On the determination of forming limits in thin-walled tubes

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\textbf{ABSTRACT}

This paper proposes a methodology to determine the formability limits of thin-walled tubes and to plot them in principal strain space and in the space of effective strain vs. stress-triaxiality. Digital image correlation (DIC), combined either with time-dependent methodologies or strain-force approaches, is utilized to identify the onset of failure by necking and obtain the corresponding limit strains. Thickness measurements and determination of the gauge length strains across the cracked regions are utilized to characterize the onset of fracture and to evaluate the fracture limit strains.

Results show that the utilization of tube expansion with rigid punches and elastomers allow obtaining strain loading paths and fracture loci by necking and fracture across a wide range of tube forming conditions ranging from biaxial stretching in the first quadrant to pure tension in the second quadrant of principal strain space. The fracture forming line (FFL) is the first time ever determined for thin-walled tubes. The forming limit curve (FLC) and the FFL resemble those of sheet and strip materials and their use is of paramount importance in the design and optimization of tube forming processes.

1. Introduction

In the past years, the growing interest in high-strength, lightweight structures for transportation vehicles, aerospace and civil engineering has been promoting the use of tubes, namely thin-walled tubes \cite{1}. Tube bending and end forming operations are the most widely used to fabricate thin-walled structural parts but other processes, such as tube hydroforming \cite{2}, have also gained a role in mass production of lightweight tubular parts.

Novel processes such as incremental tube forming \cite{3} and friction-spinning \cite{4} also show potential to become alternative manufacturing solutions capable of reducing energy and raw material consumption. In this regard, there is a need to characterize the formability limits and predict failure in tube forming.

There are two main approaches to achieve this goal: (i) establishing process workability windows for a range of operation parameters \cite{5} and (ii) setting up failure limits in the Forming Limit Diagram (FLD), in a similar way to what is done in sheet forming.

As in metal sheets, both approaches are dependent on the material and the initial fabrication process, but the first is specific to each tube forming process \cite{6} whereas the second is independent of the tube forming process by which the formability limits were determined \cite{7}.

In sheet forming, the formability limit by necking (FLC), was originally developed by Keeler \cite{8} and Goodwin \cite{9}, as a general tool to evaluate the formability of a metal sheet. In tube forming, several experimental studies were performed to determine FLC's by means of tube bulging \cite{10,11} and tube hydroforming with varying strain loading paths \cite{12,13}. These investigations made no distinction between necking and fracture and considered that the limiting strains were the maximum strains measured on the tube surface.

The introduction of the onset of fracture in the FLD was proposed by Embury and Duncan \cite{14} and has been gaining importance due to recent developments in incremental sheet forming, which can ensure stable deformation until fracture without previous necking \cite{15}.

However, the possibility of attaining strain values beyond the formability limit by necking is not exclusive of sheet forming. Davis et al. \cite{16} observed that tube hydroforming of seamless AA-6260-T4 tubes were capable of withstanding strains significantly larger than those corresponding to the onset of necking of the tubular material. More recently, Song et al. \cite{17} concluded that theoretically determined FLC's using three different necking criteria also do not fit well against the experimental strains attained in tube bulging.

All these results and observations point towards the fact that tube forming, similarly to sheet forming \cite{18}, can also fail by fracture with postponing or suppression of necking. Because of this, authors have

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recently proposed an experimental procedure for the determination of the critical strains and ductile damage at the onset of failure by fracture for the special case of tube expansion undergoing pure tension [19]. The procedure was later extended to tube reduction and inversion [20]. Under these circumstances, the main objective of this paper is to present a methodology to determine the formability limits of thin-walled tubes by necking and fracture associated to mode I of fracture mechanics (in-plane tension). This is the first attempt, as far as the authors are aware, to perform a simultaneous determination of the formability limits by necking and fracture in tubular materials.

The presentation draws from tensile tests aimed at characterizing the mechanical behaviour along the longitudinal and transversal tube directions to a series of experiments comprising tube expansion with rigid punches and elastomers. The non-quadratic, anisotropic, Hosford plasticity criterion is utilized to convert the strain loading paths from principal strain space to the space of effective strain vs. stress-triaxiality and to plot the corresponding formability limits by necking and fracture.

2. Experimentation

2.1. Tensile tests

The investigation was carried out on commercial AA6063-T6 aluminium tubes with 20 mm outer radius. The average wall thickness $t_0$ calculated from measurements taken every $45^\circ$ was $1.9 \, \text{mm}$, in order to span the entire circular cross section (Table 1). The $0^\circ$ reference of the graphical representation of tube wall thickness variation shown in Table 1 coincides with the most visible weld resulting from the extrusion with porthole die that was utilized to produce the supplied tubes.

The mechanical properties and stress–strain curve of the tubular material were determined by means of longitudinal (Fig. 1a) and transversal (also known as ‘ring hoop’) (Fig. 1b) tensile tests. The specimens were machined out of the supplied tubes and their surfaces were electrochemically etched along their active loading directions.

The longitudinal tensile test specimens with 50 mm gauge length were prepared in accordance to the ASTM E8/E8M standard [21]. Special fixing clamps were employed not to plastically deform the tube ends during fixing in the universal testing machine (refer to the detail in Fig. 1a).

The tube wall thickness variation along the circular cross section was taken into consideration by using longitudinal tensile test specimens with their axis taken at every $45^\circ$ along the perimeter.

The transversal tensile test specimens were prepared in accordance to a sub-size geometry of the ASTM E8/E8M standard [21] in order to accommodate their reduced area in one of the D-shaped blocks (or, semi-cylinders) that hold the specimen and move freely when loading is applied (refer to ‘2’ in Fig. 1b). This is necessary to prevent undesirable bending during the tensile test [22].

The transversal tensile test specimens were lubricated with polytetrafluoroethylene (teflon®) sheets to reduce the friction between the specimen and the D-shaped blocks.

The tensile tests were performed at room temperature in a universal testing machine Instron 5900R.
Table 2
Summary of the experimental work plan for the expansion of AA6063-T6 aluminium tubes with rigid punches (notation in accordance with Fig. 2).

<table>
<thead>
<tr>
<th>Test geometry</th>
<th>Tube material</th>
<th>Operating conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Outer radius $r_0$ (mm)</td>
<td>Thickness $t_0$ (mm)</td>
</tr>
<tr>
<td>ERP 15</td>
<td>20</td>
<td>1.9</td>
</tr>
<tr>
<td>ERP 30</td>
<td>20</td>
<td>1.9</td>
</tr>
<tr>
<td>ERP 45</td>
<td>20</td>
<td>1.9</td>
</tr>
</tbody>
</table>

2.2. Tube formability tests

The formability limits by necking and fracture were determined by means of tube expansion with rigid punches and elastomers. The tests were performed at room temperature on a hydraulic testing machine Instron SATEC 1200 kN in quasi-static operating conditions.

2.2.1. Tube expansion with rigid punches

In the expansion tests with rigid punches, the leading edge of the tubes undergoes pure tension up to failure due to progressive circumferential stretching as the conical punch surfaces are forced into the tubes [19].

The experimental setup and notation are shown in Fig. 2 and the work plan is summarized in Table 2. The tests were performed in a random order and at least three repetitions were made for each punch geometry in order to ensure reproducibility of the results.

2.2.2. Tube expansion with elastomers

In the expansion tests with elastomers, the tube ends were clamped, and a cylindrical rubber plug was placed inside the tube and compressed with a punch to expand (bulge) the tubes up to fracture [23]. The experimental setup and notation are shown in Fig. 3a and b, and the main detail of the clamping system is given in (c).
active tool components are identified as: (i) the punch, (ii) the dies, (iii) the clamping system and (iv) the cylindrical rubber plug.

The clamping system is made of three blocks (Fig. 3c) with knurled surfaces to provide enough grip to hold the tubes in position and prevent longitudinal displacements during testing. The blocks are connected to each other by springs that are regulated by three fixing screws (refer to Fig. 3c).

The cylindrical rubber plug acts as a pressure transmitting medium for tube expansion and by changing the free length of the tubes $l_{neq}$ (i.e., the gap distance between the two dies along which the tube is free to deform) it is possible to ensure loading conditions from pure tension deformation to equi-biaxial stretching.

The work plan is summarized in Table 3 and the overall experimental methods and procedures were similar to those utilized in the expansion of tubes with rigid punches.

### Table 3

<table>
<thead>
<tr>
<th>Test geometry</th>
<th>Tube material</th>
<th>Operating conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Outer radius $r_0$ (mm)</td>
<td>Thickness $t_0$ (mm)</td>
</tr>
<tr>
<td>EEL 10</td>
<td>20</td>
<td>1.9</td>
</tr>
<tr>
<td>EEL 20</td>
<td>20</td>
<td>1.9</td>
</tr>
<tr>
<td>EEL 30</td>
<td>20</td>
<td>1.9</td>
</tr>
</tbody>
</table>

![Fig. 4. Schematic representation of the setup utilized by the DIC system for measuring the strains in a tube expansion with a rigid punch.](image)

### 2.3. Strain measurements

Strain measurements on the vicinity of the zones where cracks are opened in both tensile and tube formability tests were performed with a digital image correlation (DIC) system from Dantec Dynamics –model Q-400 3D (Fig. 4).

The surfaces of the specimens were sprayed with a stochastic black speckle pattern on a uniform background previously painted in white. The tensile test specimens were illuminated with one spotlight, whereas the tube forming specimens were illuminated with two spotlights to allow a wider measurement region.

The DIC system is equipped with 2 cameras with 6 megapixels of resolution and 50.2 mm of focal lenses with an aperture of f/11. The frequency of image acquisition was set to 20 frames per second and the correlation algorithm was performed with the INSTRA 4D software. A facet size of 13 px with a spacing grid of 7 px was considered.

### 3. Methodology

The proposed methodology is aimed at providing the formability limits by necking and fracture in the principal strain space and in the space of effective strain vs. stress-triaxiality.

#### 3.1. Formability limits

The formability limits by necking (FLC) are determined by means of DIC. The instant of time corresponding to the onset of necking is identified by means of a time-dependent approach or, if not possible, by means of an alternative approach that combines the evolution of the strains and forces with punch displacement (Fig. 5).

The time-dependent approach utilized by the authors (Fig. 5a) makes use of the experimental strains measured by DIC and of the maximum strain rate value to identify the instant of time corresponding to the onset of failure necking. The approach is based on the original work of Martínez-Donaire et al. [24] in sheet forming.

The alternative approach combining the experimental strains measured by DIC and the force-displacement evolutions (Fig. 5b) is based in the work on tube expansion with rigid punches by Cristino et al. [25] and had been originally developed by Magrinho et al. [26] for bulk forming tests. The approach (that will be hereafter denoted as ‘combined strain-force’) is based on the identification of the amount of displacement after which the force drops and the evolution of major and minor strains remain stationary.

Utilization of DIC to obtain strains in the necking region after it forms and, therefore, close to the fracture, provides strain values that cannot be considered the fracture strains. Moreover, such measurements suffer from sensitivity to the location of the selected points where measurements are to be performed owing to the inhomogeneous deformation in the neighbourhood of the crack. As a result of this, the experimental procedure for constructing the formability limits by fracture (FFL) requires measuring of thickness at cracking in order to obtain the ‘gauge length’ strains. These strains were obtained from individual measurements of thickness along the cracks with a stereomicroscope Nikon SMZ800 with a magnification of 20x using KAPPA Image Base Metreo 2.7.2.

The procedure is schematically shown in Fig. 6a and the thickness strains at fracture $\varepsilon_{gF}$ are calculated by,

$$\varepsilon_{gF} = l_0 \left( \frac{t_F}{t_0} \right)$$  (1)

where $t_0$ is the initial thickness and $t_F = \frac{1}{n} \sum_{i=0}^{n} t_F^i$ the average fracture thickness of the specimens.

In case fracture is preceded by necking, the minor strain $\varepsilon_2$ (meridional strain), is assumed to remain constant after the last measurement by DIC and the major strain $\varepsilon_1$ (circumferential strain), is obtained by incompressibility under plane strain deformation conditions (Fig. 6b),

$$\varepsilon_{1F} + \varepsilon_{2F} + \varepsilon_{gF} = 0 \Leftrightarrow \varepsilon_{1F} = -(\varepsilon_{2F} + \varepsilon_{gF})$$  (2)

#### 3.2. Graphical representation of the formability limits

Graphical representation of the strain loading paths in principal strain space is obtained by merging the time (or displacement) evolutions of the major and minor strains determined by DIC. If test geometries are properly selected, it is possible to plot strain loading paths ranging from the first to the second quadrant and to represent the onset of failure by necking as a V-shaped FLC, similar to that commonly depicted in sheet forming.

The formability limits by fracture are determined from the gauge length strains along the crack regions of the specimens after testing by
means of the experimental technique that was previously described in Section 3.1.

The plot of both formability limits (necking and fracture) in principal strain space for the AA6063-T6 aluminium tubes under investigation is disclosed in Section 4.

The transformation of the experimental strain loading paths and of the corresponding formability limits from the principal strain space to the space of effective strain vs. stress-triaxiality requires determining the effective strain $\dot{\varepsilon}$, the effective stress $\bar{\sigma}$ and the average stress $\sigma_m$ for all the points belonging to the experimental strain loading paths. The transformation to be presented in what follows, is built upon the non-quadratic plasticity criterion proposed by Hosford [27] and assumes proportional loading and tube deformation under plane stress deformation conditions ($\sigma_2 = \sigma_3 = 0$) along the thickness direction.

The non-quadratic plasticity criterion proposed by Hosford [27] was originally derived for anisotropic sheet metals and considers planar anisotropy and the principal axes of the stress and strain tensors to coincide at each point,

$$ r_0(\sigma_2 - \sigma_3)^2 + r_0(\sigma_3 - \sigma_1)^2 + r_0\sigma_1(\sigma_1 - \sigma_2)^2 = \sigma_m(1 + r_0)\delta_\sigma $$

where the plastic potential $f(\sigma_\varepsilon)$ is taken as the Hosford’s plasticity criterion and $d\varepsilon$ is an instantaneous, nonnegative, proportionality factor related to the material stress–strain curve, the following ratios of strain increment are obtained,

$$ \dot{\varepsilon}_1 : \dot{\varepsilon}_2 : \dot{\varepsilon}_3 = -r_0(\sigma_3 - \sigma_1)^{\omega-1} + r_0r_0(\sigma_1 - \sigma_2)^{\omega-1} : r_0(\sigma_2 - \sigma_3)^{\omega-1} - r_0r_0(\sigma_1 - \sigma_2)^{\omega-1} : -r_0(\sigma_2 - \sigma_3)^{\omega-1} + r_0r_0(\sigma_1 - \sigma_2)^{\omega-1} $$

(5)

Considering thin-walled tubes to be loaded under plane stress conditions ($\sigma_3 = 0$), the plasticity criterion (3) can be rewritten as follows,

$$ r_0\sigma_2^2 + r_0\sigma_1^2 + r_0r_0(\sigma_1 - \sigma_2)^2 = \sigma_m(1 + r_0)\delta_\sigma $$

(6)

Fig. 5. Schematic representation of the two approaches to identify the onset of failure by necking and determine the corresponding necking strains. (a) Time-dependent approach [24]. (b) Combined strain and force displacement approach [25].

If the strain loading path $\beta$ and the stress ratio $\alpha$ are defined as follows,

$$ \beta = \frac{\dot{\varepsilon}_2}{\dot{\varepsilon}_1}, \quad \frac{\epsilon_2}{\epsilon_1}, \quad \frac{\sigma_2}{\sigma_1} $$

it follows from Eqs. (6) and (7) that the effective stress, $\bar{\sigma}$ is given by,

$$ \bar{\sigma} = \left[ \frac{\sigma_m + r_0\alpha^2 + r_0r_0(1 - \alpha^2)}{r_0(1 + r_0)} \right]^{1/\omega} $$

(8)

Fig. 6. Schematic representation of the approach to determine the formability limits by fracture. (a) Measuring the specimen’s thickness along the crack. (b) Fracture strain pair may result from localization after necking or from progressive reduction of thickness.

Now, considering the incremental plastic work per unit of volume $dw^2$ to be expressed as,

$$ dw^2 = \sigma_1\sigma_1 + \sigma_2\sigma_2 = \partial \varepsilon d\varepsilon $$

(9)

and substituting Eqs. (9) and (10), the effective strain increment $d\varepsilon$ becomes,

$$ d\varepsilon = \frac{\sigma_1\sigma_1 + \sigma_2\sigma_2}{\bar{\sigma}} = \frac{1 + \alpha\beta}{\left[ \frac{r_0(1 + r_0^{1/\omega})}{r_0r_0\alpha^2} \right]^{1/\omega}}d\varepsilon_1 $$

(10)

Then, considering the flow rules with plane stress loading conditions ($\sigma_3 = 0$) and taking the strain loading path $\beta$ and the stress ratio $\alpha$ into...
consideration [31], the ratios of strain increment (Eq. (5)) may be written as,
\[
d\varepsilon_1 : d\varepsilon_2 : d\varepsilon_3 = 1 : -1 + \beta = 1 + r_0 (1 - a)^{1 - e} : \frac{2 a}{r_0} a^{1 - e} - r_0 (1 - a)^{1 - e} = - \frac{2 a}{r_0} a^{1 - e} - 1
\]
(11)

with the relation between the strain loading path \(\beta\) and the stress ratio \(a\), given by,
\[
\beta = \frac{d\varepsilon_2}{d\varepsilon_1} = \frac{r_0 (1 - a)^{1 - e} - r_0 (1 - a)^{1 - e}}{r_0 + r_0 (1 - a)^{1 - e}}
\]
(12)

Considering that the AA6063-T6 aluminium tubes utilized in the investigation have a FCC crystal structure \((\beta = 8)\), Eqs. (8), (10) and (12) become,
\[
\varepsilon = \left[\frac{r_0 + r_0 a^3 + r_0 (1 - a)^{1 - e}}{r_0 (1 + r_0)^{1/8}}\right]^{1/3}
\]
(13)
\[
d\varepsilon = \frac{1 + a}{r_0 (1 + r_0)} \left[\frac{r_0 + r_0 a^3 + r_0 (1 - a)^{1 - e}}{r_0 (1 + r_0)^{1/8}}\right]^{1/3} d\varepsilon_1
\]
(14)
\[
\beta = \frac{r_0 a^3 - r_0 (1 - a)^{1 - e}}{r_0 + r_0 (1 - a)^{1 - e}}
\]
(15)

Finally, writing the mean stress \(\sigma_m\) for plane stress loading conditions \((\sigma_3 = 0)\) as,
\[
\sigma_m = \frac{\sigma_1 + \sigma_2 + \sigma_3}{3} = \frac{\sigma_1 + \sigma_2}{3} = \frac{(1 + a)}{3} \sigma_1
\]
(16)

it is possible to use Eqs. (13) and (16) to express the stress triaxiality ratio,
\[\eta = \frac{\sigma_m}{\sigma_1}\] as follows,
\[
\eta = \frac{\sigma_m}{\sigma_1} = \frac{(1 + a) [r_0 (1 + r_0)]^{1/8}}{3 [r_0 + r_0 a^3 + r_0 (1 - a)^{1 - e}]^{1/8}}
\]
(17)

Eqs. (14) and (17) will be later used to transform the strain loading paths and the formability limits from principal strain space to the space of effective strain vs. stress-triaxiality using, for the first time ever, the Hosford’s plasticity criterion [27].

4. Results and discussion

4.1. Material characterisation

The average mechanical properties obtained for the entire set of tensile tests performed along the longitudinal and transverse directions are summarized in Table 4.

<table>
<thead>
<tr>
<th>Test geometry</th>
<th>Thickness (mm)</th>
<th>(\sigma_T) (MPa)</th>
<th>(\sigma_{UTS}) (MPa)</th>
<th>A (%)</th>
<th>K (MPa)</th>
<th>(\epsilon_0)</th>
<th>n</th>
<th>(r_{avg})</th>
</tr>
</thead>
<tbody>
<tr>
<td>TL – Longitudinal</td>
<td>1.90</td>
<td>164.4</td>
<td>204.1</td>
<td>44.10</td>
<td>300.0</td>
<td>0.002</td>
<td>0.125</td>
<td>0.58</td>
</tr>
<tr>
<td>TT – Transversal</td>
<td>1.90</td>
<td>163.5</td>
<td>238.2</td>
<td>58.60</td>
<td>408.5</td>
<td>0.008</td>
<td>0.216</td>
<td>1.19</td>
</tr>
</tbody>
</table>

Fig. 7. Average true stress - true strain curves obtained from longitudinal and transversal tensile tests.

The parameters, \(K, \epsilon_0\) and \(n\) of the above equation are listed in Table 4.

The differences between the average stress-strain curves shown in Fig. 7 are attributed to frictional effects in the transverse tensile test [22] and to differences in the anisotropy coefficient \(r_{avg}\) obtained from the tensile tests along the longitudinal and transversal directions (Table 4).

4.2. Formability limits by necking

The principal strain pairs at the onset of necking for longitudinal and transversal tensile testing were determined by means of the time-dependent methodology proposed by Martínez-Donaire et al. [24]. This was the first time ever that necking strains defining the FLC of tubes were determined from transversal tensile tests.

Fig. 8a presents the experimental evolutions of the major strain at the onset of necking (points A and B) and of the major strain rate for point B. The evolutions were obtained from DIC and Fig. 8b shows an image of the distribution of the major strains at the onset of necking, indicating the location of points A and B.

The main conclusion arising from Fig. 8 is that the time-dependent methodology can be successfully applied to transversal tensile tests. Similar conclusion is obtained for the longitudinal tensile tests.

However, the attempts made by the authors to extend the application of the time-dependent methodology to tube expansion were not successful due to oscillations of the major strain rate that make impossible the identification of the maximum strain rate value (Fig. 9).

Alternative time-dependent methodologies based on other derivative procedures of the main variables would also suffer from the same problem and, therefore, their straight application would also reveal inappropriate for tube expansion.

In addition to this, it is worth mentioning that alternative approaches based on the observation and analysis of the displacements on the tube surface during expansion would also experience difficulties due to the existence of non-negligible strain gradients along the tube end caused by non-uniform frictional conditions.

All this prevented authors from identifying the instant of time corresponding to the onset of necking by means of the above mentioned
well-established procedures and required the use of the combined strain-force approach proposed by Cristino et al. [25]. This approach was briefly introduced in Section 3.1 and requires the experimental evolutions of major and minor strains with displacement to be measured in points located at the vicinity of the necked regions.

Fig. 10 discloses the application of the combined strain-force approach to case ‘ERP30’ of Table 2 (tube expansion with a rigid punch having a 30° semi-angle of inclination). As seen, the force increases with displacement up to a peak after which drops as a result of a sudden relief of stresses at necking and subsequent cracking. The peak in force is aligned with the maximum values of major or minor strains measured by the DIC system. After the peak both strains remain stationary.

As a result of this, the amount of displacement (and the instant of time) corresponding to the onset of necking is clearly identified by the dashed vertical line included in Fig. 10a.

Application of the combined strain-force approach to tube expansion with elastomer is shown in Fig. 11. As seen, not only the approach proved successful in the determination of the onset of failure by necking as it shows a strong dependency of strains from the free length of the tubes $l_{\text{free}}$. For example, a tube with $l_{\text{free}} = 30$ mm is deformed under near plane strain deformation whereas a tube with $l_{\text{free}} = 10$ mm is expanded under biaxial strain loading conditions.

This last result is very important because it opens the possibility of combining tube expansion with rigid punches and elastomers to obtain strain loading paths and strains at the onset of necking across the entire principal strain space. In this sense, the utilization of tube expansion with rigid punches and elastomers to determine the FLC of tubular materials bears a resemblance to the well-known use of Nakajima or Marciniak stretch forming tests to determine the FLC of sheet and strip materials [32].

The major and minor strain-displacement evolutions for expansion tests with elastomers showed a non-uniform evolution (Fig. 11b) due to non-uniform descendent movement of the cylindrical rubber plug caused by high friction.
4.3. Formability limits by fracture

Fig. 12 presents the results obtained from the application of the proposed methodology to the determination of the fracture strains in tube expansion with a rigid punch. As seen, a point located in the cracked region (point B of Fig. 12b) undergoes pure tension up to the onset of necking, after which experiences localization and rapid plane strain deformation until failure by fracture. The strains at fracture are determined by measuring the gauge length strains in the cracked region after testing and employing the procedure that was previously described in Section 3.1.

The strain loading path for point B undergoes pure tension for a long period after the onset of diffuse necking (Fig. 12a) due to the rigid punch geometry. The conical geometry of the punch causes a stabilizing effect on higher strains at the tube end due to the lower strains experienced by the nearest regions above it, as claimed by Daxner et al. [33].
Fig. 12. Tube expansion with a rigid punch (Case ERP30 of Table 2). (a) Strain loading path for points A and B located at the vicinity of necking and in the cracked region, respectively. (b) Distribution of the major strain at the onset of necking obtained from DIC.

Fig. 13. Strain loading paths and formability limits of the AA6063-T6 aluminium tubes by necking (FLC) and fracture (FFL) in principal strain space. Tensile and formability tests according to Tables 2–4.

4.4. Formability limits in principal strain space

The methodology described in Section 3.1 and applied in previous Sections 4.2 and 4.3 allowed plotting the strain loading paths obtained by DIC for all the test cases listed in Tables 2–4, as well as the formability limits by necking (FLC) and fracture (FFL) of the AA6063-T6 aluminium tube material (Fig. 13).

The FLC was built from the open markers corresponding to the strain pairs at the onset of necking. The FFL was built from the solid markers corresponding to the gauge length strains at fracture and required measuring the thickness of the specimens along the cracked regions.

As seen in Fig. 13, the slope of the strain loading paths of the longitudinal and transversal tensile tests are close to pure tension and the small differences found between the two paths are attributed to different values of the anisotropy coefficient in both directions (Table 4).

The experimental strain loading paths of tube expansion with rigid punches are also close to pure tension and the main reason why there are slightly deviated to the left side of the principal strain space is because of friction. In fact, friction at the contact interface between the tube and punch leads to slightly more compressive strains along the meridional direction.

The differences in the punch semi-angles of inclination reveal no influence in the strain loading paths, as previously reported by Centeno et al. [19]. In contrast, tube expansion with elastomers is very sensitive to the free length $l_{\text{free}}$ of the tubes. Larger values of $l_{\text{free}}$ originate deformation under plane strain loading conditions whereas small values of $l_{\text{free}}$ originate deformation under biaxial strain. This means that by reducing the free length of the tubes, the meridional strain increases, and deformation is able to span across the entire first quadrant of principal strain space.

The FLC depicted in Fig. 13 is approximated by V-shaped curve whereas the FFL is approximated by a straight line with a negative slope $-0.9$. Both results are similar to those observed in sheet metal forming. This is in good agreement with critical ductile damage at fracture being controlled by stress-triaxiality and the FFL being a line falling from left to right with a negative $-1$ theoretical slope in principal strain space [18].

4.5. Formability limits in the space of effective strain vs. stress-triaxiality

The space of effective strain vs. stress-triaxiality relates ductility of a material, which is commonly defined by the strain at fracture, with the ratio of the first invariant of the stress tensor (hydrostatic stress) and the second invariant of the deviatoric stress tensor (effective stress). It allows characterizing the formability limits as a function of the stress state [34] and provides a direct link between critical ductile damage, fracture toughness and formability limits at fracture [35].

Fig. 14 presents the loading paths and the formability limits of the AA6063-T6 aluminium tubes by necking and fracture in this space. The strain values for each point were obtained by DIC and the effective stress and stress-triaxiality were calculated by means of the analytical
procedure based on the Hosford plasticity criterion that was presented in Section 3.2 (refer to Eqs. (14) and (17)).

Considering the void-growth damage-based criterion proposed by McClintock [36] that is associated with crack opening by tension (mode I of fracture mechanics) [18], it is possible to link the fracture limit with the critical value of damage,

\[ D^F_{\text{crit}} = \int_0^{\beta} \frac{\sigma_{\text{crit}}}{\delta} d\tilde{e} \]  

(19)

This is performed by determining the critical experimental value of ductile damage at the onset of failure by fracture \( D^F_{\text{crit}} \), by calculating the area between the FFL and the effective strain axis for a specific value of stress triaxiality, under the assumption of proportional loading. The procedure should be understood as an approximation, because in real tests loading is not proportional and, therefore, stress triaxiality ratio is not constant.

Such approximation is shown in Fig. 14 for a strain loading path corresponding to plane strain deformation (refer to the grey area associated to the longitudinal tensile tests) and the associated critical ductile damage \( D^F_{\text{crit}} \) is equal to 0.225.

5. Conclusions

A new methodology to determine the formability limits of tubular materials by necking and fracture was successfully applied to tube forming. The onset of failure by necking was determined by application of time-dependent and strain-force procedures on DIC measurements. The onset of failure by fracture involved determination of the gauge length strains along the cracked regions and was, for the first time ever, plotted in the principal strain space and in the space of effective strain vs. stress-triaxiality.

The transformation of the formability limits and strain loading paths from principal strain space to the space of effective strain vs. stress-triaxiality by means of an analytical procedure based on the non-quadratic plasticity criterion due to Hosford [27] revealed appropriate, because the anisotropy coefficients of tubes can only be easily determined at 0° and 90° with relation to the extrusion direction.

The choice of tube expansion with rigid punches and elastomers allows determining the strain loading paths and the fracture loci across a wide range of tube forming conditions. This result resembles the use of Nakajima tests to determine the failure limits in sheet and strip materials across the entire range of strain loading conditions and provides an easy and straightforward procedure to be applied in thin-walled tube forming.

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