Evaluation of ductile fracture models for different metals in blanking

A.M. Goijaerts*, L.E. Govaert, F.P.T. Baaijens

Materials Technology, Eindhoven University of Technology, PO Box 513, 5600 MB Eindhoven, The Netherlands

Received 1 October 1999; received in revised form 7 November 2000; accepted 27 November 2000

Abstract

This study is focussed on the evaluation of ductile fracture methodologies, which are needed to predict product shapes in the blanking process. In an earlier publication [Goijaerts et al., J. Manuf. Sci. Eng., Trans. ASME 122 (2000) 476], two approaches were elaborated using local ductile fracture models. The first strategy incorporates the characterisation of a ductile fracture model in a blanking experiment. The second methodology is more favourable for industry. In this approach, instead of a complex and elaborate blanking experiment, a tensile test is used to characterise a newly proposed criterion, which was shown to predict accurately the ductile fracture for different loading conditions. In this paper, finite element simulations and experiments are performed on both tensile testing and blanking to evaluate the validity of both approaches with corresponding criteria for five different metals. In the blanking process, different clearances as well as different cutting radii of the tools are considered. In conclusion, it can be stated that the first approach gives very good results close to, or within the experimental error for all five materials. The second approach, the more favourable one for industry, yields good results that deviate slightly more over the range of metals.

Keywords: Blanking; Ductile fracture; FEM; Metals; Tensile testing

1. Introduction

Blanking is a common technique in high volume production. Since the beginning of this century, researchers have been analysing the blanking process. Blanking experiments on either planar [2,3] or axisymmetric [4–7] configurations have led to empirical guidelines for process variables such as punch and die radius, speed and clearance. Nevertheless, the blanking process is not yet fully understood.

Nowadays, it can be observed that product specifications are getting more severe. This may lead to lengthy trial and error procedures in the development of industrial blanking applications, which is based on empirical knowledge. Therefore, a proper model of the blanking process is desired. Because of the constantly changing loading situations in the material, the process is too complex for an analytical approach [8–10]. Instead, the finite element method has been used to simulate the blanking process [11–13]. One major difficulty in the numerical analysis is the accurate description of ductile fracture initiation, which greatly determines the product shape (Fig. 1).

The physical background for ductile fracture in metals is known to be the initiation, growth and coalescence of voids [14–16]. Voids can initiate at inclusions, secondary phase particles or at dislocation pile-ups. Brokken [17] showed with numerical simulations that even under large hydrostatic pressures, voids can initiate at inclusions in shear deformation. Growth and coalescence of voids are driven by plastic deformation. Consequently, in the modelling of ductile fracture initiation, the deformation history is required.

In this paper, the class of local ductile fracture criteria that incorporate the stress and strain history are applied for the prediction of ductile fracture initiation (a short overview of these criteria is given in [18]). They can be written in a general form as an integral over equivalent plastic strain reaching a threshold value $C$:

$$\int_{\tilde{\varepsilon}_p} f(\sigma) \, d\tilde{\varepsilon}_p = C$$

(1)
where \( f(\sigma) \) can be interpreted as a function of the invariants of the Cauchy stress tensor \( \sigma: f(J_1, J_2, J_3) \). If the integral on the left-hand side reaches the critical value \( C \) during the process, ductile fracture is supposed to initiate. In the blanking process, this initiation determines the height of the shear zone (Fig. 1) and thus the shape of the blanked edge. In the formulations for the different criteria, some parameters that influence ductile fracture are expected to appear: plastic strain and triaxiality (triaxiality is defined as hydrostatic stress divided by equivalent Von Mises stress: \( \sigma_h/\sigma \)). It is well known that hydrostatic pressure postpones ductile fracture initiation which is generally rationalised by the effect of this stress state on void initiation and void growth [14–17]. Therefore, triaxiality is often represented in \( f(\sigma) \). Large plastic strains permit voids to grow and coalesce. This justifies the integration over plastic strain.

In the formulation of Eq. (1), \( C \) is generally regarded as a material constant. One has to perform an experiment to determine \( C \) and then it should be possible to use the characterised criterion in any desired application, the criterion ought to be valid for both the characterisation experiment and the application. Unfortunately, there were no examples found in the literature where the critical value \( C \) is determined under a loading condition other than that of the actual application. In other words, existing ductile fracture criteria are only successful when they are both characterised and applied under similar loading conditions. This suggests that some information of the loading path is represented by the parameter \( C \).

The FEM model of the blanking process that is used in this study [13] (incorporating a material model with isotropic hardening) is extensively verified on both deformation fields and process forces [19,20]. In a previous publication [1], using the aforementioned FEM model, two approaches were elaborated to predict fracture initiation in the blanking process, using specific formulations of Eq. (1), for a ferritic stainless steel. In one approach, \( C \) is determined in the blanking process. No existing ductile fracture criteria were found to yield satisfactory results. However, very good results were achieved within the experimental error over a wide range of clearances [1] when the Rice and Tracey [21] criterion was adapted. The second approach involved characterisation of \( C \) in a tensile test, which represents in a methodology that is more favourable for industrial applications. For this approach, a new criterion was proposed that showed good results within 6% of the blanking experiments. Moreover, this new model could also predict ductile fracture initiation for tensile tests under different superposed hydrostatic pressures (different triaxialities). This is important, because it shows that the criterion is valid for different loading conditions.

The two aforementioned approaches showed good results for a ferritic stainless steel. The remaining question was [1] whether the utilised criteria contain more material-dependent parameters than only the critical \( C \). Therefore, in this paper, these two approaches were evaluated for four different materials using exactly the same methodology, including the material characterisation.

In Section 2, we explain the methodologies to characterise ductile fracture initiation in both blanking and tensile testing. In Section 3, we discuss the experimental and numerical methods needed. In Section 4, the results are presented for all five materials. Finally, we discuss the results and conclude in Section 5.

### 2. Methodologies to characterise ductile fracture

In the following, the experimental determination of the critical parameter \( C \) is referred as the characterisation of local ductile fracture criteria with the general formulation of Eq. (1). Two approaches will be utilised (as in [1]): determination of \( C \) in a blanking experiment and characterisation of the criterion in a tensile test. These methodologies are explained in Sections 2.1 and 2.2.
2.1. Characterisation in blanking

We consider ductile fracture initiation criteria in the formulation of Eq. (1). The right-hand side of this formulation is assumed to be a material constant. The characterisation of a ductile fracture model in the blanking process can be performed with the four following steps:

1. For one clearance, the punch displacement at ductile fracture initiation is experimentally determined.
2. That particular blanking process is simulated with a validated FEM model of the blanking process [1] and the left-hand side of Eq. (1) is updated and stored as a field variable.
3. When the experimental punch displacement at fracture is reached in the simulation, C is determined to be the maximum value of \( \int f(\sigma) \, d\varepsilon_p \) over the entire FEM mesh.
4. At the end of the simulation (the moment of fracture initiation), we check if the location of the maximum value of \( \int f(\sigma) \, d\varepsilon_p \) is in agreement with the experimental location of ductile fracture initiation.

If this final check is satisfied, we declare the ductile fracture initiation criterion to be characterised.

If a ductile fracture initiation model is characterised, we can evaluate the validity of it for the blanking process over the entire range of clearances. This evaluation is performed using FEM simulations of the blanking process for the other clearances. During the simulations \( \int f(\sigma) \, d\varepsilon_p \) is again updated and stored as a field variable and as soon as this field variable reaches the critical C, the punch displacement at fracture is predicted. Goijaerts et al. [1] showed that for X30Cr13 no existing criterion could predict ductile fracture initiation in the blanking process satisfying well, following this strategy. Moreover, the application of the criterion defined in Eq. (2) exhibited deviations in the order of 30%, which was regarded not to be acceptable. Therefore, a new criterion was proposed:

\[
\int_{\varepsilon_p} \left( 1 + 3.9 \frac{\sigma_p}{\sigma} \right) \, d\varepsilon_p = C_G
\]

where the brackets \([\cdot]\), defined as

\[
[x] = \{ x, \ x > 0, \ 0, \ x \leq 0 \}
\]

are used to ensure that the criterion cannot decrease. This newly posed criterion performed well in the blanking process with maximum deviations of only 7% for the predicted punch displacement at fracture over a range of clearances for X30Cr13. Moreover, it was capable of predicting ductile fracture initiation for tensile tests under superposed hydrostatic pressures up to 500 MPa. This indicates that the criterion of Eq. (3) captures the influence of triaxiality on ductile fracture initiation correctly, at least for X30Cr13.

3. Methods

In this section all methods, needed for the methodologies of Section 2, are explained. In Section 3.1, five materials were presented for which the characterisations were performed. For one of these materials, X30Cr13, the strategies of Section 2 were elaborated in an earlier publication [1]. The experimental results for X30Cr13, presented in that publication, are used to explain the experimental techniques in Section 3.2. In Section 3.3, the FEM models of the blanking process and the tensile test are presented.

3.1. Materials

The two aforementioned methodologies were elaborated for the blanking process to characterise ductile fracture
initiation for X30Cr13 [1]. To verify these strategies, exactly the same procedures were applied to four other metals which are presented, along with X30Cr13 in Table 1. X30Cr13 is a ferritic stainless steel and 316Ti is an austenitic stainless steel. Ms64 is a brass that is often used in cutting and forming processes. DC04 is a high quality steel with a large formability that is applicable in deepdrawing processes. Al51ST is employed often in cutting and has a low formability (is relatively brittle).

3.2. Experimental

In order to obtain a satisfying material description, also for large plastic strains, a material characterisation technique is used, which is briefly explained in Section 3.2.1 with the help of the results for the aforementioned ferritic stainless steel (X30Cr13). To characterise and verify ductile fracture initiation criteria of the form of Eq. (1), experiments were needed. As was done previously [1] an axisymmetric blanking set-up is used with different geometries (Section 3.2.2). We chose to vary the clearance because the effect on the product shape of a change in clearance is known to be large [4,5,7].

3.2.1. Material characterisation

As was done earlier for X30Cr13, all materials considered were assumed to be plastically deformed according to the Von Mises yield condition with isotropic hardening. In formulating this plastic deformation, the yield stress increases with increasing equivalent plastic strain. The relationship between the yield stress and the equivalent plastic strain is difficult to be obtained experimentally for large strains, using conventional tests such as tensile or shear experiments. This relationship is determined by performing approximately 10 tensile tests with each tensile specimen being subjected to a different amount of rolling to obtain different initial plastic deformations. The assumption of isotropic hardening allows the addition of the rolling and tensile equivalent plastic strains. We quantified the relationship between the yield stress and the equivalent plastic strain, by fitting a mastercurve through the maxima of the stress–strain curves of these tensile tests. For X30Cr13, the results are presented in Fig. 2. At the left-hand side, the true stress–true strain results of 17 tensile tests are presented with their maxima (triangles), together with a number of points (circles) of the tensile test of as-received material. At the right, a mastercurve is fitted through the markers. In this fitting procedure, the circles are essential to correctly capture the hardening behaviour for small strains. For this fitting procedure, the power law of the Nadai [22] hardening model does not suffice because it only incorporates two constants to be determined and cannot be accurately fitted along the markers. The hardening model of the Voce type [23] involves three material parameters but grows to a constant yield stress level for large strains. Therefore, this model is phenomenologically expanded with a square root and a linear term in strain so that different hardening behaviour for large strains can be captured:

$$\sigma_y = \sigma_{y,0} + M_1 (1 - e^{-\bar{e}_p/M_2}) + M_3 \sqrt{\bar{e}_p} + M_4 \bar{e}_p$$

where $\sigma_y$ is the Von Mises yield stress, $\sigma_{y,0}$ the initial yield stress, $M_1$–$M_4$ are material constants and

$$\bar{e}_p = \sqrt{\left(\frac{3}{2}\right)\varepsilon_{pp} : \varepsilon_{pp}}$$

the equivalent plastic strain, with $\varepsilon_{pp}$ the double inner product of the logarithmic plastic strain tensor with itself.

![Fig. 2. Fitting procedure for the hardening curve of X30Cr13 [1]. Left: the separate tensile curves are presented and their maxima (triangles), together with a number of points on the as-received tensile curve (circles). Right: the mastercurve is fitted through all markers.](image-url)
3.2.2. Blanking experiments

An axisymmetric blanking set-up with a die-hole diameter of 10 mm, including a blankholder with constant pressure, was built [1] with five different punches (diameters: 9.98, 9.94, 9.88, 9.80 and 9.70 mm) resulting in five different clearances, covering the industrially used range of clearances (1, 3, 6, 10 and 15% of the sheet thickness of 1 mm). To avoid exorbitant simulation times, the cutting radii of the punches and die are enlarged to approximately 0.1 mm. The punch radius is somewhat smaller (±0.08 mm) and the die radius is a bit larger (±0.14 mm), to make sure that fracture will initiate at the punch and grow to the die radius. We want to determine the punch displacements at fracture experimentally, to have reference points in the numerical simulations for the initiation of ductile fracture. In our blanking set-up, six experiments were performed for every clearance. The shear zone (b) and the burr (c) are measured afterwards at eight positions over the circumference of the blanked products, and averaged to justify the misalignment of the punch. Then, the values are averaged over the six experiments and the standard deviation is calculated (as an example, results are presented for X30Cr13 at the left-hand side of Fig. 3).

It was shown that the roll-over could be accurately predicted for X30Cr13 with a validated model [19,24]. Because it is difficult to measure the roll-over in the axisymmetric set-up, due to the spring-back of the specimen, the roll-over (a) is taken from the numerical simulations. The element size near the transition of roll-over and shear zone is taken as the corresponding standard deviation. Although, the mentioned numerical model is not validated for the other materials, it is assumed correctly to predict the roll-over for those materials as well.

The roll-over height is very low for small clearances and becomes larger for wider clearances because the broader deformation zone allows more bending. The shear zone is getting smaller for larger clearances and this is caused by the hydrostatic stress state; for small clearances, the hydrostatic pressure is larger and this postpones ductile fracture initiation, despite of the fact that the deformation is more localised and that the strains are larger. The burr height is very small (in the order of 5 µm) and is largely determined by the punch radius. The average punch displacements at fracture \( a + b + c \) for X30Cr13 are plotted at the right-hand side of Fig. 3, along with twice the standard deviations (95% interval). The combination of the trend for roll-over height and shear height (plus burr) explains the minimum in the curve. There is a small experimental deviation for the clearance of 10%. This is a result of the larger punch radius for the specific 10% clearance punch. A larger punch radius postpones ductile fracture initiation because the deformation becomes less localised.

3.3. Numerical

In Section 3.3.1, the FEM model of the blanking process, validated for X30Cr13 [19,24], is briefly discussed. In Section 3.3.2, the three-dimensional FEM model of a tensile test is presented [1].

3.3.1. Blanking

We simulated the blanking process using a two-dimensional, axisymmetric FEM model, described by Brokken et al. [25]. Quasi-static analyses were performed on the model geometries that match the experimental set-up for the five different clearances. We modelled the specimen with an isotropic elasto-plastic material, using the material properties that are determined in Section 4.1. The plastic material behaviour is described by the Von Mises yield condition, with isotropic hardening and by the Prandtl–Reuss representation of the flow rule [26]. The mesh, used for the 15% clearance, is shown in Fig. 4. The left boundary at the top (specimen centre) is the axis of symmetry. The other boundaries are either free surfaces or in interaction with a contacting body (punch, die or blankholder).

Linear quadrilateral elements are used that become smaller as they approach either the die radius or the punch radius. Near those radii (±0.1 mm) the element proportions need to be in the range 5 µm [17], resulting in up to 3000 elements in the entire mesh. This element size is not necessary to predict the process force correctly, but it will be vital accurately and smoothly to describe the field variables, needed to predict

Fig. 3. Experimental results for ductile fracture initiation for varying clearance for X30Cr13 [1].
ductile fracture initiation. The punch moves down and penetrates the specimen, resulting in constantly changing boundary conditions. To deal with these difficult boundary conditions and the localised large deformations, the FEM application that we used, combines three numerical procedures: the commercial implicit package MARC [26] (using an updated Lagrange formulation), an Arbitrary Lagrange–Euler approach [27,28] and an automatic adaptive remeshing algorithm [17,25], to overcome severe mesh distortion problems. This model was experimentally validated prior to fracture on both deformation fields — using digital image correlation — and process forces, using a planar blanking set-up [19,20]. Therefore, the deformation history in the blanking process can be calculated adequately, which is a prerequisite for the local modelling of ductile fracture.

3.3.2. 3D FEM model of a tensile test

For the determination of $C$ in a tensile test (the second approach), specimens were used with sizes that are presented at the right-hand side of Fig. 5. In previous work a 3D FEM model was presented [1]. The undeformed specimen is shown at the left, of which $\frac{1}{8}$ is modelled (shown in the centre of Fig. 5). The three-dimensional calculation is needed to correctly simulate the necking process, for which no imperfection was needed. At the location where the neck is expected to occur the mesh is refined. Exactly, the same material model will be used as in the blanking model of Section 3.3.1.

4. Results for different materials

Two different approaches were elaborated for the blanking process to characterise ductile fracture initiation for X30Cr13 [1]. To verify these methodologies, exactly the same procedures are applied to the four other materials of Table 1.

In Section 4.1, the material parameters concerning elasto-plastic deformation are determined assuming the same constitutive model to be valid for all materials. Results for tensile testing, in which the parameter $C$ is determined for the second approach, are presented in Section 4.2. For the blanking process, the resulting maximum blanking forces and punch displacements at fracture initiation are outlined in Section 4.3. For two materials some special applications of both criteria are outlined in Section 4.4.

4.1. Material characterisation

All materials are assumed to behave according to the elasto-plastic model with Von Mises plasticity and isotropic hardening. The elastic properties (Young’s modulus $E$ and Poisson’s ratio $\nu$) are taken from the material supplier. For the determination of the plastic material parameters, the procedure of Section 3.2.1 is applied, where a number of pre-rolled specimens are tested in rolling direction to give yield stress information for large strains. The results are plotted in Fig. 2 for X30Cr13 and in Fig. 6 for the other materials. The triangles represent the maxima of the separate tensile tests (the maximum stress occurring in homogeneous deformation, obtained just before necking). The circles are extra points, taken from the tensile test of the as-received material, which are needed to supply information for the yield stress in the region of small strains. Through all markers a mastercurve is fitted using the formulation of Eq. (5). The parameters resulting from the fitting procedures are all reported in Table 2 together with the elastic constants.

As can be seen in Table 2 some parameters are fitted to be zero. This is due to the fact that in the fitting procedure, employing Eq. (5), the parameters $M_3$ and $M_4$ are not
allowed to become negative. If one of these parameters is fitted to be negative, it is made zero and the fitting procedure is repeated, keeping the specific parameter zero. $M_3$ and $M_4$ are not allowed to become negative because an extrapolation of the mastercurve with negative parameters $M_3$ or $M_4$ may introduce intrinsic softening for large strains which is not realistic for metals.

As shown in Figs. 2 and 6 the fitted mastercurves describe the yield stress data well, for all five materials.

### 4.2. Tensile tests

For all five materials, a 3D simulation was performed with the FEM model of Section 3.3.2 using the material parameters of Table 2. The deformed mesh and the fractured specimen are shown in Fig. 7 for X30Cr13 [1]. This figure shows that the FEM model predicts the deformation of the tensile specimen well. Also the wedge-like shape of the specimen at fracture is predicted correctly.

Besides this verification on deformation behaviour for X30Cr13, the FEM simulation is also checked on the force–displacement curve. The experimental and numerical force–displacement curves for all five materials are depicted in Fig. 8. It can be seen that the shape of the predicted force–displacement curves deviates only for Al51ST and DC04. For these two materials only five and six circles, respectively, could be employed in the fitting procedure (Fig. 6), because otherwise deviations become too large for large strains. Because less experimental data for small strains is used during fitting, the stress–strain relationship is not fitted

Fig. 6. Fitting procedures for four different materials. The triangles represent the maxima of the separate tensile curves and the circles denote some points of the as-received tensile curve. The curves are fitted on Eq. (5).

<table>
<thead>
<tr>
<th>Material</th>
<th>$E$ (MPa)</th>
<th>$v$ (–)</th>
<th>$\sigma_{y0}$ (MPa)</th>
<th>$M_1$ (MPa)</th>
<th>$M_2$ (MPa)</th>
<th>$M_3$ (MPa)</th>
<th>$M_4$ (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>X30Cr13</td>
<td>$187 \times 10^3$</td>
<td>0.28</td>
<td>420</td>
<td>133</td>
<td>0.0567</td>
<td>406</td>
<td>70.7</td>
</tr>
<tr>
<td>316Ti</td>
<td>$200 \times 10^3$</td>
<td>0.28</td>
<td>298</td>
<td>1003</td>
<td>0.414</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Ms64</td>
<td>$100 \times 10^3$</td>
<td>0.35</td>
<td>251</td>
<td>345</td>
<td>0.320</td>
<td>56.0</td>
<td>0</td>
</tr>
<tr>
<td>DC04</td>
<td>$210 \times 10^3$</td>
<td>0.29</td>
<td>185</td>
<td>309</td>
<td>0.378</td>
<td>113</td>
<td>0</td>
</tr>
<tr>
<td>Al51ST</td>
<td>$70 \times 10^3$</td>
<td>0.33</td>
<td>306</td>
<td>61.0</td>
<td>0.0476</td>
<td>73.0</td>
<td>0</td>
</tr>
</tbody>
</table>

Table 2
Material parameters of five different materials for elasto-plastic model with Von Mises plasticity, with hardening according to Eq. (5)
accurately in this strain region for these two materials. The shape of the curves for the other materials is well predicted. The only deviation seems to be the clamp displacement, which might be under-predicted numerically.

4.3. Blanking

Axisymmetric blanking experiments were performed for five different materials with five different clearances using the experimental set-up described in Section 3.2.2. The maximum blanking forces are measured, as well as some measures of the blanked edge, from which the punch displacement at fracture initiation is determined. Using the material parameters, characterised in Section 4.1, axisymmetric FEM simulations are performed for all clearances for the five different materials.

The numerical and experimental maximum blanking forces are presented in Fig. 9 at the left axes. For X30Cr13, Al51ST and Ms64, deviations are observed in the order of 5% or smaller. For 316Ti and DC04, the predictions deviate approximately 10% from the experiments. The stresses in the shear zone where strains occur larger than 3, largely determine this maximum blanking force. An error in the material description for large strains will consequently result in a corresponding error in the maximum blanking force. Therefore, the results and deviations for the maximum blanking force provide information...
about the performance of the material model, including the assumption of Von Mises plasticity with isotropic hardening and characterisation for large strains.

Two approaches were employed to predict the punch displacement at fracture initiation for all materials. The first approach utilises the adapted criterion of Rice and Tracey [21] of Eq. (2). $C_{RT}$ is determined in the blanking experiment with the 6% clearance and with this $C_{RT}$ the punch displacement at ductile fracture initiation is predicted for all other clearances. Results for the quantified $C_{RT}$'s are given in Table 3 and the predictions are presented in Fig. 9. For the approach of Section 2.2, the thickness at fracture initiation in the tensile tests are measured and the critical $C_G$'s (for the criterion proposed by Goijaerts et al. [1]) are determined in the 3D FEM simulations of the tensile tests for all five materials (Table 3). Predictions for the punch displacements

Table 3
For five different metals, the experimentally determined critical C's are presented for both applied criteria$^a$

<table>
<thead>
<tr>
<th>Material</th>
<th>$C_{RT}$ (blanking)</th>
<th>$C_G$ (tensile testing)</th>
<th>Thickness at fracture (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>X30Cr13</td>
<td>2.70</td>
<td>3.53</td>
<td>0.448</td>
</tr>
<tr>
<td>316Ti</td>
<td>7.42</td>
<td>6.53</td>
<td>0.329</td>
</tr>
<tr>
<td>Ms64</td>
<td>7.27</td>
<td>6.39</td>
<td>0.342</td>
</tr>
<tr>
<td>DC04</td>
<td>8.70</td>
<td>14.8</td>
<td>0.132</td>
</tr>
<tr>
<td>A1S1ST</td>
<td>1.49</td>
<td>1.45</td>
<td>0.642</td>
</tr>
</tbody>
</table>

$^a$ For the Rice and Tracey [21] criterion, $C_{RT}$ is quantified in the blanking experiment with the 6% clearance. For the Goijaerts et al. [1] criterion, $C_G$ is resolved from the presented experimental thickness at fracture initiation using the 3D FEM simulations of the tensile tests.
at fracture initiation for all clearances are presented in Fig. 9 for all materials.

For the first approach, the predictions fall within the experimental errors or in a very close range, for all materials. The maximum deviation from an experiment is 15% for Ms64 for the blanking geometry with the 15% clearance. All other predictions deviate in the range of 10% or smaller. For the second approach, with characterisation in the tensile tests, predictions for X30Cr13 and Al51ST maximally deviate 6 and 10%, respectively, from the experiments. For 316Ti and DC04, the maximum deviations are 12 and 15% and the maximum deviation for Ms64 is 24%. The first approach performs better for all materials.

4.4. Special applications

So far in this paper, only relatively large cutting radii of the tools are considered in order to reduce the calculation times in the large number of simulations, of which the results were presented in the previous section. Now, for X30Cr13, results are displayed for both small (read industrial) and large cutting radii of the punch for two extreme clearances in Fig. 10. The first methodology, applying the adapted Rice and Tracey criterion, and the second methodology, applying the Goijaerts et al. criterion, show good results for different geometries: a change in clearance which does not exhibit a drastic change in punch displacement at fracture (Fig. 9), is captured by both methodologies; moreover, a change in cutting radii of the tools, which shows a drastic change in punch displacement at fracture (Fig. 10), is also accurately described by both approaches ($C_{RT}$ and $C_{G}$ are taken from Table 3). These results strongly suggest that both methodologies with the corresponding criterion can be applied successfully in the blanking process over a wide variety of geometries.

Up to this point, for the first approach — characterisation in blanking — only the adapted Rice and Tracey criterion of Eq. (2) is utilised, and for the second strategy — characterisation in tensile testing — only the criterion of Eq. (3) is used. To check their applicational area, both criteria are applied in both methodologies for Al51ST. Results are presented in Fig. 11. As can be seen, the criterion of Eq. (3) performs well for both methodologies. From Table 3, one can find that for Al51ST, $C_{G}$ is determined to be 1.45 in the tensile test. In blanking it is quantified quite close to $C_{G} = 1.29$, which shows that the criterion is valid for both tensile testing and blanking. Unfortunately, the adapted Rice and Tracey criterion performs badly when characterised in tensile testing ($C_{RT} = 3.18$, determined in tensile testing compared to $C_{RT} = 1.49$ quantified in blank-
5. Discussion and conclusion

Complete strategies to predict ductile fracture initiation in the blanking process, which resulted in a full blanking model, were presented for one metal in an earlier publication [1]. These methodologies, including the assumptions for the material behaviour, are applied to four other metals. For all five materials, blanking results are presented in this paper concerning maximum process force and punch displacement at ductile fracture initiation. The maximum process force, occurring in blanking, is important for engineering aspects when wear and duration of life of the tools is considered. The punch displacement at fracture initiation is important because it largely determines the shape of the blanked edge.

Material characterisation is a significant issue in modelling of the blanking process. Because very large strains occur in the localised shear zone it is important to determine accurately the stress–strain relation for large strains, which is not straightforward. Tensile tests are performed on pre-rolled specimens to evolve this relation up to large strains for all metals. The calculated force–clamp displacement curves for the tensile tests and the predicted maximum blanking forces suggest that the plastic material behaviour is characterised successfully over the entire strain range.

Predictions for maximum blanking forces are good for all metals with maximum deviations of approximately 10%. For the prediction of punch displacement at ductile fracture initiation, two methodologies are evaluated. The strategy where the fracture criterion is characterised in a blanking experiment performs well for all materials. The second methodology, which utilises a tensile test to characterise ductile fracture with a newly proposed criterion [1], yields satisfactory results. The advantage of the first approach is the higher accuracy. The second strategy has the advantage of a much simpler experiment that is needed for the characterisation of ductile fracture. Moreover, the ductile fracture initiation criterion of the latter is not only valid for blanking but also for tensile testing under different pressures. Although, the criteria may look promising for other forming processes, it should be noted that the results presented here do not guarantee the applicability under the corresponding loading conditions.

For both criteria it is concluded that the only material parameter is the critical C.

Acknowledgements

This research was funded by the Dutch innovative research projects (IOP-C.94.702.TU.WB). Jan Post of Philips DAP/LTM kindly supplied the X30Cr13 material and the experimental data for the corresponding material model.

References