Communications

Strain Localization in Sheet Metal Containing a Geometric Defect

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Tensile failure of sheet metal usually occurs as a result of a strain localization process referred to as localized necking. An important concept for understanding and predicting localized necking is that strain localization can develop as a result of the presence of a linear imperfection, as initially proposed by Marciniak and Kuczynski (M–K).[1] Although in the M–K article, the imperfection was conceived as a geometric defect in the form of a long groove lying normal to the maximum principal strain direction, it can also result from a material inhomogeneity, which takes the form of a long band of weak material. Marciniak and Kuczynski analyzed the evolution of strain localization within the imperfection as a function the far-field deformation outside the imperfection, and that analysis subsequently became a basis for understanding localized necking of sheet under a wide range of strain paths.[2–6]

Despite the extensive application of the M-K model in predicting the forming limit behavior of sheet metal, experimental tests of the model predictions have been few. One attractive feature of the M–K model is its ability to establish a local plane strain condition necessary for localized necking under biaxial stretching conditions. As a result, most of the experimental investigations have addressed the behavior of biaxially stretched sheets containing imperfections in the form of patches with varying aspect ratios,[7] a single planar groove,[8] and a series of axisymmetric indentations with varying spacings and depths.[6,8,9] The results of these experiments show limited agreement with M–K predictions, possibly because of the difficulty in establishing a plane strain condition within the groove while maintaining biaxial proportional straining outside the groove.

In this communication, we present experimental results obtained in the course of testing ZIRCALOY* tubing under conditions relevant to a reactivity-initiated accident in a nuclear power reactor.[10] Under such conditions, a sudden increase in the fission rate results in a rapid expansion of the uranium dioxide fuel, which impinges upon the ZIRCALOY cladding, deforming it in a "transverse" plane-strain condition. In that case, it is relevant to study the possible influence of a band of weak material (resulting, for example, from corrosion or hydriding) on the ductility of the thin-wall tubing.[11,12] The associated testing involves experiments that we believe support the M–K predictions under "ideal" conditions, as described subsequently.

*ZIRCALOY is a trademark of Westinghouse Electric Company, Pittsburgh, PA.

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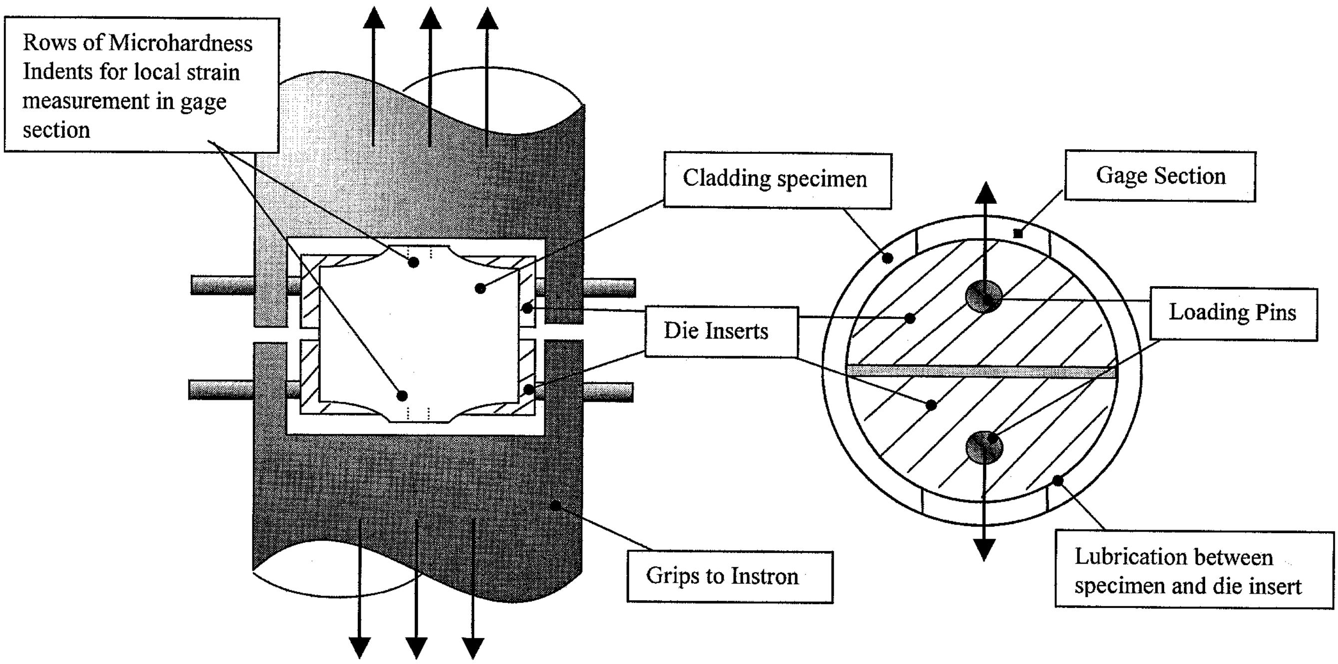
In particular, we describe the results from a series of tensile tests of thin ZIRCALOY-4 cladding specimens, containing a single linear groove, of uniform depth and width, oriented perpendicular to the major principal strain direction. These tensile tests were conducted such that the specimens were deformed under near plane-strain conditions. In this configuration, plane-strain deformation can be maintained both within and outside the groove while strain localization develops. These tests allow us to experimentally determine the influence of groove depth (*i.e.*, imperfection severity) on localized necking and to compare those results with the predictions of M–K theory.

The material we tested was cold-worked and stress-relieved ZIRCALOY-4 cladding tubes with an outer diameter of 9.5 mm and a wall thickness of 0.56 mm. The grain structure consists of elongated grains with an approximately 10:1 aspect ratio parallel to the tube axis; the elongated grains were approximately 10- to 15- μm long and 1- to 2-μm thick; thus, a large number of grains are present across the thickness of the specimens. In the cold worked and stress relieved condition, the ZIRCALOY-4 cladding used in this work exhibits a crystallographic texture such that the plastic anisotropy parameter, R, as determined in the hoop direction of the cladding is $R = 2.3$. The strain hardening exponent, $n = d \ln \sigma / d \ln \varepsilon$, was measured using small compression samples, approximately 1-mm long with widths and thicknesses ranging from 0.5 to 0.7 mm, and oriented in the hoop direction of the cladding tube. These data indicate a strain hardening exponent $n = 0.068$ at a strain rate of 10^{-3}/s. At room temperature, there is a small degree of strain-rate hardening such that $m = d \ln \sigma / d \ln \dot{\varepsilon} = 0.02$.

As shown in Figure 1, we used a double-edge notched "transverse plane-strain" specimen geometry to obtain a near transverse plane-strain deformation condition under loading transverse to the cladding tube axis.[12] Strain measurements show that the notches induce the central section of the gage section to deform such that the minor strain is less than 10 pct of the major strain, *i.e.*, a near plane-strain deformation condition, as described previously.[12] The specimen was loaded *via* two D-shaped die inserts, and TEFLON* and vacuum grease lubrication was applied to reduce the friction coefficient between the die inserts and the specimen. Since the gage section was oriented on top of the die inserts, a self-similar geometry was maintained throughout the deformation. In order to determine the local strain distributions, an array of two rows (spaced approximately 1 mm apart) of 14 microhardness indentations (spaced approximately 0.2 mm apart) was applied along the entire gage length of each specimen before testing. Using a diamond saw, we prepared several samples each containing a single shallow, flat-bottomed groove, approximately 0.5-mm wide and in which the depth was systematically varied between 10- and 77-μm deep. The grooves were oriented, as shown in Figure 1, perpendicular to the major principal strain direction. The tests were performed at room temperature.

*TEFLON is a trademark of E.I. Du Pont de Nemours & Co., Inc., Wilmington, DE.

An example of the strain localization within the grooved



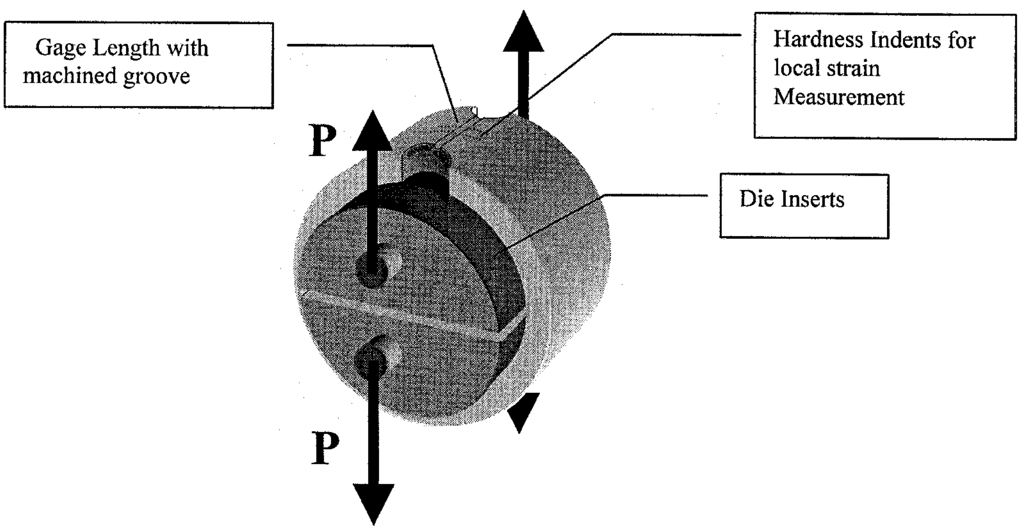


Fig. 1–The geometry of the transverse plane-strain ring specimen. Note the two rows of hardness indentations along the central portion of the gage sec tion and the shallow groove, which runs along the full length of the gage section.

gage section of a specimen deformed to failure is depicted in Figure 2, for a groove depth of 20 μm. The two sets of hardness indentations agree quite well and show that a limit strain of roughly 0.06 to 0.07 has accumulated outside the groove at failure. Failure of these grooved specimens occurs due to localized necking, which, as expected, initiates in the center of the specimen where deformation is near plane strain and which results in a failure path oriented approximately 45 deg through the cladding thickness. For all groove depths, fracture strains in the range of 0.10 to 0.14 were measured within the groove.

In Figure 3, we plot experimental data depicting the dependence of the limit strain on the severity, f, of the groove, as defined in Eq. [1]. The limit strain (as determined outside the groove) decreases rapidly with increasing severity of the groove. We see that a 30-μm deep groove (which results in a 5 pct reduction of cladding thickness) causes the limit strain outside of the groove to be reduced by half (from 0.08 to 0.04). Taken as a whole, the data in Figures 2 and 3 indicate that in our case surface flaws having a severity of $f \geq 0.03$ can be very effective in localizing deformation and reducing the limit strain. These results are consistent with

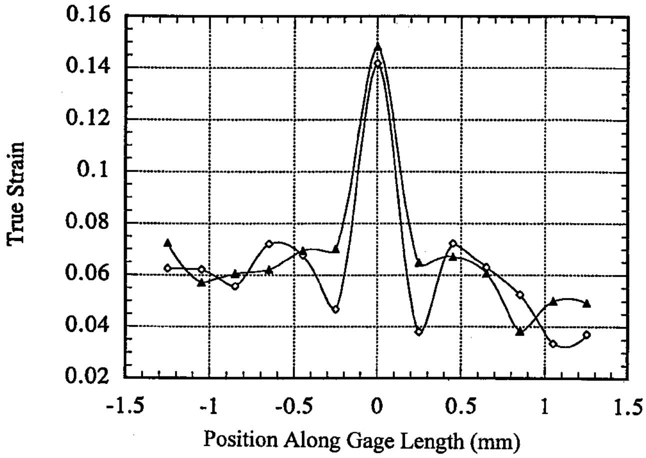


Fig. 2–The major strain distribution along a ZIRCALOY-4 cladding tube specimen containing a 20-μm deep groove and subjected to transverse plane-strain tension. The strain measurements were performed after specimen failure.

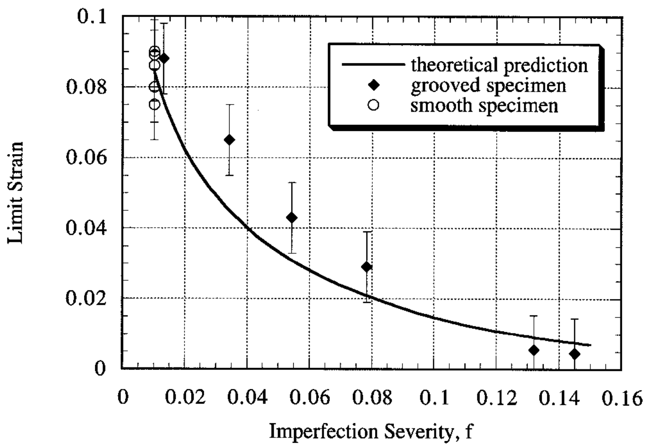


Fig. 3–Experimental results and predictions based on the M–K theory [1,3] showing the limit strains as a function of imperfection severity for ZIRCALOY-4 cladding tube specimens deformed in transverse plane-strain tension.

those of Wilson *et al.*,[8] who observe that a long groove with $f = 0.03$ reduces the biaxial limit strain of aluminum sheets.

We now consider the use of the M–K analysis to calculate the limit strain as a function of the groove severity for our specimens.[11] In the present study, our grooves are sufficiently wide (0.5 mm) that homogeneous deformation occurs within the groove and the plane stress assumption in the M–K analysis is satisfied.[3] Each specimen contains only a single groove. Furthermore, since our specimens are initially in a condition close to plane strain, there is no need for a large strain-path change to occur within the groove to achieve the local plane-strain condition required for localized necking.[13] We also note that although the material is plastically anisotropic, localized necking in plane-strain tension should be independent of R value,[4,14] and thus, our results should not be affected by the inherent plastic anisotropy of ZIRCALOY. In summary, in our case, we should be able to use the M–K analysis to predict with accuracy the influence of imperfection severity on the limit strain of these ZIRCALOY specimens.

In order to predict the sensitivity of sheet metal ductility

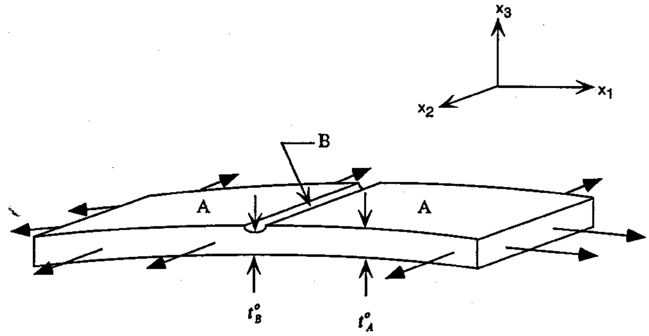


Fig. 4–A schematic drawing showing a linear imperfection in the form of a long groove oriented normal to the maximum principal stress.

to the presence of linear imperfections, such as the groove shown in Figure 4, the M–K analysis assumes load transfer along the specimen in the x_1 direction in Figure 2. For the case of a long groove oriented normal to the major principal strain direction, we define the severity, f, of the imperfection as

$$f = 1 - \frac{t_B^0}{t_A^0} \quad [1]$$

where t_A^0 and t_B^0 are the initial thicknesses of regions A and B. Using the von Mises flow theory, under plane-strain tension, the strains in the grooved and ungrooved regions of the specimen evolve as[3]

$$(\varepsilon_1^A)^{n/m} \exp(-\varepsilon_1^A/m)\, d\varepsilon_1^A = (1 - f)^{1/m}(\varepsilon_1^B)^{n/m} \exp(-\varepsilon_1^B/m)\, d\varepsilon_1^B \quad [2]$$

where ε_1^A and ε_1^B are the major principal strains in regions A and B, respectively; n is the strain-hardening exponent; and m is the strain-rate hardening exponent.

Equation [2] can predict the onset of localized necking in terms of three variables: the imperfection severity f, the strain hardening exponent n, and the strain-rate hardening exponent m. Since n and m are material properties whose values have been determined in this study, we may solve Eq. [2] numerically to obtain the dependence of strain within the groove as a function of the strain outside of the groove for a given imperfection severity. Figure 5 shows an example of such deformation localization curves for the case of unirradiated ZIRCALOY cladding ($n \cong 0.07$ and $m = 0.02$) containing grooves of three different depths. The strain accumulated outside the neck, at the point where deformation concentrates within the neck, is termed the "limit strain" and is indicated by the arrows in Figure 5 in each case. As the imperfection severity increases, the presence of the groove induces flow localization earlier in the deformation process; the net effect is a reduced limit strain, as shown in Figure 3. In sheet metal forming, such a limit strain at the onset of such *localized necking* is typically used as a failure criterion for the sheet being formed.

We now compare the M–K predictions with the experimental results shown in Figure 3. It is important to realize that there are no adjustable parameters in this analysis; we specify the severity f of the long groove in our material with known strain hardening and strain-rate hardening exponents and which is undergoing plane-strain deformation. As shown

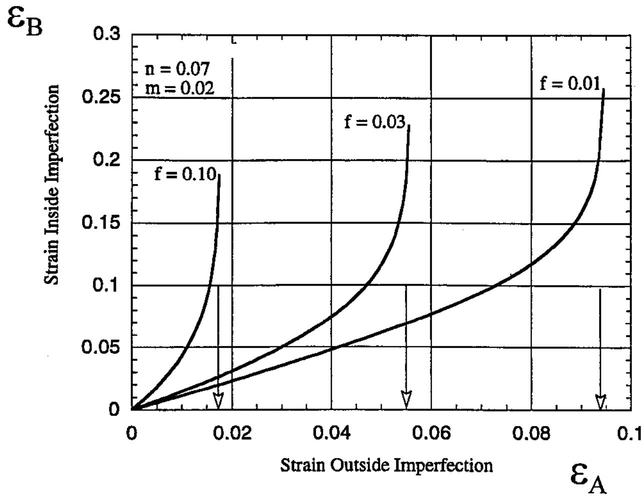


Fig. 5–Strain outside groove ε_A *vs* strain inside the imperfection ε_B, showing the evolution of deformation localization as a function of imperfection severity *f* for a material subject to plane-strain tension. The arrows indicate the limit strain. The values of *m* and *n* used here are valid for ZIRCALOY-4 at room temperature.

in Figure 3, there is good agreement between the experimental results and the theoretical analysis based on M–K predictions. The observed loss of ductility with imperfection severity is predicted quite well by the analysis.

The results from several smooth (*i.e.*, ungrooved) test specimens have also been included in Figure 3, and the corresponding limit strains are adequately predicted using an imperfection severity of 0.01. Such an imperfection size is consistent with the measured thickness variations of 3 pct, which are present around the circumference of as-received cladding and which extend along the cladding length. Thus, a 1 pct thickness imperfection is likely to exist within the notched gage section of the cladding simply due to the cladding fabrication process.

In summary, the failure behavior of thin ZIRCALOY-4 specimens containing long grooves is consistent with predictions based on the M–K analysis. The present experimental conditions are such that the "far-field," plane-strain deformation path roughly coincides with that in the groove from the beginning and throughout the deformation process, which permits a straightforward analysis. However, this also means that these experiments do not test the theory under conditions in which the far-field strain path differs markedly from plane-strain tension, such as in the case of equal-biaxial tension. As a result, there is good agreement between the observed influence of groove severity on failure strains with that predicted by the M–K analysis. The results indicate a strong sensitivity of the failure strains to the presence of even small imperfections for a material with small strain and strain-rate hardening capacity.

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