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DIRECT MEASURMENT OF THE J INTEGRAL ON A PRESSURE VESSEL

DIREKTNO MERENJE J INTEGRALA NA POSUDI POD PRITISKOM

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Keywords

- direct measurement of J integral
- cylindrical pressure vessel
- material heterogeneity
- boundary conditions

Ključne reči

- direktno merenje J integrala
- cilindrična posuda pod pritiskom
- heterogenost materijala
- granični uslovi

Abstract

Direct measurement of J integral has been analysed in respect to the effect of material (heterogeneity and strengthening) and different boundary conditions, enabling its application to pressure vessels with a crack in welded joint. It has been shown that modifications of the original expression are needed. The possibility of applying direct measurement of J integral for pressure vessel integrity assessment has been considered.

Izvod

Direktno merenje J integrala je analizirano u odnosu na uticaj heterogenosti i ojačavanja materijala, kao i različitih graničnih uslova, a u cilju primene na posude pod pritiskom sa prslinom u zavarenom spoju. Pokazano je da su neophodne modifikacije originalnog izraza. Takođe su razmatrane mogućnosti primene direktnog merenja J integrala kao metode procene integriteta posude pod pritiskom.

INTRODUCTION

Unlike the standard determination of the J integral, direct measurement is based on the path independence of the J integral, by choosing the most suitable (outer) contour. Because of this, the measured values do not depend on crack length, making this method more universal than the standard ones, more so because the shape and dimension of specimens are irrelevant. On the other hand, direct measurement is more complicated and expensive than the standard procedure, since it requires the use of strain gauges, including chains for strain measurements in a large number of points located over a distance, as small as possible.

The basic form of the method is defined by Oh, /1/, but it was first applied in fracture mechanics by Read and his associates, /2/. The first version of the application was for thin, wide plates with edge cracks, subjected to loads that enable rotation of outer edges, Fig. 1. A measuring error was assessed to be 3–5%, /2/, and this it is primarily caused by integration with a finite number of points.

Its next application was related to central surface cracks /3/ in a plate fixed by jaws in such a way that prevents the rotation of edges under the load. In this case, basic relations remain the same, but the error margin is increased to 10% /3/, since the 3D nature of the problem is partially neglected. This leads to obvious disruption of boundary conditions, same as when applying four point bending to a plate, as discussed in paper /4/. Further modifications and applications included elastic-plastic material with strengthening instead of an elastic-ideal plastic material, /5/, heterogeneous material (welded joints) instead of homogeneous, /6/, and biaxial tension instead of uniaxial, /5/.

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The principle of the method consists of determining the J integral by calculating integrand members for the corresponding contour, Fig. 1, according to the formula:

$$J = \int_{\Gamma} W dy - \vec{T} \frac{\partial \vec{u}}{\partial x} ds \qquad (1)$$

where x and y represent Cartesian coordinates with an origin at the tip of an edge crack, Fig. 1, W is the strain work density, $\vec{T}$ is the tension vector on contour Γ, $\vec{u}$ is the displacement vector, and ds is a curvilinear element of contour Γ. The stress-strain curve representing the elastic-ideally plastic material behaviour, to which the method will be applied, is given in Fig. 2.

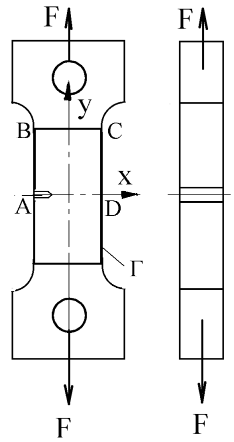

Figure 1. Contour Γ on a tensile specimen with an edge crack.
Slika 1. Kontura Γ na zateznoj epruveti sa ivičnom prslinom

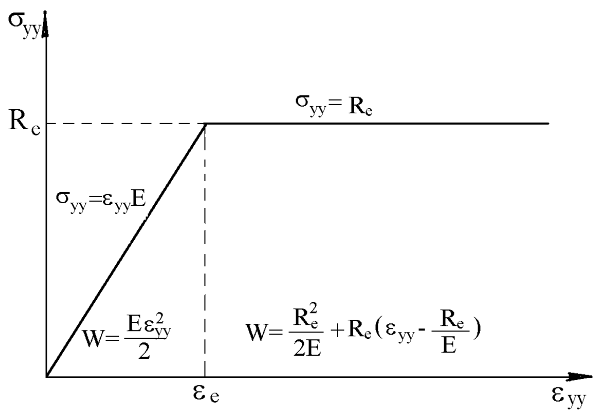

Figure 2. Stress-strain curve for elastic-ideally plastic material behaviour.
Slika 2. Kriva napon-deformacija za elasto-idealno plastično ponašanje materijala

As can be seen in Fig. 1, the problem is symmetrical to the crack plane, hence strain gauges are placed only along one half of the specimen contour. The choice of contour is in accordance with its geometry and position of the edge crack, so that this contour is made of free specimen surfaces along the y axis, away from the crack plane. Plane stress state is expected along this contour, since its thickness is small in comparison with the length and width of specimen and there are no stresses in that direction.

Two integrand expressions in Eq.(1), i.e. the strain energy member Wdy and tensile-bending member $\vec{T}\frac{\partial \vec{u}}{\partial x}ds$ are calculated for contour segments AB, CD and BC as follows.

The strain work density W is determined from:

$$W = \int \sigma_{ij} d\varepsilon_{ij}$$

where σ_{ij} is the stress tensor and ε_{ij} is the strain tensor. The full expression is given as:

$$W = \int \sigma_{xx} d\varepsilon_{xx} + \sigma_{yy} d\varepsilon_{yy} + \sigma_{zz} d\varepsilon_{zz} +$$
$$+ \sigma_{xy} d\varepsilon_{xy} + \sigma_{yz} d\varepsilon_{yz} + \sigma_{zx} d\varepsilon_{zx}$$

For the plane stress state one gets:

$$\sigma_{zz} = 0, \quad \sigma_{yz} = 0, \quad \sigma_{zx} = 0$$

hence:

$$W = \int \sigma_{xx} d\varepsilon_{xx} + \sigma_{yy} d\varepsilon_{yy} + \sigma_{xy} d\varepsilon_{xy}$$

i.e., for contour segments AB and CD

$$W = \int \sigma_{yy} d\varepsilon_{yy}$$

since stress tensor components along the free surface are all equal to zero, except σ_{yy}.

Within the area of linear elasticity, before the yield stress is reached, the following law applies:

$$\sigma_{yy} = E\varepsilon_{yy}$$

hence the yield criteria is $E\varepsilon_{yy} = R_e$, where R_e is the nominal yield stress of the material. In the case that the elastic-ideally plastic law of material behaviour is adopted, Fig. 2, the following expressions apply:

$$\sigma_{yy} = E\varepsilon_{yy} \quad \text{for} \quad \varepsilon_{yy} \le \frac{R_e}{E}$$

$$\sigma_{yy} = R_e \quad \text{for} \quad \varepsilon_{yy} > \frac{R_e}{E}$$

By using these expressions, the strain energy density becomes:

$$W = \frac{1}{2}\sigma_{yy}\varepsilon_{yy} = \frac{E\varepsilon_{yy}^2}{2} \quad \text{for} \quad \varepsilon_{yy} \le \frac{R_e}{E}$$

$$W = \frac{R_e^2}{2E} + R_e\left(\varepsilon_{yy} - \frac{R_e}{E}\right) \quad \text{for} \quad \varepsilon_{yy} > \frac{R_e}{E}.$$

Segment BC, where the contour intersects with the specimen, needs to be at a sufficient distance from the crack plane in order to avoid the effects of crack tip on stress-strain distribution fields.

Expressions for strain energy density obtained in this way are used for calculating the first member of the J integral along the contour segments AB and CD. Tensile force $\vec{T}$ equals zero along the contour segments AB and CD, since they are located at free surfaces, hence the second member in the J integral expression equals zero for these segments. On segment BC, $dy = 0$, since this segment is parallel to the x axis, hence the first member equals zero for this segment. Tensile force $\vec{T}$ is calculated as:

$$T_i = \sigma_{ij} n_j \quad (i,j = x,y,z)$$

where n_j is the unit vector along the outer normal to the contour Γ. Taking plane stress state into account, the following expressions apply in the Cartesian coordinate system:

$$T_x = \sigma_{xx}n_x + \sigma_{xy}n_y\,; \quad T_y = \sigma_{yx}n_x + \sigma_{yy}n_y$$

Along segment BC, $n_y = 1$, and $n_x = 0$, so one gets:

$$T_x = \sigma_{xy}\,, \quad T_y = \sigma_{yy}\,.$$

Shear component σ_{xy} can be neglected since the chosen contour segment is parallel to the x axis and at a sufficient distance from the crack. Component u_x of the displacement vector $\vec{u}$ along segment BC can also be neglected, since there is no displacement in that direction. Hence the first integrand member in J integral for the contour segment BC is reduced to the product of the tensile force $\vec{T}$ and the change in displacement vector $\vec{u}$ along the x axis. Tensile component T_y is obtained from strain, measured using strain gauges at points B and C, Fig. 1, since all stress components except σ_{yy} can be neglected along the segment BC. Bending member $\partial u_y/\partial x$ along segment BC can be expressed as:

$$\frac{\partial u_y}{\partial x} = \frac{u_y(\mathrm{C}) - u_y(\mathrm{B})}{x(\mathrm{C}) - x(\mathrm{B})} \tag{2}$$

where displacements u_y(C) and u_y(B) at points C and B are measured using a linear variable differential transformer (LVDT). Variables x(C) and x(B) represent coordinates at points C and B, and the difference between them, x(C) – x(B), represents the specimen thickness. Strain ε_{yy} is obtained from strain gauges, which are set according to Fig. 3. These values of strain for calculating the work density W are limited to a finite number of locations along segments AB and CD, hence the value of the J integral is an approximation.

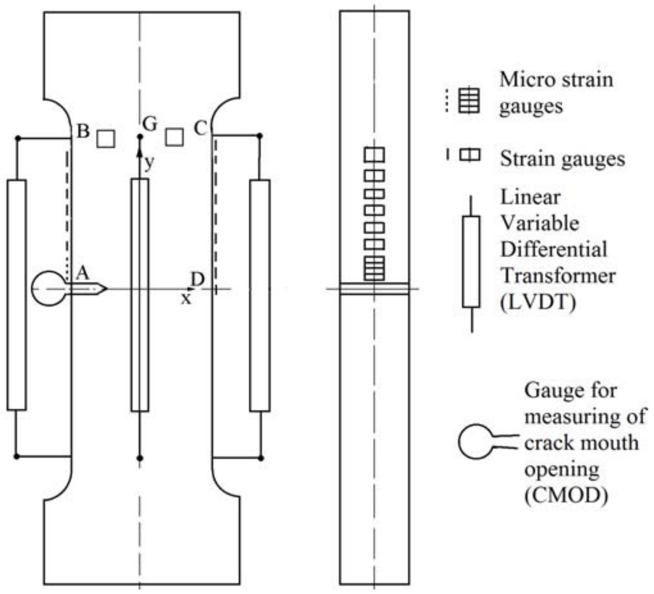

Figure 3. Instrumentation for tension tests of the edge cracked specimen.
Slika 3. Instrumentacija za ispitivanje na zatezanje epruvete sa ivičnom prslinom

Calculation of tensile-bending member, J_{BC}, is reduced to multiplying $T_y(\approx\sigma_{yy})$ with $u(C) - u(B)$, since $ds = dx$. By using numerical integration, it is possible to determine the values of displacements $u_y(C)$ and $u_y(B)$ via strain distribution, using the following expressions:

$$u_y(C)=\int_D^C \varepsilon_{yy}dy \quad \text{and} \quad u_y(B)=\int_A^B \varepsilon_{yy}dy+\frac{CMOD}{2}$$

where CMOD is the crack mouth opening displacement, measured using a special gauge, Fig. 3.

Direct numerical integration is used for calculating members in Eq.(1), since segments DC and AB are divided into smaller segments, each containing a single strain gauge. Each segment contribution is taken into account as the product of strain energy density, calculated from the measured strain from Eq.(2), and segment length. Thus, members J_{AB}, J_{BC} and J_{CD} are then added in the following way:

$$J = 2(J_{AB} + J_{CD} + J_{BC})$$

MODIFICATIONS OF THE ORIGINAL EXPRESSION

Material strengthening and heterogeneity

For obtaining a modified stress-strain curve with the use of elasto-plastic law with linear material hardening, the constant H' is introduced. It defines the slope of the curve in the plastic area, Fig. 4, hence the stress now becomes:

$$\sigma_{yy} = R_e + H'\left(\varepsilon_{yy} - \frac{R_e}{E}\right) \quad \text{for} \quad \varepsilon_{yy} > \frac{R_e}{E}$$

The strain work density for $\varepsilon_{yy} > R_e/E$ becomes:

$$W = \frac{1}{2}\frac{R_e^2}{E} + R_e\left(\varepsilon_{yy} - \frac{R_e}{E}\right) + \frac{1}{2}H'\left(\varepsilon_{yy} - \frac{R_e}{E}\right)^2$$

whereas the remaining J integral members are the same.

As shown in paper /6/, by applying the direct measurement of the J integral to welded joints requires the introduction of an additional line integral which brings back the feature of path independence. Since the additional integral is impossible to measure because its contour passes along the thickness direction (plane), the application of the direct measurement of the J integral is limited to welded joints where the influence of material heterogeneity is negligible. Author's experience has shown that this condition is generally fulfilled by homogeneous welds, /7-8/, whereas in case of welds such as ferrite-austenite, the additional integral needs to be taken into account.

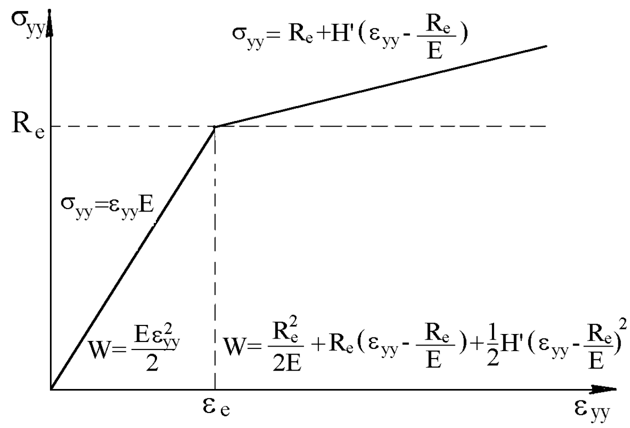

Figure 4. Stress-strain curve for a hardening material.
Slika 4. Kriva napon-deformacija za materijal koji ojačava

Two-dimensional stress analysis – pressure vessels

Direct measurement of the J integral can be applied to pressure vessels, assuming that differences in geometry, i.e. biaxial stress state is taken into account. For example, a cylindrical pressure vessel (with radius R and thickness t, ($t << R$) can be treated as a thin plate (with a curve), subjected to tensile load (circumferential and axial), in plane strain state conditions. An axial crack is of particular significance for cylindrical vessel analysis, since circumferential stress, σ_y is two times larger than axial stress, σ_x:

$$\sigma_x = 0 \quad \sigma_y = \frac{pR}{t} \quad \sigma_z = \frac{pR}{2t}$$

Based on the relation between stress and strain, the corresponding strain components are calculated as:

$$E\varepsilon_x = \sigma_x - \nu\sigma_z - \nu\sigma_y = -\frac{3}{2}\nu\frac{pR}{t}$$

$$E\varepsilon_y = \sigma_y - \nu\sigma_z - \nu\sigma_x = \left(1-\frac{\nu}{2}\right)\frac{pR}{t}$$

$$E\varepsilon_z = \sigma_z - \nu\sigma_y - \nu\sigma_x = \left(1-\frac{\nu}{2}\right)\frac{pR}{t}$$

For Poisson's ratio values of $\nu = 0.3$, these relations become:

$$E\varepsilon_x = 0.2\frac{pR}{t} \quad E\varepsilon_y = 0.85\frac{pR}{t} \quad E\varepsilon_z = -0.45\frac{pR}{t}$$

By introducing appropriate values for stresses in equivalent stress equation, the following is obtained:

$$\bar{\sigma} = \frac{1}{\sqrt{2}}\sqrt{(\sigma_z - \sigma_y)^2 + \sigma_z^2 + \sigma_y^2} = \frac{\sqrt{3}}{2}\frac{pR}{t} =$$

$$= 0.866\frac{pR}{t} = 0.866\sigma_y$$

Equivalent strain $\bar{\varepsilon}$ follows from Hooke's law:

$$\bar{\varepsilon} = \frac{\bar{\sigma}}{E} = 0.866\frac{pR}{Et}$$

Knowing that normal stress along the y axis in case of biaxial stress state is:

$$\sigma_y = \frac{E}{1-\nu^2}(\varepsilon_y + \nu\varepsilon_z)$$

and by using relations between strains ε_y and ε_z, values of circumferential stress can be obtained:

$$\sigma_y = \frac{E}{1-\nu^2}\left(\varepsilon_y + \nu\frac{0.5-\nu}{1-0.5\nu}\varepsilon_y\right) \approx 1.18E\varepsilon_y \tag{3}$$

being larger than the value of the normal stress $\sigma_y = E\varepsilon_y$, in the case of uniaxial interpretation of stress, over 18%, i.e. $2/(2-\nu)$. If Eq.(3) is included in the expression for equivalent stress, the following is obtained:

$$\bar{\sigma} = \frac{\sqrt{3}}{2}\frac{2}{2-\nu}E\varepsilon_y = \frac{\sqrt{3}}{2-\nu}E\varepsilon_y = \frac{\sqrt{3}}{2-0.3}E\varepsilon_y \approx 1.02E\varepsilon_y$$

$$\bar{\varepsilon} = \frac{\bar{\sigma}}{E} = \frac{R_e}{E} = 1.02\varepsilon_y$$

so that the equivalent stress and equivalent strain are calculated according to:

$$\bar{\sigma} = \frac{\sqrt{3}}{2}\sigma_y \;;\quad \bar{\varepsilon} = \frac{\sqrt{3}}{2-\nu}\varepsilon_y$$

Since ε_y is the measured strain, the corresponding yield criterion becomes:

$$\bar{\varepsilon} \geq \varepsilon_e, \quad \text{or} \quad 1.02\varepsilon_y \geq \frac{R_e}{E},$$

and since

$$0.866\frac{pR}{Et} \geq \frac{R_e}{E},$$

it can be concluded that the yield criterion for biaxial stress state is fulfilled (Von Mises criterion): $\bar{\sigma} \geq R_e$.

Finally, the measured strain, ε_y, must meet the following condition in the region of material yielding, based on the previous analysis:

$$\varepsilon_y \geq \frac{R_e}{1.02E}$$

which can be interpreted as a yield stress reduction (for a factor of 1.02), hence, equations for calculating the J integral can still be applied, assuming that this correction is made. It is also clear that for common calculations, this correction is negligible. However, it should be mentioned that in the case of spherical vessels, this factor would be considerably larger, around 1.3.

Fixed point- and four-point bending – modification of boundary conditions

If the ends of the plate are in the jaws of the testing machine so that they cannot rotate, then the boundary conditions change, or the tension-bending member becomes zero. This simple fact has not been noticed in the works dealing with such investigations, e.g. /3/, but it does not mean that the results are incorrect. Actually, it means that they can serve as a verification of the above mentioned. The situation is similar for pressure vessels, but in that case there is still a possibility for (small) rotation of loaded ends. Therefore, here are presented results of experimental investigation in which the components of the J-integral are separated into the tension-bending component, ST, and the deformation component, SW, so it follows: J = SW – ST. The mentioned components are presented in Figs. 5 (ST) and 6 (SW), taken from /9/, in both cases for wide plates under tension with under-matching and over-matching welded joints. Also, in Fig. 5 some of the ST components are presented, from measured CMOD (STCMOD), from base metal (STBM) and from weld metal (STWM), so it follows: ST = STBM + STWM – STCMOD.

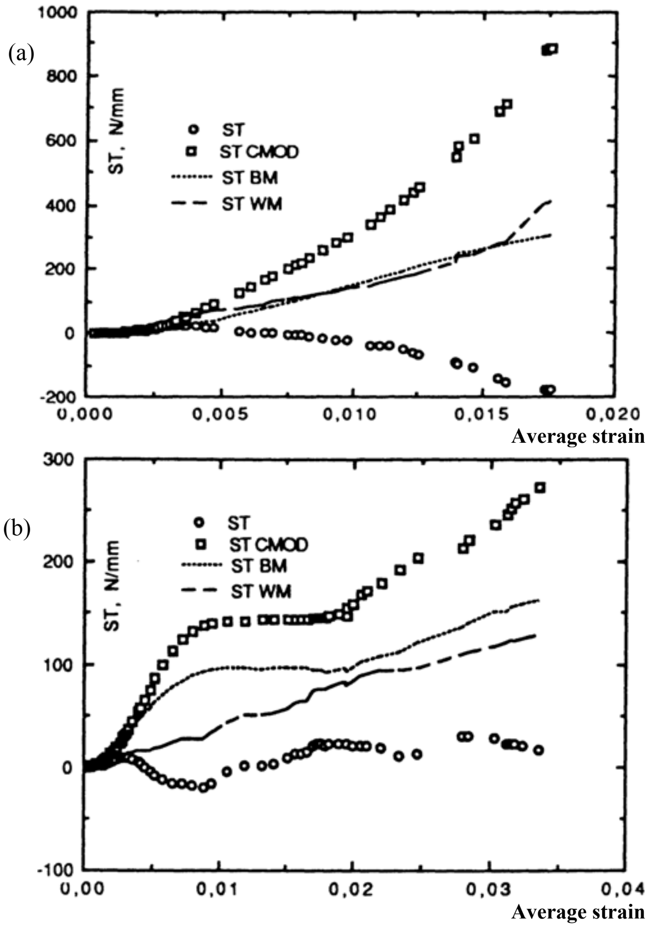

Figure 5. ST component of J integral, a) under-, b) over-matching, /9/
Slika 5. ST komponenta J integrala, a) ander-, b) over-mečing, /9/

In Fig. 6 contributions of SW components are presented, one from the crack side (SWN) and the other from the smooth side (SWUN).

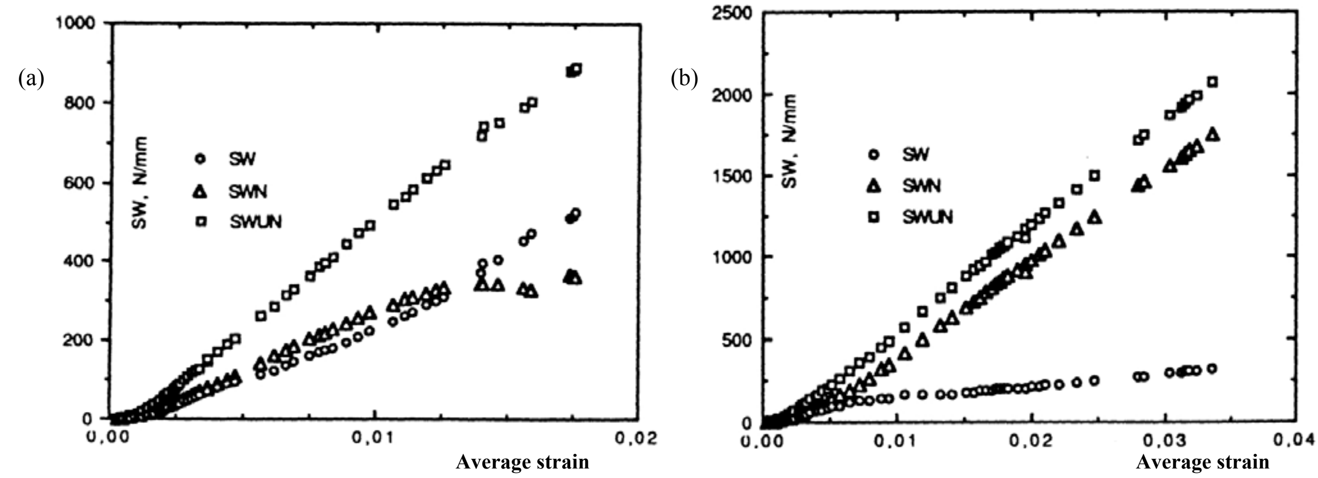

Figure 6. SW component of J integral: a) under-, b) over-matching, /9/
Slika 6. SW komponenta J integrala: a) ander-, b) over-mečing, /9/

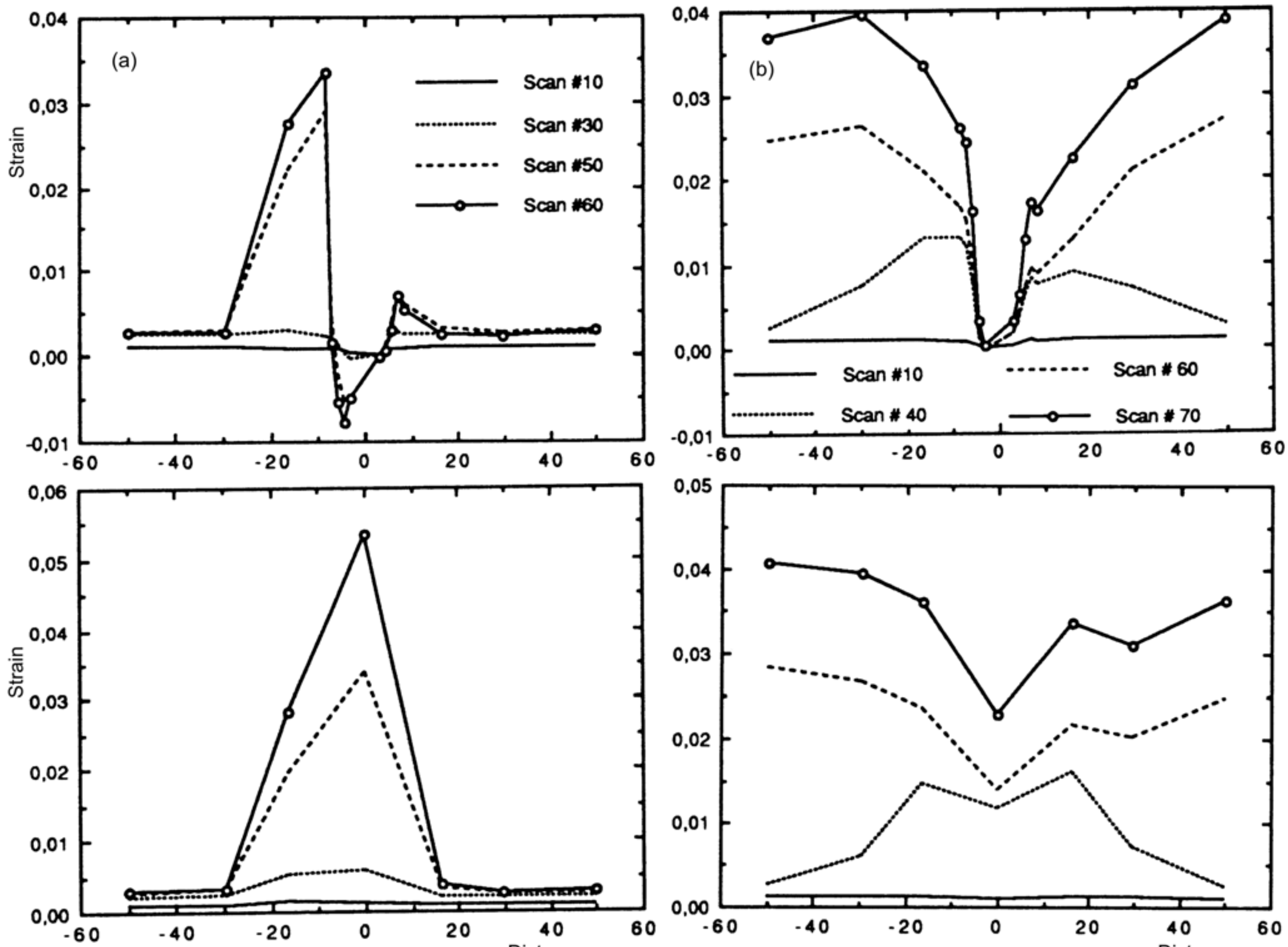

Figure 7. Deformation on the crack side and on the smooth side: a) under-matching, b) over-matching, /9/
Slika 7. Deformacije na strani prsline i na glatkoj strani: a) ander-mečing, b) over-mečing, /9/

Although these results basically show the already mentioned tendency, since in the over-matching joint ST ≈ 0 and SW ≈ 300 N/mm (for a remote deformation of 0.034), and in the under-matching joint ST ≈ –200 N/mm and SW ≈ 500 N/mm (for a remote deformation of 0.018), it is obvious that there also influence of material homogeneity. In fact, as it can be seen from Fig. 7, the distribution of the deformation is significantly different for the over-matching and the under-matching joint, and apparently a concentration and asymmetry of plastic deformation in the case of the under-matching joint, which is not in accordance with the assumption that CMOD is divided into two equal parts, and so this is used to calculate the ST in the study, /9/.

On the other hand, four-point bending requires further modification of the equations given here, since in that case the stress distribution σ_{yy} at the free edges is linear. Consequently, component J_{BC} becomes zero since it reduces to

$$\int_B^C \sigma_{yy} dx = \int_B^C x dx = x_C^2 - x_B^2 = 0 ,$$

whereby the coordinate system is placed as in Fig. 8. In that case, the J integral reduces to a difference of deformation energies at the smooth side and at the side of the crack, and CMOD measurement is not necessary. It is interesting to notice that the J integral is interpreted in the same way as in the study /1/, with graphical presentation as in Fig. 8.

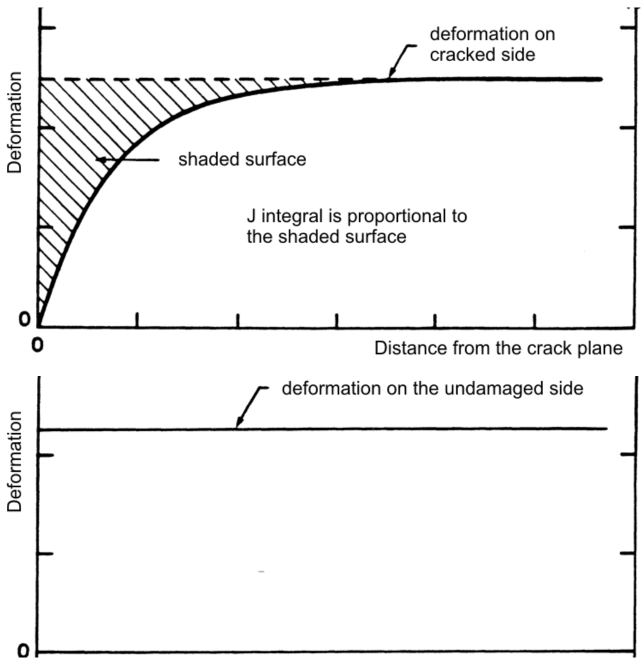

Figure 8. Deformations on the crack side and on the smooth side: interpretation of the J integral, /1/.
Slika 8. Deformacije na strani prsline i na glatkoj strani: tumačenje J integrala, /1/

DISCUSSION

It has been shown that modifications of the original expression for direct measurement of the J integral are necessary due to material heterogeneity and strengthening, as well as due to different boundary conditions and stress state in order to apply this simple experimental technique for pressure vessels with a crack in the welded joint.

In this paper, results indicate that the effects of material heterogeneity and strengthening are not significant for common ferritic steels, but may become significant if an austenitic steel is used for dissimilar welded joints.

Different boundary conditions can be an important effect when applying the direct measurement of the J integral to pressure vessels. Fixed ends of the integration path, instead of the rotating ones, change even the basic expression because of the different remote stress distribution. This has been clearly shown in the paper, but needs further consideration.

The biaxial stress state, as typical for pressure vessels, turned out to be important only for the spherical shape, and not also for the cylindrical.

Another important issue is possible application of Digital Image Correlation (DIC) technique of strain measurement instead of strain gauges, /10-12/. Although limited to outer surface measurements, this technique could be used for integrity assessment of pressure vessels with cracks located inside. Namely, a combination of measured strain distribution on the outer surface, a numerical evaluation of CMOD and a simple assumption that the inside strain distribution can be represented as bilinear (constant at the remote end up to the point where the maximal stain appears on the outer surface, and then reduces to zero at the location of the crack), leads to the simple evaluation of the J integral as a difference of strain energy densities on outer and inner surfaces, plus the contribution of CMOD. This possibility should be further elaborated, since it can lead to a simple Non-Destructive Technique (NDT), applied as a monitoring tool for pressure vessel testing.

CONCLUSIONS

Based on the results presented in this paper, one can conclude:

- The effect of the material heterogeneity, unless dissimilar welded joints are analysed (e.g. ferrite-austenite), is negligible.
- The effect of strengthening is also not significant, except for austenite and similar materials, with large plasticity and a significant difference between yield and tensile strength.
- Based on the introduced expressions for the bi-axial stress state, one can conclude that the difference is negligible (2%) in the case of a cylindrical pressure vessel, whereas it can be significant (30%) in the case of a spherical pressure vessel.
- The possibility of using direct measurement of J integral as a technique for pressure vessel integrity assessment has been introduced, based on the application of DIC for strain measurement on the outer surface, the numerical evaluation of CMOD and the assumption of bilinear strain distribution on the inner surface.

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