reliability assessment of joint sealing capacity

The method proposed in the previous section is deterministic. It neglects the influence of uncertainties associated with the orientation and distance of the offset, the installation quality of the pipe joint, the soil properties, and the crossing positions, etc., on joint sealing capacity, which might lead to unsafe results of maximum allowable offsets for joint sealing. Therefore, a Monte Carlo simulation-based reliability assessment procedure is introduced in this section to investigate the effect of parameter uncertainties on the evaluation of joint sealing capacity under strike-slip offsets.

4.1 MCS-based reliability assessment procedure

The reliability based design procedure proposed by Wang (2011) is used in this section. The purpose here is to find out the maximum allowable offset, under which the pipe joints can meet both the serviceability limit state requirement, i.e., no leaking, and the specified reliability requirement. Different from previous probabilistic assessments on safety of pipelines under earthquakes in which both demand and capacity uncertainties are considered (Melissianos et al. 2017a; Melissianos et al. 2017b), the offset parameters (δ , θ) in this study are used as the external parameters. This is similar to a reliability-based design process (Wang 2011; Wang et al. 2011; Wang and Cao 2013) that a pair of feasible “design values” is to be found, which can meet both the SLS and reliability requirements.

The flow chart of the reliability assessment is shown in Fig. 12. Firstly, the probability distributions of faulting parameters and joint leaking-related uncertain variables are characterized. Then, n sets of random samples are generated, and Monte Carlo simulation (MCS) is conducted to obtain n sets

of outputs for leaking examination. The statistical analysis on the probability of leaking failure p_f^L and conditional joint probability $p(\delta, \theta | \text{Leaking})$ can be performed using

$$p_f^L = \frac{n_f}{n} \quad (18)$$

$$p(\delta, \theta | \text{Leaking}) = \frac{n_l}{n_f} \quad (19)$$

in which n_f is the number of MCS samples with joint leaking, n is the total number of MCS samples, n_l is the number of MCS samples where joint leaking and a specific combination of δ and θ occur simultaneously.

The accuracy of the MCS depends on the value of n . According to Wang (2011), the minimum value of n can be determined by

$$n_{\min} = \frac{10n_\delta n_\theta}{p_T} \quad (20)$$

where p_T is the target failure probability, n_δ and n_θ are the numbers of possible discrete values for δ and θ .

Finally, the conditional probability $p(\text{Leaking} | \delta, \theta)$ can be calculated using Bayes' Theorem by

$$p(\text{Leaking} | \delta, \theta) = \frac{p(\delta, \theta | \text{Leaking})}{p(\delta, \theta)} p_f^L \quad (21)$$

in which $p(\delta, \theta)$ is the mass function and can be written as

$$p(\delta, \theta) = \frac{1}{n_\delta n_\theta} \quad (22)$$

4.2 Identification and quantification of uncertain variables

The statistics of uncertain variables are listed in Table 1. Since studies regarding distributions of some related parameters, such as the initial axial translation λ_0 and the relative crossing position rp , are still rare, these uncertainties are therefore estimated using values which are possibly encountered in engineering practice. The initial axial joint displacement λ_0 occurs during installation, which is taken to follow a normal distribution with a mean value of $m=5$ mm, and a standard deviation of $SD=2$ mm. The unit weight γ and the friction angle φ of soil also follow normal distributions, with $m=42^\circ$, $SD=3.8^\circ$ for γ , and $m=17.6$ kN/m³, $SD=1.76$ kN/m³ for φ , respectively. A correlation coefficient of $\rho=0.6$ is applied between γ and φ . A total of 6 crossing positions are investigated, i.e., $rp=0, 0.1, 0.2, 0.3, 0.4$, and 0.5 , which follow a uniform distribution in the MCS.

The dimensionless maximum lateral force N_{qh} mainly reflects the influence of soil friction angles and relative burial depths on lateral soil resistance. Previous studies (Trautmann and O'Rourke 1985; O'Rourke and Liu 2012; Jung et al. 2016) and ASCE standard (ASCE 1984) recommended values of N_{qh} for different relative burial depths H_c/D and four friction angles, i.e., $\varphi=30^\circ, 35^\circ, 40^\circ$, and 45° , see Fig. 13. For other friction angles, N_{qh} based on linear interpolation is suggested. In the process of MCS, it is highly possible that different friction angles other than the four mentioned above can be generated. For ease of MCS, a curve fitting analysis among N_{qh} , φ , and H_c/D is conducted, based on which N_{qh} can be quickly estimated by

$$N_{qh} = \frac{0.6355e^{0.0858\varphi} \frac{H_c}{D}}{0.2633e^{0.0635\varphi} + \frac{H_c}{D}} \quad (23)$$

Fig. 13 shows the comparison between the results of Eq. 23 and the recommended values of N_{gh} (O'Rourke and Liu 2012). It can be seen that the two results match quite well with each other. The possible values and numbers of δ and θ are listed in Table 2. Under the studied condition, the possible δ varies from 1 to 200 mm with an increment of 1 mm, which is chosen cautiously to spot the initiation of joint leaking. O'Rourke and Liu (2012) suggested an optimal crossing angle of $\theta=60^\circ$ for buried pipelines subjected to fault crossing hazard. Therefore, three crossing angles of $\theta=50^\circ$, 60° , and 70° are considered here. The numbers of possible δ and θ are thus $n_\delta = 200$ and $n_\theta = 3$. In the traditional MCS procedure, a single run of MCS is conducted to estimate p_f and β for one combination of (δ, θ) , and a total of 600 separate runs of MCS is needed and conducted repeatedly to cover all the possible combinations of (δ, θ) . In the presented procedure, the possible δ and θ from Table 2 are generated as independent discrete random variables with a uniformly distributed probability mass function $p(\delta, \theta)=1/n_\delta n_\theta$ (see Eq. 22). Thus, each possible combination of (δ, θ) has the same occurrence frequency, and only one single run of MCS is needed, which significantly improves the computational efficiency and avoid repeating 600 runs of MCS for different combinations of (δ, θ) .

In the presented procedure, the minimum numbers of MCS samples for the reliability levels of $\beta_T^{SLS}=1.5$ (or $p_T^{SLS}=6.7\times 10^{-2}$) and $\beta_T^{SLS}=2.6$ (or $p_T^{SLS}=4.6\times 10^{-3}$), respectively, are $n_{\min}=89553$ and 1304348 according to Eq. 20. In the traditional procedure, $n_{\min}$ is only $10/p_T^{SLS}=2174$ for $p_T=4.6\times 10^{-3}$. The accuracy of MCS results improves as the number of MCS samples increases. Therefore, the accuracy of MCS results in the presented procedure is significantly enhanced. To further improve the resolution at small probability levels, a sample number of $n = 2000000$ is used in this study. Although n seems to be quite large, it only takes about several minutes to finish the

single run of MCS using MATLAB R2014b (MathWorks 2014) in a laptop with i7-3520M CPU and 8-GB RAM. Comparatively, a single beam-on-spring FE model (see Section 2) requires at least half a minute using the same computer, and a total of $n=2000000$ FE modeling would cost nearly two years. The efficiency of the proposed procedure is significantly higher than that using FEM.

4.3 Baseline MCS results on joint sealing capacity assessment

A series of baseline MCS for the joint sealing capacity assessment of DI pipes under strike-slip fault offsets is conducted first, results of which are given in Fig. 14. The leaking of joint due to excessive joint kinematics is defined as the serviceability limit state (SLS) failure in this section. Two target reliabilities (i.e., $\beta_T^{SLS} = 1.5$ and 2.6) or target probabilities of failure (i.e., $p_T^{SLS} = 6.7 \times 10^{-2}$ and 4.6×10^{-3}) are considered (Wang 2011), which are also depicted in Fig. 14. Two burial depths are considered, i.e., $H_c = 0.85$ m and 1.75 m, or relative burial depths of $H_c/D = 4.86$ and 10. The joint sealing allowable offsets are those with probabilities of leaking failure p_T^L smaller than p_T^{SLS} . Generally, p_T^L increases as the offset distance δ increases. The maximum joint sealing allowable offset δ_m increases with the increment of the crossing angle θ , but decreases slightly when p_T^{SLS} decreases. For instance, when $H_c = 0.85$ m and θ increases from 50° to 70° , δ_m increases from 76 to 139 mm for $p_T^{SLS} = 6.7 \times 10^{-2}$, and from 72 to 132 mm for $p_T^{SLS} = 4.6 \times 10^{-3}$ (see Fig. 14a). It can be attributed to that under the same δ , larger θ can lead to relatively smaller joint axial translations, and a smaller p_T^{SLS} requires smaller offsets. When the burial depth increases to $H_c = 1.75$ m, the results of δ_m are nearly identical with those for $H_c = 0.85$, as shown in Fig. 14b. This is probably because that, although the change of H_c/D can affect N_{qh} and thus the soil resistance on DI pipes, the ground deformations at the two depths are considered as the same. Therefore, it

can be seen that under the studied conditions, the effect of burial depths on joint sealing capacity is negligible under the studied condition. For brevity, only $H_c=0.85$ m is considered in the following analysis. As a comparison, if no uncertainty is considered, the maximum joint sealing allowable offset for $\theta=50^\circ$ and 70° , respectively, should be at least 88 mm and 161 mm (see Fig. 11), which remarkably overestimates the sealing capacity of DI pipe joints under strike-slip offsets as compared to the results in Fig. 14. The sensitivity of parameter uncertainties on the maximum joint sealing allowable offset are further investigated in the next section.

5. Sensitivity of parameter uncertainties on joint sealing capacity

5.1 Effect of COV_{λ_0} on joint sealing capacity

The effect of Coefficient of variation of the initial axial translation, COV_{λ_0} , on joint sealing capacity is investigated in Fig. 15. With the decrease of COV_{λ_0} , the slope of p_T^L - δ curve becomes steeper, and δ_m increases slightly. Besides, the difference between δ_m for the two p_T^{SLS} also becomes less significant when COV_{λ_0} decreases. For instance, at $\theta=50^\circ$ and $p_T^{SLS}=6.7\times 10^{-2}$, δ_m increases slightly from 74 to 78 mm when COV_{λ_0} decreases from 0.6 to 0.2 (see Fig. 15a). If $p_T^{SLS}=4.6\times 10^{-3}$ is considered, δ_m for $COV_{\lambda_0}=0.6$ decreases from 74 to 68 mm, and δ_m for $COV_{\lambda_0}=0.2$ only decreases from 78 to 76 mm (see Fig. 15a). Similar results can be observed for other crossing angles. The effect of COV_{λ_0} becomes more obvious when θ increases. For example, at $\theta=70^\circ$, when COV_{λ_0} decreases from 0.6 to 0.2, δ_m increases from 134 to 142 mm when $p_T^{SLS}=6.7\times 10^{-2}$, and increases from 125 and 139 mm when $p_T^{SLS}=4.6\times 10^{-3}$ (see Fig. 15c). Fig. 15 illustrates the importance of joint installation quality, and the effect of COV_{λ_0} on joint sealing capacity.

5.2 Effect of uncertainties in soil properties on joint sealing capacity

508 The effects of uncertainties in soil properties, i.e., $\rho_{\gamma-\varphi}$, COV_{γ} and COV_{φ} , on joint sealing capacity
 509 are investigated in Fig. 16. Three groups of uncertainty combinations are considered. In the first
 510 one, relatively small COV for γ and φ are considered, i.e., $\text{COV}_{\gamma}=\text{COV}_{\varphi}=0.05$, and the correlation
 511 between the two is also relatively low, i.e., $\rho_{\gamma-\varphi}=0.3$. The second is the baseline case, i.e., $\rho_{\gamma-\varphi}=0.6$,
 512 $\text{COV}_{\gamma}=0.1$, and $\text{COV}_{\varphi}=0.09$. In the third one, $\rho_{\gamma-\varphi}=0.9$, and $\text{COV}_{\gamma}=\text{COV}_{\varphi}=0.15$. It can be seen that
 513 under each crossing angle θ , the change of $\rho_{\gamma-\varphi}$, COV_{γ} and COV_{φ} does not cause any obvious
 514 difference in δ_m compared to the baseline results. Although soil properties (i.e., γ and φ) can affect
 515 the soil resistance on the pipes during the offset movements, the joint sealing capacity is not
 516 sensitive to the uncertainties associated with γ and φ under the studied condition.

517

518 5.3 Effect of rp on joint sealing capacity

519 The effect of rp on joint sealing capacity is investigated, results are depicted in Fig. 17. It shows
 520 that under different crossing angles, δ_m for $rp=0.1$ to 0.4 are nearly identical with the baseline
 521 results, and significantly lower than that for $rp=0.5$. With the increment of θ , δ_m for $rp=0.0$
 522 becomes slightly larger than baseline result, i.e., the p_t^L - δ curve for $rp=0.0$ stands at the right of
 523 the baseline curve. The even condition of $rp=0.5$ is the most favorable crossing position for joint
 524 sealing, and the odd condition ($rp=0.0$) can only be regarded as the worst scenario when a relatively
 525 small crossing angle (e.g., $\theta=50^\circ$) is encountered. Comparatively, $rp=0.1$ to 0.4 should be
 526 considered for joint sealing capacity evaluation. Besides, the reliability-based δ_m are much smaller
 527 than those from the deterministic method (see Figs. 11 and 17). For instance, at $\theta=50^\circ$, δ_m under
 528 $\beta_T^{\text{SLS}}=1.5$ and 2.6 for $rp=0.1$ to 0.4 are approximately 76 and 72 mm, respectively, whereas the
 529 deterministic result is about 88 mm (see Fig. 11a). Similar results can also be observed for other

crossing angles. It illustrates the need of considering the possibilities and uncertainties associated with rp in joint sealing capacity evaluation.

5.4 Effect of deterministic computational solutions on joint sealing capacity

The reliability assessment procedure provides flexibility to use different deterministic computational solutions in MCS (Wang 2011). An additional run of MCS using the existing solutions of Eqs. 1 and 2 (O'Rourke and Liu 2012) was conducted for comparison, the results are shown in Fig. 18. Note that the existing solution only considers the odd and even conditions, i.e., $rp=0$ and 0.5 , therefore the deterministic results for rp between 0.1 and 0.4 are based on linear interpolation. The results of δ_m from the proposed method are generally smaller than those from the existing solutions, i.e., p_r^L - δ curves from the proposed method are on the left of those based on the existing solutions. With the increment of θ , the overestimation of δ_m based on the existing solutions becomes more significant. For instance, at $\theta=50^\circ$, δ_m from the proposed method is 76 mm for $\beta_T^{SLS}=1.5$, about 5 mm smaller than the one based on the existing solutions (see Fig. 18a). At $\theta=70^\circ$, δ_m from the proposed method increases to 139 mm for $\beta_T^{SLS}=1.5$, but δ_m based on the existing solutions further increase to 153 mm. The improvement of the proposed method compared to the existing solutions can thus be seen.

It is noteworthy that this study is based on the typical leaking boundary of DI pipes with TYTON bell-spigot joints (Wham and O'Rourke 2016). Extra cautions are recommended when other types of DI pipe joints with different leaking boundaries are to be assessed. Once the corresponding leaking boundary is available, the sealing capacity of the DI pipe joints under strike-slip fault offsets can be quickly assessed using the method proposed in this study.

6. Summary and conclusions

Deterministic and Monte Carlo simulation-based reliability assessments on sealing capacity of bell-spigot jointed ductile iron pipes under strike-slip fault offsets were conducted in this study. Beam-on-spring finite element modelling was firstly carried out to investigate the joint kinematic behaviour under various fault-pipe crossing positions. Based on that, a modified joint kinematic solution was proposed, and a deterministic approach for joint sealing capacity evaluation under strike-slip fault offset was established. Then, a Monte Carlo simulation-based reliability assessment procedure is used to investigate the effects of uncertainties associated with fault offsets and pipe-soil interactions on the joint sealing capacity of DI pipes. Following conclusions can be drawn:

- (1) The beam-on-spring FEM modeling revealed that both joint rotation and axial translations were remarkably affected by fault-pipe crossing positions, and the deflection along DI pipes and joint shear force had significant influence on the rotation of pipe joints.
- (2) The proposed kinematic solution showed that the largest joint rotation occurred when the fault-pipe crossing position located at nearly $rp=0.3$ of the pipe segment, whereas the smallest axial translation appeared at the even condition of $rp=0.5$. Compared to the existing solution, the estimate of offset that causes joint leaking based on the proposed solution was much closer to the test measurement.
- (3) The offset distance that causes joint leaking under the even condition is much larger than other crossing positions. The odd condition can be regarded as the worst crossing scenario when a relatively small crossing angle, i.e., 50° , was encountered. When parameter uncertainties are

considered, the maximum allowable offset for joint sealing is significantly smaller than that from the deterministic method.

(4) Comparatively, the uncertainties associated the initial axial translation and crossing positions significantly influence the reliability assessment results, whereas the joint sealing capacity is not sensitive to the uncertainties in soil properties. Different deterministic solutions can be easily integrated with MCS, and MCS with the solution proposed in this study provided better assessment on joint sealing than MCS with the existing solution.

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List of symbols

D	pipe outer diameter (mm)
$F_a, F_{ap}, F_{atp}, F_t, F_{tp}$	axial soil resistance, peak frictional force, modified peak frictional force, transverse soil resistance, and peak transverse soil resistance (kN/m)
H_c	burial depth from ground surface to pipe centerline (mm)
L, L_s, L_m	length of pipe segment, lengths of segment portion in the stationary side and in the moving side (mm)
N_{qh}	dimensionless maximum lateral force
p_f^L, p_T, p_T^{SLS}	probability of leaking failure, target probability of failure, and target probability of SLS failure
rp	relative crossing position
α	joint rotation angle ($^\circ$)
$\beta, \beta_T, \beta_T^{SLS}$	reliability, target reliability, and target SLS reliability
γ_d	dry unit weight of soil (kN/m ³)
$\delta, \delta_L, \delta_m$	fault offset, offset that causes joint leaking, and maximum sealing allowable offset (mm)
$\delta_a, \delta_{ap}, \delta_{atp}, \delta_t, \delta_{tp}$	mobilization displacements for $F_a, F_{ap}, F_{atp}, F_t$, and F_{tp} (mm)
ζ	distance along pipeline (m)
θ	fault-pipe crossing angle ($^\circ$)
λ, λ_0	joint axial translation and initial axial translation (mm)
φ	soil friction angle ($^\circ$)

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Table 1. Statistics of uncertain variables

Uncertain variables	Distribution	Distribution parameters
Initial axial displacement, λ_0 (mm)	Normal	N(5, 2)
Soil friction angle, φ (°)	Normal	N(42, 3.8)
Soil unit weight, γ (kN/m ³)	Normal	N(17.6, 1.76)
Correlation coefficient, $\rho_{\gamma-\varphi}$	/	0.6
Relative crossing position, rp	Uniform	0, 0.1, 0.2, 0.3, 0.4, 0.5

Table 2. Considered values and numbers of external parameters

Artificial variables	Discrete values	Discrete number
Offset distance, δ (mm)	1, 2, ..., 159, 200	200
Crossing angle, θ (°)	50, 60, 70	3

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