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Ductile failure prediction of pipe-ring notched AISI 316L using uncoupled ductile failure criteria

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Abstract

The goal of this work is to predict ductile failure of pipe-ring notched AISI 316L specimens, where notches mimic the geometry of corrosion defects. Uncoupled damage models are used to that end. The Johnson-Cook and Lou-Huh criteria are calibrated.

The uncoupled damage models are calibrated using experimental results from testing pipe-ring notched specimens. Calibration was achieved using a hybrid experimental-numerical approach. Six notch shapes are studied. Experimental matrices are designed to determine these shapes, and the pipe-ring notched specimen is inspired from the literature.

Calibration of the uncoupled damage models require a ductile crack initiation indicator. The first indicator was based on the derivative curves of the force versus the connectors displacement curve. The second was based on the raw images of the gage section. The third was based on a percentage of the connectors displacement at fracture.

Results show that ductile fracture depends on the Lode paramater. As a consequence, the Lou-Huh criterion is more effective at predicting ductile fracture than the Johnson-Cook criterion.

Keywords: AISI 316L, crack initiation indicator, ductile failure, hybrid experimental-numerical identification, pipe specimen, uncoupled damage models.

1. Introduction

Ductile failure is characterized by significant plastic deformation during loading [1]. Ductile failure process is induced either by void coalescence, or by plastic instability through shear bands [1]. The failure process depends on the stress state and the material [2, 3]. A combination of both failure processes can exist [2, 3]. Pipelines are made of ductile materials. However, corrosion defects may arise at the surface of these pipelines. These defects locally reduce the pipeline thickness. As a result, the pressure of the transported fluid must decrease to avoid a catastrophic event. Ductile crack initiation prediction consists in estimating the maximum plastic deformation, under local multiaxial loading, that the material can accumulate before crack initiation. Subsequently, the relative maximum admissible pressure can be assessed.

Numerous standards provide tools to estimate the maximum pressure within a corroded pipe [4–7]. However, these estimations are over-conservative [8–12]. Multiple research projects have been undertaken to provide more accurate tools [8–20]. Global and local approaches have been used to predict ductile failure. In the global approach, the von Mises stress at failure is assumed proportional to the ultimate tensile strength. The von Mises stress is computed using finite element simulations, and the coefficient of proportionality is calibrated on experimental results at failure. This approach provided accurate results, since the relative errors between the predictions and the experiments at failure were between -4.6% and 1.1% [8, 10–17]. However, as pointed out by Chiodo and Ruggieri [14], the coefficient of proportionality depends on the defect geometry. The defect geometry of corroded pipes being random, this approach is not reliable for industrial applications.

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models with the material behaviour. The coupled damage models take into account the softening behaviour of the material due to damage increase. The material behaviour in the uncoupled damage models is unaffected by damage growth and increasing plastic hardening is observed until fracture. The advantage of the coupled models is their capability of predicting localized necking. However, the main issue regarding coupled damage models is their numerical dependency on the mesh size, which requires the use of non-local approaches [21]. Additionally, these models often require numerical developments and access to the source code of the finite element program. This is not always possible. Uncoupled damage models are acceptable for industrial applications, since the material is usually removed or repaired before reaching a softening behaviour. In addition, uncoupled damage models do not have numerical bias. In the present study, uncoupled damage models were studied to predict ductile failure. Pipe failure occured when a ductile crack initiated.

Ductile failure in uncoupled models occurs when the damage indicator (D_c) reaches a critical value, which depends on the failure criterion function. At failure, D_c is expressed as:

$$D_c = \int_0^{\bar{\varepsilon}_f} f\left(X_1, \dots, X_n, C_1, \dots, C_m\right) d\bar{\varepsilon}_p, \qquad (1)$$

where $\bar{\varepsilon}_p$ is the equivalent plastic strain, $\bar{\varepsilon}_f$ the equivalent plastic strain at failure, X_i parameters representing the local stress state, and C_i fitting parameters of the failure criterion function (*f*). Numerous failure criterion functions are proposed in the literature. Pioneer criteria [22, 23, 24, 25] had a simple form. For instance, the criterion function from Cockcroft and Latham [23] was $f = \sigma_1$, where σ_1 is the maximum principal stress. These criteria do not have fitting parameters. They predict ductile failure under simple loading. The second family of criteria [26–29] introduced the stress triaxiality parameter (η), expressed as [30]:

$$\eta = \frac{\sigma_m}{\sigma_{vm}}, \qquad (2)$$

where σ_m and σ_{vm} are the mean and the equivalent von Mises stresses, given by:

$$\sigma_m = \frac{\mathrm{tr}(\boldsymbol{\sigma})}{3}, \qquad (3)$$

$$\sigma_{vm} = \sqrt{\frac{3}{2}\boldsymbol{s}\cdot\boldsymbol{s}} \quad \text{with } \boldsymbol{s} = \boldsymbol{\sigma} - \sigma_m \boldsymbol{\delta},$$

where $\boldsymbol{\sigma}$ is the Cauchy stress tensor, $\boldsymbol{s}$ the deviatoric component of $\boldsymbol{\sigma}$, and $\boldsymbol{\delta}$ the Kronecker delta. These second family criteria offered an analytical solution to one or numerous dimples growing in a representative elementary volume. These criteria were representative of void coalescence induced ductile failure mode, which is in the range of high stress triaxialities [2]. In the range of low stress triaxialities, plastic instability mode dominates [2]. Indeed, the local stress state is not fully describe with the use of the stress triaxiality parameter. As a consequence, third family criteria emerged [31–39]. The purpose of these criteria was to predict ductile failure for the full range of stress triaxialities. These criteria phenomenologically introduced a new parameter, which enables to fully describe the local stress state. This paramater has multiple equivalent forms, including the Lode parameter (*L*), the Lode angle (θ), or the normalized Lode angle ($\bar{\theta}$), which are given by [30]:

$$L = \frac{2\cdot\sigma_2 - \sigma_1 - \sigma_3}{\sigma_1 - \sigma_3} \quad \text{with } \sigma_3 \le \sigma_2 \le \sigma_1, \qquad (4)$$

$$\theta = \frac{\qquad}{\sqrt{3}}$$

$$\bar{\theta} = 1 - \frac{6 \cdot \theta}{\pi}$$

where σ_1, σ_2, and σ_3 are the principal stresses. These criteria theorically predict ductile failure for the full range of stress triaxialities. Indeed, ductile failure is sensitive to the Lode parameter in the range of low stress triaxialities. However, the tests calibrating the fitting parameters in Equation (1) must cover a range of low stress triaxialities. Otherwise, the prediction capability of the second family criteria is sufficient.

Oh *et al.* [18] used a second family criterion, namely, Johnson-Cook [29], to predict the ductile failure of API X65 steel pipes. The API X65 steel was a ferritic alloy having an ultimate tensile strength of 564MPa. The maximum relative errors between the predictions and the experimental results were 4.0%. This result showed that a second family criterion was sufficient to predict ductile failure of API X65 steel pipes. DÅ¾ugan *et al.* [20] compared the prediction capability of second and third family criteria on a ferritic and an austenitic steel alloys. Ductile failure of the studied austenitic steel alloy was reliably predicted by second family criteria. However, for an identical range of stress triaxialities, the studied austenitic steel alloy was only reliably predicted by third family criteria. This study showed that ferritic and austenitic steel alloys are independent and dependent on the Lode parameter, respectively. Barsoum and Al-Khaled [40] as well as Baghous and Barsoum [41] studied the Lode parameter influence in ductile failure of steel alloys. Two materials were investigated, namely, SA-106 and SA790 steels having an ultimate tensile strength of 453MPa and 781MPa, respectively. They experimentally concluded that SA-106 and SA790 steels were independent and dependent on the Lode parameter, respectively. Interestingly, the authors introduced a Lode sensitivity paramater that quantified the Lode parameter effect on ductile failure. This parameter was expressed as, $S_L = \frac{\bar{\varepsilon}_f(\eta = 0.5, L = 0)}{\bar{\varepsilon}_f(\eta = 0.5, L = -1)}$. The authors reviewed data from the litterature for numerous steel alloys. They concluded that ductile failure of steel alloys having an ultimate strength lower and higher than 550MPa, was sensitive and insensitive to the Lode parameter, respectively. It has to be pointed out that stress triaxialities higher than 0.5, are usually considered within the range of high stress triaxialities. At this range, ductile failure is considered insensitive to the Lode parameter. In conclusion, recent works [20, 40] showed that ductile failure of steel alloys having an ultimate strength higher than 550MPa depended on the Lode parameter at high stress triaxialities.

Calibration of uncoupled damage models is commonly achieved by testing specimens exhibiting a large range of stress state, such as butterfly, notched tensile bars, and small punch specimens [3, 31, 42]. These specimens cover a large range of stress triaxialities and Lode parameters. In particular, Oh *et al.* [18] and DÅ¾ugan *et al.* [20] calibrated the uncoupled damage models testing such specimens. However, the pipeline's thickness must be sufficiently large to machine these samples within the pipe. Herein, the thickness of the pipe was 2.9mm. This thickness involved that the mechanical tests were performed on unconventional specimens. As a result, calibration was performed on pipe-ring notched specimens, as shown in Figure 5. Multiple notch geometries were machined on specimens, in order to cover a range of stress triaxialities and Lode parameter representative of corroded pipelines.

The main objective of this work was to predict ductile failure of pipe-ring notched AISI 316L using uncoupled damage models. Failure was considered to occur at ductile crack initiation. Calibration of the uncoupled models was performed on pipe-ring notched specimens, where the notches mimic the geometry of corrosion defects. To the authors knowledge, calibration using such specimens has never been undertaken in the literature dealing with ductile failure of pressurized pipes. Two criteria were calibrated, namely, Johnson-Cook (J-C) and Lou-Huh (L-H). For these criteria the D_c value is 1.0.

evaluate the sensitivity of the Lode parameter on ductile failure of the studied steel.

This paper is organized as follows: Section 2 presents the material, the methodology, as well as the numerical and the experimental procedures. Section 3 provides the results and the corresponding discussion. Finally, Section 4 concludes the work.

Table 1: Failure criterion functions (*f*) of Johnson-Cook (J-C) and Lou-Huh (L-H).

Name	Failure criterion function *f*	Fitting parameters
J-C [29]	$\frac{1}{\varepsilon_f}, \varepsilon_f = C_1 + C_2 \cdot e^{-\eta \cdot C_3}$	C_1, C_2, and C_3
L-H [34]	$\frac{1}{C3}\left(\frac{2}{\sqrt{L^2+3}}\right)^{C_1} \cdot \left(\frac{\max(1+3\cdot\eta, 0)}{2}\right)^{C_2}$	C_1, C_2 and C_3

2. Material, methodology and procedures

2.1. Material

This study focused on the AISI 316L stainless steel. Specimens were machined from a raw pipeline having a 76.1mm nominal diameter and a 2.9mm nominal thickness. Tensile tests were carried out on unstandardized tensile specimens, shown in Figure 1. Tensile specimens were tested to characterize the elasto-plastic behaviour to be further implemented in the finite element models.
Tensile properties were characterized in the longitudinal direction, shown in Figure 1. Three specimens were tested on a servo-hydraulic Dartec traction machine of 300KN capacity. These tests were performed at room tempature with a strain rate of 0.01 s^{-1}. Local deformations were computed using the digital image correlation technique.
The digital image correlation technique is an optical method that provides full-field displacement by tracking multiple images of the gage section photographed during the test. The nominal strain in the longitudinal direction was then computed from the measured full-field displacement with a gage length of 12mm. The images were photographed by two AVT PIKE F505 cameras with Schneider Kreuznach 50mm lens. The system resolution was 4Mpx. The subset size and the step-filter sizes product were 19px and 65px, respectively. These values were obtained after performing a sensitivity analysis [43].
Figure 2 shows the tensile stress-strain curves characterized in the longitudinal direction. Table 2 provides the tensile properties in the longitudinal direction, as defined by ASTM E8-13a standard [44]. It has to be noted that the pipe-ring notched specimens were loaded in the tangential direction. The likely anisotropy was corrected by changing the tensile properties in order to fit the numerical force-displacement data on the experimental force-displacement data of the tested notched specimens.

Figure 1: Geometry of the unstandardized tensile specimen (a) in 2D (b) in 3D. All dimensions are in mm.

Figure 2: Tensile stress-strain curves characterized in the longitudinal direction.

Table 2: Average tensile properties and standard deviation in the longitudinal direction of the pipeline.

Tensile strength,	Yield strength at 0.2% offset,	Elongation at fracture,
MPa	MPa	%
636 ± 4.2	302 ± 13	111 ± 15

This study aimed at predicting ductile failure of pipe-ring notched specimens. To that end, uncoupled damage models were calibrated using results from pipe-ring notched testing. Three geometrical defect shapes were machined, namely, U-shape, V-shape, and rectangular shape. These defects are shown in Figure 3. The paramaters *P* ($P = H + h$), θ, and ϕ, shown in Figures 3a and 3b, are the depth of the notch, the angle defining the height of the cone, and the diameter of the cone, respectively. The parameter *R*, shown in Figures 3b and 3c, is the fillet radius set to 0.1mm and 0.5mm, respectively. The value of 0.1mm and 0.5mm were an electro-machining and a machining constraints, respectively. Finally, the parameters C_e, H_e, and l_e, shown in Figure 3c, are the length of the defect in the longitudinal direction, the depth of the defect in the radial direction, and the width of the defect in the orthoradial direction, respectively.

Figure 3: Shape of the studied defects.

Figure 4: Matrices of experimental design for the studied shape. Filled circles are the selected configurations. Unfilled squares are not tested configurations.

In order to cover the ranges of the Lode parameter and the stress triaxiality representing the local stress state of corroded defects, two experimental matrices were designed. These experimental matrices were designed for the U-shape and the rectangular shape. The U-shape and the rectangular shape were selected to reproduce *in-situ* corrosion defects. U-shape and rectangular shape were observed on corroded pipelines. Nevertheless, the corresponding machined notches were ideal representation of *in-situ* corrosion defects. As a consequence, the local stresses of machined notches may have slightly differed from those of *in-situ* corrosion defects. The experimental matrix related to the V-shape was not designed since V-shape was not observed on the corroded pipes. A single V-shape configuration was machined on a sample to increase the range of stress triaxiality and Lode parameter.

The experimental matrices were designed with *H*, θ, ϕ, C_e, H_e, and l_e paramaters for the U-shape and the rectangular shape. The parameter *h* was mathematically deduced from setting *H*, θ, and ϕ parameters. Figures 4a and 4b show the experimental design matrices for the U and the rectangular defect shapes, respectively.

The U-shape experimental matrix was designed based on the most severe notch; $(H, \theta, \phi) =$ (1.0mm, 180°, 3.0mm). This geometry was set to have a notch depth representing 80-85% of the pipeline's thickness. Three values per parameter were then set to limit the number of tests. The rectangular shape experimental matrix was designed similarly to the U-shape matrix. In addition, the fillets radius was set to 0.5mm. As a consequence, configurations given by $H_e =$ 0.5mm and $l_e =$ 0.5mm were not tested. Indeed, these configurations provided a cylindral shape. The corresponding configurations are shown in unfilled squares in Figure 4b.

The number of defect configurations with these matrices of experiment was 39. A numerical study was performed using finite element method to reduce the number of tests. The finite element model is further described in Section 2.4.4. This study consisted in comparing the stress triaxialities and the Lode paramaters of all configurations. Indeed, defect configurations having similar stress triaxiality and Lode paramater paths shall fail similarly.

The stress triaxialities and the Lode paramaters were extracted at nodes having the maximum equivalent plastic strain. When multiple defect configurations had similar stress triaxiality and Lode parameter paths, one configuration was maintained. Similarity was defined when two paths had average values in the range of ± 5 %. This hypothesis is valid in case of fracture at the maximum equivalent plastic strain. However, materials do not always fracture at the maximum equivalent plactic strain [1], and thus this study could be improved by using a damage criterion to locate the number of defect configurations from 39 to 4. Table 3 summarized the tested configurations with the corresponding serie number. As mentioned, a V-shape configuration was added to the U-shape and rectangular shape configurations. Finally, an extra U-shape configuration was also added to validate the calibrated models. The corresponding serial number is #0.

Table 3: Summary of the tested configurations with the corresponding serial number. The ligament column provides the ligament length, which is the thickness of the sample minus the defect depth.

Serie number	Defect shape	H or H_e	θ or C_e	ϕ or l_e	Ligament
Serie #0	U-shape	0.5mm	180°	2.0mm	1.4mm
Serie #1	U-shape	1.0mm	180°	3.0mm	0.4mm
Serie #2	U-shape	2.0mm	180°	1.0mm	0.4mm
Serie #3	V-shape	1.0mm	180°	3.0mm	0.4mm
Serie #4	Rectangular shape	2.5mm	2.5mm	2.5mm	0.4mm
Serie #5	Rectangular shape	2.5mm	10.0mm	2.5 mm	0.4mm

2.3. Experimental procedure

Experimental tests on pipe-ring notched specimens were inspired from Al-Khaled and Barsoum [45] and Dick and Korkolis [46] studies. Figure 5 provides the geometry of the unnotched sample. The filled black circles localise the defect position, which was at the center of the gage section. Herein, the gage section was numerically designed to prevent edge effect in terms of equivalent plastic deformation. To that end, a pipe-ring specimen was modeled using the finite element method with the largest defect, *i.e.* the rectangular shape with the following dimensions: $H_e = 2.5$mm, $C_e = 10.0$mm, and $l_e = 2.5$mm. The width of the gage section was set when the gage section edges were unaffected by plastic deformation. Figure 6 shows the equivalent plastic strain computed using finite element method for the largest defect when the final width was set. The finite element model is further described in Section 2.4.4. Three pipe-ring notched specimens were tested per serie, which are defined in Table 3.

The pipe-ring notched specimens were tested on a servo-hydraulic Dartec tensile machine of 300KN capacity. These tests were performed at room tempature with a strain rate of 0.05 s^{-1}. Two connectors were designed to test the specimens using the Dartec tensile machine. Figure 7a shows the experimental setup with these connectors. Figure 7b shows another perspective of the experimental setup. This perspective shows the angle between the loading axis and the pipe center-pipe gage section center axis. This angle, herein named the traction angle, was set to 49.9°. This value was a trade-off between camera observation of the gage section and having a connectors-sample contact during testing. This contact was required to prevent undesired bending stress during testing [46].

Finally, minimization of the friction between the connectors and the sample reduced the magnitude of the tensile force, and had negligible influence on stress triaxiality, Lode parameter, and plastic strain [45]. As a result, two layers of Teflon, 0.12mm thick, were inserted between the connectors and the sample for lubrication purpose. Silicone grease was also spread between the Teflon layers with a similar purpose.

mm.

Figure 6: Equivalent plastic strain computed using finite element method with the largest defect on the optimized pipe-ring specimen.

Figure 7: Experimental setup. The pipe-ring notched specimen was tested using two connectors. Lighting was used to increase image quality.

Figure 8: Example of a digital image correlation output showing the displacements. The relative connectors displacement was measured using an optical sensor.

The force was measured using a load cell. Misalignment and bending were assumed ineffective. The relative connectors displacement was measured using an optical sensor. The optical sensor was created using the digital image correlation technique. This technique is described in Section 2.1. Figure 8 shows the optical sensor measuring the relative displacement of the connectors.

2.4. Damage model calibration, validation, and verification procedures

The uncoupled damage models were calibrated using the tests of the series #1, #3, and #4. Figure 9 shows the range of stress triaxialities and Lode parameters covered by the studied defect configurations, represented in symbols. The predictive capabilities of the calibrated uncoupled damage models were evaluated by comparing the experimental ($PEEQ_{exp,\ failure}$) and the numerical ($PEEQ_{num,\ failure}$) equivalent plastic strains at failure. Note that $PEEQ_{exp,\ failure}$ and $PEEQ_{num,\ failure}$ were assessed using finite element simulations. The experimental and numerical terminologies were used when ductile failure was experimentally and numerically estimated, respectively.

Figure 9: Common stress triaxiality versus Lode parameter at fracture. The strips indicate the average stress triaxialities and Lode parameters range covered by the studied defect configurations. The symbols indicate the defect configurations used to calibrate the Lou-Huh and Johnson-Cook criteria.

2.4.1. calibration

The calibration of an uncoupled damage model consists in finding the set of fitting parameters $C = \{C_1, \ldots, C_m\}$ that minimizes the F function given by:

$$\min_{C_1,\ldots C_m} \|F\|^2 = \min_{C_1,\ldots C_m} \left(F_1^2 + \ldots + F_j^2 + \ldots + F_E^2\right) \qquad (5)$$
$$\text{with } F_j = \left(D_c^j - 1.0\right), \forall j \in K,$$

where D_c^j is the damage indicator at failure of the j^{th} calibration test and K the number of calibration tests. The trapezoidale rule technique was used to approximate the integral in Equation (1). The approximated integral $\left(D_{c,app}\right)$ is given by:

$$D_c \approx D_{c,app} = \frac{\overline{\varepsilon_f}}{N}\left[\frac{f(\overline{\varepsilon_p} = 0) + f(\overline{\varepsilon_p} = \overline{\varepsilon_f})}{2} + \sum_{k=1}^{N-1} f\left(\overline{\varepsilon_p} = \frac{k \cdot \overline{\varepsilon_f}}{N}\right)\right], \qquad (6)$$

where N is the number of subintervals within the full interval $[0; \overline{\varepsilon_f}]$. All variables are defined in Section 1. In addition, the stress triaxialities and the Lode parameters used in Equation (6) were stored to subsequently compute their average values.

experimental failure indicator is used to estimate the displacement of the connectors at the initiation of the ductile crack. (b) The experimental force-displacement curve of the connectors is numerically correlated. (c) The equivalent plastic strain, the stress triaxiality, and Lode parameter at $d_{num} = d_{exp}$ are extracted at the node having the maximum equivalent plastic strain or the maximum stress triaxiality. These extractions are used to calibrate the ductile failure criteria.

The calibration requires the knowledge of the loading histories ($X = \{X_1, \ldots, X_n\}$ from 0 to $\bar{\varepsilon}_f$). To that end, a hybrid experimental-numerical technique was used [47–52]. Figure 10 provides a schematic illustration of this technique. Firstly, this technique consists in determining experimentally the ductile failure, herein the ductile crack initiation, using an experimental failure indicator, as shown in Figure 10a. Section 2.4.2 presents the proposed experimental failure indicators in details. Secondly, the corresponding test is modeled using finite element method, and the experimental force versus displacement curve is numerically correlated, as shown in Figure 10b. Section 2.4.4 describes the finite element modeling in details. Thirdly, the loading history ($X = \{X_1, \ldots, X_n\}$ from 0 to $\bar{\varepsilon}_f$) is extracted from the finite element simulation for a numerical connectors displacement equal to the experimental connectors displacement at ductile crack initiation, as shown in Figure 10c.

In the present study, the loading history was represented by the equivalent plastic strain versus the stress triaxiality and the Lode parameter. The numerical locus extraction was performed at the surface node, where ductile crack experimentally initiated. However, these results were inconsistent with ductile failure modeling, as exposed in Section 3.3.1. The numerical locus extraction was then performed at the node having the maximum stress triaxiality within the ligament. These results provided consistant results with ductile failure modeling, as exposed in Section 3.3.1.

2.4.2. Experimental failure indicators

An experimental failure indicator was required to calibrate the uncoupled damage models and to assess the experimental equivalent plastic strains at failure. The purpose of this indicator was to evaluate the ductile crack initiation. Three indicators were proposed.

The first indicator was based on the use of the first and second derivative of the force versus the connectors displacement curve. This indicator rested upon the idea that failure would occur when the force dropped. The connectors displacement was experimentally measured using the optical sensor defined in Section 2.3. Failure was defined by the second derivative of the force of the connectors displacement reaching -2.0 $KN^2.mm^{-2}$. This value was arbitrary set. Figure 11 shows an example of the first experimental indicator failure. This indicator provided inconsistent results, since failure experimentally occured before the force drop. The smallness of the machined defects relative to the gage section was the cause.

The second indicator was based on the use of the raw images photographed for the digital image correlation technique. Failure was defined by the appearence of a crack. Figure 12 shows an example of the second experimental indicator failure. This indicator was manually identified. This indicator is thus user-dependent.

Figure 11: Example of the experimental failure indicator based on the second derivative of the force with respect to the displacement.

Figure 12: Example of the experimental failure indicator based on visual tracking. Image (1) shows the shape of the defect when the connectors displacement is null. Image (2) shows the shape of the defect at surface crack initiation, assumed to be at failure. Images (3) and (4) show the defect shape at higher displacements.

defined as the separation of the sample’s gage section into two parts. The 61% value was set using the procedure explained in Secion 3.1.

2.4.3. Verification and validation

The calibrated uncoupled damage models were verified by comparing the experimental and the numerical equivalent plastic strains at failure for series #1, #3, and #4. The calibrated uncoupled damage models were validated by comparing the experimental and the numerical equivalent plastic strains at failure for series #0, #2, and #5.

Numerical equivalent plastic strain at failure was evaluated by computing Equation (6) for every increment and every node within the gage section of the corresponding finite element simulation. The first node reaching $D = 1.0$ in the finite element simulation was considered as the node where ductile fracture occured. The location’s equivalent plastic strain at $D = 1.0$ was considered as the numerical failure strain. This strain was then compared to the experimental equivalent plastic strain at failure. The experimental equivalent plastic strain at failure was extracted from the same finite element simulation at $d_{num} = d_{exp}$, and at the node having the maximum stress triaxiality within the ligament.

2.4.4. Finite element modeling

Elasto-plastic finite element simulations were performed using Abaqus/CAE 2018 software. An implicit integration scheme was employed to solve the corresponding system accounting for geometric non linearity.

Figure 13 shows the model. The connectors were modeled using two rigid parts. Half of the specimen was modeled and a symmetry boundary condition was imposed along the Z-axis. The specimen was divided into three parts: A, B, and C. The goal was to refine the mesh around the notch without interfering with the rest of the specimen. These parts were connected using â□˜*TIEâ□™ constraints. The element sizes for parts A, B, and C were 0.2mm, 0.4mm, 0.8mm, respectively. These element sizes were identified after performing a mesh convergence study. The parts A and B were meshed by 20-nodes quadratic brick elements with reduced integration (C3D20R elements). The FR regions in part C, shown in Figure 13, were meshed using 15-node quadratic triangular prism with reduced integration (C3D15 elements). The rest of the part C was meshed with C3D20R elements. The goal was to reduce element distortion in the FR area.

The angle axis was set to 49.9°, which was used experimentally. The interaction between the rigid elements and the specimen was defined by using a hard contact relationship in the normal direction. The coefficient of friction between the rigid elements and the specimen was arbitrary set at 0.2 to represent the experimental lubricated state. Al-Khaled and Barsoum [45] indicated that the friction coefficient barely affected the Lode parameter, the stress triaxility, and the plastic deformation at the notch section. These parameters were extracted to calibrate the uncoupled damage models. As a result of the negligible effect of the friction coefficient on these parameters, this coefficient was arbitrary set to 0.2 rather than being experimentally measured.

Figure 13: Finite element modeling of a U-shape defect. FR stands for fillet radius.

A Johnson-Cook law was implemented in Abaqus/CAE 2018 software to model the elasto-plastic behaviour. This law was experimentally characterized and was confidential.

3. Results and discussion

3.1. Selection of the experimental failure indicator

crack initiated. The first indicator was based on the force drop, assuming that the crack would initiate simultaneously. The second indicator was based on the visual appearence of the crack. Figures 11 and 12 present both indicators determined for the same test. For this test, the first and the second indicators provided a crack initiation at $d_{exp} = 11.9$ mm and $d_{exp} = 9.3$ mm, respectively. The first indicator overestimated the crack initiation by at least 28%, when compared to the visual indicator. Additionally, the second indicator was an upper bound of crack initiation, since this indicator set visually the failure moment. As a consequence, the first indicator was unable to accurately determine the crack initiation. The smallness of the machined defects relative to the gage section was the cause. Indeed, the failure of these small defects uninfluenced the force evolution. As a result, the first indicator was dismissed.

The second indicator provided a surface crack initiation. This indicator overestimated subsurface crack initiation. In addition, the use of this indicator was lengthy for industrial application. Investigations were undertaken to propose a reliable indicator of ductile crack initiation. A serie of indicators was proposed based on the force versus the gage section displacement curves and their derivatives. Figure 14 shows the multiple displacements that were studied to determine a reliable indicator. This serie of indicators overestimated results similarly to the first indicator. As a result, this serie of indicators was dismissed.

Figure 14: Measured orthoradial displacements using the digital image correlation technique to determine a reliable crack initiation indicator.

Table 4 provides the connectors displacements when a crack was visually identified and at fracture. Fracture is defined in Section 2.4.2. Series #0 and #2 provided inconsistent results. The connectors displacements estimated by the second indicator were excessively close to the fracture displacements for both series, when compared to series #1, #3, #4 and #5. The visual indicator most likely overestimated crack initiation for series #0 and #2. Samples from serie #0 had a ligament length of 1.4mm, as shown in Table 3. This length could have resulted in a subsurface crack that would have been observed at the surface around the fracture moment. Samples from serie #2 had the narrowest defect shape. This shape could have resulted in a crack hardly observable. As a consequence, the visual indicator was assumed to overestimate ductile crack initiations for series #0 and #2.

Table 4 provides the V/F ratio, which is the ratio of the connectors displacements estimated with the second indicator and at fracture. These ratios were close to 1.0 for series #0 and #2 as a consequence of the visual indicator overestimation. The V/F ratios were lower for serie #5 than for series #1, #3 and #4. Ductile crack initiated significantly early for the tests of serie #5. In addition, the tests data from serie #5 were unusable, as discussed in Section 3.2. As a consequence, results from serie #5 were dismissed.

The second indicator was assumed to provide accurate results for series #1, #3 and #4, since these series had similar and consistent V/F ratios. In order to homogenise ductile crack initiations, a third indicator was proposed. This indicator assumed that the connectors displacement at the ductile crack initiation was a percentage of the connectors displacement at fracture. This percentage was the minimum of the V/F ratios among series #1, #3 and #4, *i.e.* 61%. The minimum value was selected to have conservative modeling. The third indicator was used to evaluate the connectors displacement at crack initiation for every serie except serie #5. Serie #5 tests had a different indicator than the other series because crack initiation appeared when the connectors displacement was around 25% of the connectors displacement at fracture. As a consequence, the third indicator would overestimate crack initiation for serie #5 tests. The results are shown in Table 4.

the connectors displacements at crack initiation estimated with the 2^{nd} indicator and at fracture. N/O and N/A stand for not observed and not applicable, respectively.

Serie	Test	2^{nd} indicator,	Fracture,	V/F ratio	3^{rd}, indicator
		mm	mm		mm
	Test #1	20.81	21.24	0.98	12.96
	Test #2	21.06	21.41	0.98	13.06
Serie #0	Test #3	21.35	21.88	0.98	13.35
	Test #1	9.20	12.29	0.75	7.50
	Test #2	6.90	11.33	0.61	6.91
Serie #1	Test #3	7.20	11.70	0.62	7.14
	Test #1	17.04	18.56	0.92	11.32
	Test #2	N/O	19.90	N/A	12.14
Serie #2	Test #3	N/O	17.80	N/A	10.86
	Test #1	6.96	11.40	0.61	6.95
	Test #2	8.72	10.80	0.81	6.59
Série #3	Test #3	8.97	12.10	0.74	7.38
	Test #1	12.40	16.59	0.75	10.12
	Test #2	10.30	14.91	0.69	9.10
Serie #4	Test #3	11.56	15.46	0.75	9.43
	Test #1	1.10	4.03	0.27	N/A
	Test #2	1.10	4.36	0.25	N/A
Serie #5	Test #3	0.85	3.93	0.22	N/A

In summary, the third indicator was used to assess the experimental connectors displacement for series from #0 to #4. The visual indicator was used to assess the experimental connectors displacement for serie #5. However, a simpler and more reliable indicator should be investigated in the future. The direct current potential-drop technique [53] could be a candidate. However, this technique is heavy for industrial application and complex geometries, since it requires the knowledge of the deformed geometry during testing. Indeed, the deformed geometry impacts the electrical resistivity.

3.2. Correlation of numerical and experimental data

Experimental tests were modeled using finite element method to calibrate, verify, and validate the damage models.

Figures 15 and 16 show the force-connectors displacement curves for the series used to calibrate and validate the damage models, respectively. The black lines represent the data from the finite element simulations. The dotted curves represent the experimental data. The crosses indicate the experimental ductile crack initiation estimated in Section 3.1. The numerical and experimental data were reasonably correlated for series #0, #1, #2, #3, and #4. However, the finite element simulations slighty overestimated the hardening parts. This overestimation probably affected the damage modeling.

observed on the experimental curves, followed by a force increase. These drops coincided with the experimental visual crack detections. The crack initiation may be due to thin remaining ligament distributed over a large area. The following force increase was caused by the deformation of the lateral sides, which were undeformed during the initial force increase. Uncoupled damage models do not take into account the softening behaviour of the material. As a consequence, the finite element simulation of serie #5 was unable to correlate the corresponding experimental tests. The designed pipe-ring notched specimen was probably inadequate for the defect shape of the specimens of serie #5. The use of such large geometry defect should be avoided in future studies.

Investigation should be performed to standardize the pipe-ring notched specimen. The goal would be to avoid unsuccessful tests as was the case for serie #5.

Figure 15: Correlation of numerical and experimental data for the series calibrating the studied uncouple damage models.

Figure 16: Correlation of numerical and experimental data for the series evaluating the predictive capability of the studied uncouple damage models.

3.3. Uncoupled damage models

3.3.1. Numerical extraction locus

The uncoupled damage models were calibrated using the tests of series #1, #3, and #4.

The calibration was performed following the hybrid experimental-numerical technique described in Section 2.4.1. The first step was to determine d_{num} values. These values were the minimum among d_{exp}, provided in Table 4, for a given serie in order to be conservative, which is important for industrial application. These values were 6.91mm, 6.95mm, and 9.10mm for series #1, #3, and #4, respectively.

Figure 17: Equivalent plastic strain (PEEQ) outline computed by finite element for series #1, #3, and #4 at failure.

The second step was to determine the numerical locus extraction. The numerical locus extractions were initially performed at the surface nodes where the ductile crack were experimentally initiated. Figure 17 shows the initial locus extractions for series #1, #3, and #4. These loci coincided with the maximum equivalent plastic strain (PEEQ) for series #1 and #3. The maximum PEEQ for series #1 and #3 are shown in Figures 17a and 17b, respectively. Table 5 provides the related average η and L computed through the loading history, as well as $PEEQ_{exp,\ failure}$. Serie #3 had higher η and $PEEQ_{exp,\ failure}$ values than serie #1. However, failure modeling involved $PEEQ_{exp,\ failure}$ should decrease when η increased. The results from Table 5 were inconsistent with ductile failure modeling [29, 34]. This inconsistency meant that the cracks probably initiated underneath the surface.

Table 5: Average values of the stress triaxiality (η) and the Lode parameter (L) of series #1, #3, and #4 at the surface nodes where the crack initiation was experimentally observed. $PEEQ_{exp,\ failure}$ is the experimental equivalent plastic strain at failure.

Serie	η	L	$PEEQ_{exp,\ failure}$
Serie #1	0.51	-0.13	0.84
Serie #3	0.68	0.09	1.6

Figure 18: Equivalent plastic strain (PEEQ) and stress triaxiality (η) versus normalized ligament length for series #1 and #3.

Table 6: Average values of the stress triaxiality (η) and the Lode parameter (L) of series #1, #3, and #4 at the nodes having the maximum η within the ligament. $PEEQ_{exp, failure}$ is the experimental equivalent plastic strain at failure.

Serie	T_x	L	$PEEQ_{rupture}$
Serie #1	0.52	-0.44	0.73
Serie #3	0.75	-0.65	0.59
Serie #4	0.56	-0.25	0.55

Ductile failure depended on PEEQ and η values. The PEEQ and η were plotted against the normalized ligament length for series #1 and #3. These graphes are shown in Figure 18. While the maximum equivalent plastic strain was at the surface, the maximum stress triaxility was subsurface. Since the nodal extraction at the surface provided inconsistent results, a nodal extraction was achieved at the node having the maximum η. Indeed, ductile failure depends on PEEQ and η values. Table 6 provides the related average η and L computed through the loading history, as well as the experimental equivalent plastic strain at failure ($PEEQ_{exp, failure}$). The results were consistent with ductile failure modeling. As a result, cracks were assumed to initiate at the nodes having the maximum η within the ligament. The loading histories were extracted at these nodes to calibrate the uncoupled damage models.

3.3.2. Calibration and verification

Johnson-Cook

The Johnson-Cook criterion was calibrated using the methodology described in Section 2.4.1. Figure 19 shows the prediction of the Johnson-Cook criterion along with the experimental loading histories of the series used to calibrate the model. Experimental loading history was the loading history plotted until $PEEQ = PEEQ_{exp, failure}$. Table 7 shows the $D_{c,app}$ values computed using Equation (6) with $\overline{\varepsilon}_f = PEEQ_{exp, failure}$ for series #1, #3, and #4. $D_{c,app} = 1.0$ involved the criterion coincided with the data. Table 7 shows that Johnson-Cook model was unable to perfectly fit the data of series #1, #3, and #4, even though the errors are reasonable. Indeed, $D_{c,app}$ values were 1.06, 0.99, and 0.95 for series #1, #3, and #4, respectively.

Figure 19: Curve failure predicted by the Johnson-Cook criterion as a function of η. The curves with the symbols represent the experimental loading history of the series used for the calibration.

Table 7: Verification of modeling. $D_{c,app}$ values were computed with the experimental equivalent plastic strains at failure for the series used for the criteria calibration.

	$D_{c,app}$		
Criterion	Serie #1	Serie #3	Serie #4
Johnson-Cook	1.06	0.99	0.95
Lou-Huh	1.00	1.00	1.00

symbols represent $PEEQ_{exp,\ failure}$ versus the average values of η and L of the series used for the calibration.

Lou-Huh

The Lou-Huh criterion was calibrated using the methodology described in Section 2.4.1. Figure 20 shows the prediction of the Lou-Huh criterion along with $PEEQ_{exp,\ failure}$ versus the average values of η and L of the series used to calibrate the criterion. Table 7 shows the $D_{c,app}$ values computed using Equation (6) with $\overline{\varepsilon_f} = PEEQ_{exp,\ failure}$ for series #1, #3, and #4. $D_{c,app} = 1.0$ involved that the criterion coincided with the data. Table 7 shows that Lou-Huh criterion was able to perfectly fit the data of series #1, #3, and #4. This was a consequence of the two parameters (η and L) formulation provided by the Lou-Huh model.

Table 7 shows that the Lou-Huh criterion fit the experimental data better than the Johnson-Cook criterion. This involved that $PEEQ_{exp,\ failure}$ depended on the Lode parameter at high stress triaxialities. This was consistent with the work of Barsoum and Al-Khaled [40] and DÅ¾ugan *et al.* [20] which showed that ductile failure was sensitive to the Lode parameter for steel alloys having an ultimate tensile strength higher than 550MPa. The ultimate tensile strength of the studied stainless steel alloy was 636MPa.

The Johnson-Cook and Lou-Huh criteria were valid in the ranges of stress triaxialities and Lode parameters covered by the tests used for the calibration. An extrapolation outside of these intervals could lead to incorrect failure prediction.

3.3.3. Validation

The validation consisted in comparing $PEEQ_{exp,\ failure}$ with $PEEQ_{num,\ failure}$.

Johnson-Cook

Figure 21 shows the prediction of the Johnson-Cook criterion along with the experimental loading histories of the studied series. The Johnson-Cook criterion overestimated $PEEQ_{exp,\ failure}$ of series #0, #2, #3, #4 and #5. Table 8 provides the predicted $PEEQ_{num,\ failure}$ along with the relative errors to $PEEQ_{exp,\ failure}$. The $PEEQ_{num,\ failure}$ predictions were satisfying for series #1, #2, #3, and #4, with an average of the absolute relative errors of 3.3%. However, the $PEEQ_{num,\ failure}$ predictions were unsatisfying for series #0 and #5.

The Johnson-Cook criterion was unable to predict failure for serie #0. The necking numerically appeared in the area of fillet radius shown in Figure 13, while failure appeared experimentally in the gage section. The finite element model and most likely the implemented elasto-plastic law was the cause. Indeed, the correlation of the numerical and experimental data, shown in Figure 16, were not perfectly correlated. The reported $D_{c,app}$ value was the maximum value within the gage section.

$PEEQ_{exp,\ failure}$ was overestimated for serie #5. The finite element model and most likely the implemented elasto-plastic law was also the cause. Indeed, the finite element model was unable to correlate the experimental data.

with the symbols represent the experimental loading history of the studied series. Experimental loading history was loading history plotted until $PEEQ_{exp,\ failure}$. Loading history of Serie #5 was plotted until the experimental equivalent plastic strain estimated using the visual indicator (visual ind.).

Table 8: $PEEQ_{num,\ failure}$ values predicted by the Johnson-Cook (J-C) and Lou-Huh (L-H) criteria. The values in brackets are the relative errors between $PEEQ_{exp,\ failure}$ and $PEEQ_{num,\ failure}$. When the criterion does not predict failure the calculated value of $D_{c,app}$ within the gage section is provided.

	Serie					
Criterion	#0	#1	#2	#3	#4	#5
J-C	no failure	0.69	0.67	0.56	0.60	0.61
	$D_{c,app} = 0.76$	(-4%)	(3.4%)	(1.9%)	(3.9%)	(69%)
L-H	no failure	0.73	0.65	0.55	0.59	0.57
	$D_{c,app} = 0.48$	(1%)	(0.2%)	(1.0%)	(1%)	(59%)

Lou-Huh

Figure 22 shows the prediction of the Lou-Huh criterion along with $PEEQ_{exp,\ failure}$ versus the average values of η and L of the studied series. The Lou-Huh criterion overestimated $PEEQ_{exp,\ failure}$ of series #0, #2, #3, #4 and #5. Table 8 provides the predicted $PEEQ_{num,\ failure}$ along with the relative errors to $PEEQ_{exp,\ failure}$. The $PEEQ_{num,\ failure}$ predictions were satisfying for series #1, #2, #3, and #4, with an average of the absolute relative errors of 0.8%. However, the $PEEQ_{num,\ failure}$ predictions were unsatisfying for series #0 and #5. The finite element models and most likely the implemented elasto-plastic law were also the causes, as explained above for the Johnson-Cook criterion.

Figure 22: Failure surface predicted by the Lou-Huh model as a function of η and L. The symbols represent $PEEQ_{exp,\ failure}$ versus the average values of η and L of the studied series. $PEEQ_{exp,\ failure}$ of serie #5 was estimated using the visual indicator (visual ind.).

Table 8 shows that the Louh-Huh criterion was more accurate than the Johnson-Cook criterion at predicting $PEEQ_{exp,\ failure}$. However, The η and L values were too few to really fit any meaningful ductile failure locus with. An extrapolation outside of these intervals could lead to incorrect failure prediction.

4. Conclusion

The goal of this work was to predict ductile failure of pipe-ring notched AISI 316L specimens. This work was divided into an experimental part and a numerical part.

The conclusion regarding the experimental part was firstly to find a simple and a reliable ductile crack initiation indicator usable for industrial application. Herein, three approaches were proposed. The first appoach was based on the derivative curves of the force versus the connectors displacement curve. The second was based on the raw images of the gage section. The third was based on a percentage of

application in an industrial context. More work is required in order to develop a reliable approach for industrial application. The direct current potential-drop technique could be a candidate.
The conclusion regarding the experimental part was secondly to develop a standardized pipe-ring notched specimen. Herein, a pipe-ring notched specimen was proposed. However, the tests related to serie #5 provided unusable results. In addition, the machined defects of the other tests were relatively small in comparison with the gage section. This led to failures that uninfluenced the force evolution. The failure's dependency on the force evolution would have been more convenient to estimate crack initiation. More work is required in order to develop a standardized pipe-ring notched specimen in order to avoid the issues encountered in this study.

The numerical part confirmed a Lode parameter sensitivity at high stress triaxiality, as observed in the literature. As a consequence, the Lou-Huh criterion predicted ductile fracture more accuretly than the Johnson-Cook criterion.

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