Modelling of instability and fracture effects in the sheet metal forming based on an extended X-FLC concept

Pavel Hora¹, a *, Bekim Berisha¹, Maysam Gorji¹, Holger Hippke¹
¹ ETH Zürich, Institute of Virtual Manufacturing, Tannenstrasse 3, 8092 Zürich, Switzerland
aphora@ivp.mavt.ethz.ch

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Abstract. The industrial necking prediction in sheet metal forming is still based on the Forming Limit Diagram (FLD) as initially proposed by Keeler. The FLD is commonly specified by the Nakajima tests and evaluated with the so called cross section method. Although widely used, the FLC concept has numerous serious limitations. In the paper the influences of bending on the FLC as well as postponed crack limits will be discussed. Both criteria will be combined to an extended FLC concept (X-FLC).

The new concept demonstrates that the Nakajima tests are not only appropriate for the evaluation of the necking instability, but also for the detection of the real crack strains. For the evaluation of the crack strains, a new local thinning method is proposed and tested for special 6xxx Al-alloys.

1 Introduction

In the sheet metal forming – due to the high cost for the applied tooling – numerical models for a robust process lay out are widely used. Due to high quality aspects neither shape deviation induced by sink marks or dents, nor necking or even rupture are tolerated.

Fig. 1 Different failure types in sheet metal formed parts. Top: Failures induces by initials necking and cracks. Bottom: Surface failure by Sink marks and dents

The evaluation of the failures described above is based on stress or strain distributions evaluated with non-linear finite element methods. It is well known, that such methods need exact constitutive models describing their mostly anisotropic yield behavior. As input
appropriate hardening (yield) curves e.g. (Gosh [18], Hockett-Sherby [19], Hensel-Spittel [20]) in combination with the correct yield loci e.g. (Hill’48 [23], Barlat et al. [21], Vegter et al. [24], Cazacu et al. [22]) have to be specified. The outputs of the corresponding simulation are stresses and strain distributions which usually correspond well to those of the real processes.

**Direct prediction of wrinkling:** Failure types like wrinkling, as mentioned in Fig. 1 bottom, are mostly mapped directly as a deformation state. The high accuracy of the wrinkling prediction is demonstrated in Fig. 2 on an example of food caps.

![Direct prediction of wrinkling](image1.png)

**Fig. 2** FE based simulation of wrinkling on food cups. Simulation with *AutoForm*.

**Direct modeling of necking induced ruptures:** Much more complex is the direct prediction of the necking and crack failures. Although in some specific cases a direct failure prediction is possible, an indirect modeling technique is mostly applied on real industrial parts. As examples for the direct crack prediction in Fig. 3, the simulation of a localized band for an AA 60616 alloy in a tensile test and, in Fig. 4, the simulation of shear bands arising on the edge of a hole expansion test are shown.

![Direct modeling of necking induced ruptures](image2.png)

**Fig. 3** Modelling of crack initiation in a tensile test. Left: FEM simulation results based on a Gauss distributed thickness assumption; right: LN on real specimen. Material AA6016), Hora et al. [16].
Despite the obviously correct predictions in both cases, if no regularization methods are applied, both solutions suffer from mesh size sensitivities.

**Indirect prediction methods:** In the industrial approach mostly an indirect prediction based on failure curves or loci is applied. For the prediction of the necking initiation the well known Forming Limit Curves are used. Their experimental evaluation has some weaknesses, which will be discussed. Much more complex and still topic of current research is the prediction of crack limits. In this field neither the experimental methods nor the use of the experimentally detected values is clear. New methods in the evaluation of crack limits for sheet materials is the main topic of this contribution.

## 2 Prediction of necking initiation

The “classical” Forming Limit Diagram as originally proposed by Keeler and evaluated based on Nakajima tests is shown in Fig. 5. Misleading is the already mentioned interpretation, that the material directly cracks for strain values above the FLC – without taking the loading state into account.

![Fig. 5 Prediction of formability limits based on a Nakajima based evaluation method.](image)

Even if this method seems to be very practicable it has the following weaknesses:

a) The position of the FLD curve is physically not clearly defined. This can be well demonstrated on the necking evolution of the B20 specimen.
As standard evaluation method the “cross section method” as defined in ISO/DIS 12004-2 / ISO TC 164/SC 2 will be widely used. An alternative option is the time dependent method developed by Volk and Hora [8].

b) Influence of the curvature: The Nakajima experiments are done with a standardized punch diameter of 100 mm. It is well known and it was once more demonstrated by a recent publication by Neuhauser et al. [9], that the punch curvature has a significant influence on the onset of necking.

Fig. 6 Continuous evolution of the necking on the B20 specimen

Fig. 7 FLC evaluation based on the time dependent method.

Fig. 8 Increase of critical FLD0 (β=0) values in dependency of the t/R factor in a stretch bending test, Neuhauser et al. [9].
The impact of curvature was primarily demonstrated by Hasek and numerically investigated by Hora et al. [6] based on a numerical MMFC description. As alternative an extended FLC-R description was proposed.

![Fig. 9 Influence of curvature on the FLC, Hora et al. [6].](image)

c) Influence of different boundary constrains: The experimentally evaluated FLC shows a drop down for the most left point.

![Fig. 10 Experimentally evaluated FLC of HC220-YD and AA5182, Numisheet BM 2008.](image)

In the typical deep drawing applications, the strain points on the left side reach much higher values than predicted by the Nakajima B20 experiment. The reason is the difference in BC between the Nakajima test compared to the corresponding deep drawing behavior. In the case of the Nakajima test the stress BC with $\sigma_{22} = 0$ is given. In the deep drawing the deformation path follows the strain path $\beta=-0.5$. In this way the displacement or strain conditions are given. The different BC induced significantly different necking behavior as can be demonstrated by FE tests.
Fig. 11 Influence of the strain BC in the DD case to the stress BC in Nakajima case on the necking initiation

For those reasons the FLC on the left side has to be extrapolated. Experience shows that a linear extrapolation using the B20 and B50 points delivers too conservative values. Fig. 12 demonstrates this behavior in the case of a AA6016 alloy.

| Not failed | Predicted as failed |

Fig. 12 Underestimated prediction of the DD behavior with the linear extrapolation based on B20 measurements. Material AA6016 (AC200).

The widely spread interpretation that the FLC as plotted in Fig. 5 really describes the forming limits is in the physical sense not correct. Processes like hemming or the incremental forming allows strains clearly above the FLC, see Fig. 13. (see also [10])

Fig. 13 Bending test. Strains above the classical Nakajima-Necking FLC, [4].

In this sense the region above the FLC is only conditionally unstable, Fig. 14.
3 Prediction of cracks

As demonstrated by the time dependent strain measurements material does not crack at the FLC-level but on much higher strain values. The “conditional stable deformation field” in Fig. 14 can be used for forming operation if necking instabilities are avoided (Localized Level FLC, Hora et al. [16]). Such deformation state exists for example in the case of hemming or in the case of incremental forming, Hora et al. [7].

3.1. Appearance behaviour of sheet cracks and former investigations

For ductile materials, as are the most sheet forming materials, a ductile type of cracks can be assumed. As demonstrated in Fig. 4, if the crack position is induced by the pre-going necking, the crack position can be identified directly by the localized necking area, which will be predicted in the FE models, if carefully done, correctly.

In a more detailed consideration the sheet cracks develop mostly as an “out of plane crack”, see Fig. 15., see also [12][11].

For the compression-tension stress states an “in-plane crack” also called shear cracks will be assumed. This type of cracks, even if widely investigated by many researchers (Bai, Wierzbicki and Bao [2][17], Lou [15], Mohr et al. [14]), practically does not occur in the classical sheet drawing operations. The experimental result, detected on specific shear specimen (see for
example Mohr et al. [14]) seems to be even misleading. This topic will be discussed more in detail in the oral presentation.

3.2. Direct modeling of “out-of-plane” cracks

The example in Fig. 16 demonstrates again, that the FE models are able to predict in an appropriate way the location of the crack-plane over the thickness, as shown in Fig. 15 left. In the shown example a critical strain criterion with a Gauss distribution of yield stress over the structure and an isotropic material model was applied. More details will be given again in the oral presentation.

Fig. 16 FE-modelling of an “out-of-plane” crack development for a $\sigma_2 / \sigma_1 = 0.5$ (plain strain) load case. Material AA6016.

3.3. Theoretical failure criteria

As theoretical basis for the building of cracks either a critical tensile stress

$$\text{max}[\sigma_n] = C_n(\eta, \xi)$$

or the Mohr-Coulomb critical shear stress criterion

$$\text{max}[\tau + C_s \sigma_s] = C_s(\eta, \xi)$$

are common. In above equation $\eta$ stays for the normalized hydrostatic stress $\eta = \sigma_{II} / \sigma_{eq}$ and $\xi$ for the Lode angle parameter

$$p = -\sigma_{II} = -\frac{\sigma_1 + \sigma_2 + \sigma_3}{3}, \quad q = \sigma, \quad r = \left(\frac{27}{2} s_1 s_2 s_3\right)^{1/3}, \quad \xi = \cos(3\theta) = \left(\frac{r}{q}\right)^{3}$$

With simulations on simple cup drawing tests the critical state of the shear stresses, as needed in eq. (2) can be detected.
Beside the above introduced Mohr-Coulomb criterions there are different other approaches. One of the most used ones are the GTN models based on porous plasticity models. For more details see for example Mohr et al. [14], who give a good summery of the different models.

3.4. Experimental detection of crack limits for sheets

As already mentioned above in eq. (1) and (2) the critical fracture strain strongly depends on stresses. From an experimental point of view, the definition of test with variable stress conditions is not very easy to be performed. In investigations initiated by Wierzbicki, Bai, Bao, Luo ([2], [17],[15]) and others different shapes of specimen have been proposed. Especially the detection of the so called shear crack strains seems to be very problematic due to the inhomogeneous strain distributions on such specimens and the different constrains compared to
the real deep drawing case. In different publications εf strains have been published which are too low and which have been declared as incorrect later on. How important the detection of the shear strain limits is, is still not clear due to the fact, that shear cracks practically never occur. Bending cracks on parts with small die radii are not “shear cracks” even if often declared as such.

The difficulty in application of the different specimens, for example as proposed by Bai and Wierzbicki [2] and also investigated by Mohr et al. [14], is the change of the sheet surface, strongly inhomogeneous deformation states and last but not least high costs for the fabrication of such specimens.

For those reasons alternative experimental methods have been investigated by the authors. At least for thick sheets where bar specimens can be machined a so called Tensile Torsion Test proposed by Hora can be an alternative. All measurements with this test shows for pure shear the highest strain values – these in contradiction to the values based on the so called butterfly specimens.

In contrast to the above discussed methods the goal was to use the established “standard” sheet tests. For this purpose, the authors proposed the combination of two methods – the Nakajima tests combined with an additional cup drawing test for detecting the behavior for β < −0.5

### 3.4.1. Experimental detection of crack limits based on Nakajima tests

The first method evaluates the crack strain based on the thinning strains (“Thinning Method”) measured on the broken Nakajima specimens, see Fig. 19. The detailed evaluation procedure is given in [4] and [5].

![Fig. 18 Tensile-Torsion-Test. Detection of fracture strain under different stress conditions](image)

![Fig. 19 Evaluation of the fracture strain by the local detection of fracture thinning on the Nakajima specimens. Gorji [3],[4]](image)
Fig. 20 shows the so detected crack strains $\varepsilon_{maj}^{\text{crack}} (\beta)$ for the material AA6016 in relation to the classical FLC limits evaluated by the cross section as well as the time dependent method.

![Image](image.png)

**Fig. 20** Evaluation of the fracture strain by the local detection of fracture thinning on the Nakajima specimens and comparison with the FLC. Gorji [3],[4]

### 3.4.2. Experimental detection of crack limits based on Deep Drawing tests

The Nakajima based test are restricted to the stress range

$$0 \leq \alpha = \frac{\sigma}{\sigma_i} \leq 1.0$$

In many deep drawing applications, the largest strains occur on the left side of the FLC. For those reasons a special DD test with a quadratic blank and a relatively small die curvature of $r=3.0$ mm was applied to get an additional “point” specifically in the deep drawing (compression-tension combination) range. The evaluation of the strains is based on the comparison of the real part with the strains received by the FEM simulation. Fig. 21: As critical fracture strain the strain on the surface of the sheet (top layer of the shell) was decisive.

![Image](image.png)

**Fig. 21** Calibration of the fracture line based on a deep drawing experiment and the corresponding strain distribution at the crack time step. Gorji [4]

The combination of the Nakajima fracture strains with the additional DD fracture strains can be used as data basis for the determination of a generalized fracture line.
3.5. Extrapolation and interpolation of the fracture points based on different failure criteria

The so experimentally predefined strain has been compared with the theoretical limits of different failure criteria. The check was especially done applying the following 4 different criteria:

- Maximum shear stress criterion
- Equivalent strain criterion
- Johnson-Cook criterion and
- Linear fracture line criterion

Fig. 22 shows the fits of the theoretically fitted failure curves with the experimental points. Remarkable is the fact, that all criteria fit the position of the measured points quite well, but that the criteria deviate significantly in the left “deep drawing” range. Especially this range influences strongly the virtual results for deep drawing operations.

Fig. 22 Comparison of different fracture criteria in principal strain space and in triaxiality-equivalent strain space, [4].

As will be demonstrated in Chapter 4 the Linear Fracture Line describes the behaviour in the most accurate way and delivers for real applications the best fits with the real behaviour. The JC-criterion will deliver only slightly different results.

4. FE-Implementation of the X-FLC concept for monolayer and multilayer materials

4.1. Layer based failure prediction with shell elements

The classical failure predictions is based on a mono-layer FLC prediction. If the fracture is initiated by a surface crack, as it is the case by small radii, an extended X-FLC method has to be applied, see Fig. 23. The crack develops and initiates only when the critical layer reached the crack limit. Points above the FLC will then be interpreted as conditional stable and not as usually assumed as failed by cracks.
The FEM-implementation was done in that way, that the crack failure was checked specifically for each layer. If a critical strain was detected, the specific shell layer was deactivated by setting the stresses to zero, see Hora et al. [7].

In the LS-Dyna code the implementation was done by the subroutine UMAT41. The *PART COMPOSITE functionality of FE-code LS-Dyna has been employed instead of the regular shell element. Based on this element formulation the mechanical properties and thickness distribution of each layer can be described separately.

For the validation of the above implementation a new “triangle” test was designed. The tests have been done with the monolayer material AA6016 as well as with the FUSION material. Fig. 24 demonstrates the significant change of the deep drawing behaviour for the both materials.

![Monolayer material AA6016](image1)

![Multilayer material FUSION (Fig. 26)](image2)

The crack of the AA6016 material, in the die region, is induced by the small die radius of 3.0 mm. Due to the better bending behavior of the FUSION material, the multilayer material does not fail. This special crack behavior can only be understood on the basis of an additional fracture consideration as introduced by the X-FLC concept.

4.2. Prediction of crack limits for parts with small die radii and a monolayer structure

The experimental behavior of the monolayer material was demonstrated in Fig. 24 left. The rupture occurs at a depth of $H \approx 43$ mm.

Fig. 25 demonstrates the correct prediction of the depth as of the crack position for the triangle test.
4.3. Prediction of crack limits for multilayer FUSION materials

The layer specific failure prediction is indispensable for multi-layer materials with strongly different behavior of the layers. The investigated FUSION™ [Novelis], Fig. 26, is composed of a soft AA5005 alloy outside (clad) and a hard AA6016 alloy inside (core).

Beside Fig. 24 right for the triangle part also Fig. 27 for a simple cup drawing part demonstrates again clearly how significantly the DD behavior for the multi-layer material changes.

Fig. 26  **Left:** Multilayer FUSION material composed of an AA6016 (core) and a AA5005 (clad) material – [source Novelis];  **Right:** Yield curves of the single core, clad and of the combined FUSION material.

Fig. 27  **Comparison of the forming behaviour:** (left) monolayer material AA6016; (right) FUSION material, Gorji [4]
The reason for the significant change of the forming behavior is not a shift of the FLC – which is more or less the same for the monolayer and FUSION-material – but the significant shift of the fracture strain for the clad material, as proven with the “thinning method” and shown in Fig. 28.

Fig. 28 Evaluation of the fracture strains for the core (AA6016) and the clad (AA5005) materials.

**FE-model for multilayer composite materials**

For the modelling of the multi-layer FUSION material, Fig. 26, eleven integration points (IPs) through the thickness of the composite shell elements have been employed. One IP for each clad outer layer with the thickness of 0.06 mm and nine IPs for the core with thickness of 0.0978 mm for each layer with total core thickness of 0.88 mm. The integration points of the core and clad have their own material properties i.e., hardening curve, standard or modified Yld2000-2d yield function and fracture limit as specified in Fig. 23 and Fig. 28.

Fig. 29 Forming behaviour of FUSION material: FE simulation of triangular experiment with linear fracture criterion (dashed line) specifically defined for the layers (strain distribution of lower, middle and upper-layer); solid line: FLC based on MMFC-criterion, Gorji [3].
Fig. 29 demonstrates, that based on the X-FLC concept, the “not failed” behavior of the FUSION material was again correctly predicted.

### 4.4. Prediction of crack limits in a hole expansion test

From the industrial point of view edge cracks often occur on sheet parts. Some detailed investigation has been recently published by Larour et al. [13]. In this investigation among others the quality of the edge can be identified as a significant parameter.

In the example presented below, an aluminium material AA6016 in the sheet thickness of $d = 1 \text{ mm}$ was applied. The material data are specified in Hora et al. [7].

#### MMFC evaluated FLC

<table>
<thead>
<tr>
<th>$\varepsilon_2$</th>
<th>$\varepsilon_3$</th>
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<tbody>
<tr>
<td>-0.215</td>
<td>0.43</td>
</tr>
<tr>
<td>-0.086</td>
<td>0.288</td>
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<tr>
<td>-0.036</td>
<td>0.238</td>
</tr>
<tr>
<td>-0.023</td>
<td>0.228</td>
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<tr>
<td>0</td>
<td>0.22</td>
</tr>
<tr>
<td>0.011</td>
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<tr>
<td>0.022</td>
<td>0.22</td>
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<tr>
<td>0.045</td>
<td>0.225</td>
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<tr>
<td>0.11</td>
<td>0.245</td>
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<tr>
<td>0.186</td>
<td>0.265</td>
</tr>
<tr>
<td>0.28</td>
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</tbody>
</table>

Fracture line

$$\varepsilon^f = 0.45 - 0.42 \cdot \varepsilon_2$$

Fig. 30 X-FLC data for the material AA6016

The experimental results of the Marciniak hole expansion test are plotted in Fig. 31. To avoid “undefined” edge damage the inner hole was wire cutted.

Fig. 31 HET with the material AA 6016. Critical depth at the edge fracture occurrence ca. 10.0 mm
Using the X-FLC model with the fracture line evaluated with the thinning concept, Fig. 30, the simulation delivers obviously a very correct prediction for the critical state, Fig. 32.

![Fracture line - Material AG200](image)

**Fig. 32** HET with the material AA 6016. Critical depth at the edge fracture occurrence ca. 11.0 mm

## 5 Conclusions

The contribution demonstrates on selected examples that the classical FLC prediction is not applicable if the parts have either small die radii or are composed by layers with different properties.

In this case - beside the FLC - a crack limit curve has to be specified too. For the detection of such critical strain the thinning method, evaluating the fracture strains on Nakajima specimens, has been used. The multi-layer failure identification was implemented into the explicit FEM code LS-Dyna. Further, introduction of inhomogeneities like e.g. a Gaussian distribution of the yield stress is an appropriate way to model initiation and propagation of the crack over the sheet thickness.

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