EFFECTS OF WELD STRENGTH MISMATCH ON CTOD AND *J* ESTIMATION PROCEDURES APPLICABLE TO CLAMPED SE(T) SPECIMENS CONTAINING SQUARE GROOVE WELDS WITH CENTER CRACKS

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Abstract. *The current work presents an exploratory development of CTOD and J estimation procedures considering clamped SE(T) specimens containing square groove welds with center cracks. The primary objectives are to evaluate the effects of weld strength mismatch on crack driving forces experimental evaluation and to develop procedures applicable to welded SE(T) specimens with a wide range of a/W-ratios and mismatch levels. The motivation is based on the increasing demand for safety and reliability of welded pressurized components such as pressure vessels, storage tanks and piping systems, whose crack-tip stress fields are more accurately described by SE(T) specimens if compared to conventional compact C(T) or bending SE(B) specimens. The technique considered includes estimating CTOD and J from plastic work and, to achieve these goals, very detailed non-linear finite element analyses for plane-strain models of clamped SE(T) fracture specimens with center cracked, square groove welds provide the evolution of load with increased load-line displacement (LLD) and crack mouth opening displacement (CMOD) which are required for the estimation procedure. The analyses intend to provide a fairly extensive body of results which serve to directly estimate CTOD and J from experimental data for different materials, geometries and mismatch levels using clamped SE(T) specimens. Additionally, the proposed methodology will support further investigations on the experimental evaluation of critical fracture toughness and* $J-R$ *(*$\delta - R$*) crack growing curves for welded SE(T) specimens.*

Keywords*: clamped SE(T) specimens, structural integrity, CTOD, J-integral, mismatched welds*

1. INTRODUCTION

Integrity assessments of cleavage fracture for ferritic steels (including welded structures) in the ductile-to-brittle transition (DBT) region are usually conducted based on fracture mechanics theory and rely upon the notion that a single parameter which defines the crack driving force (characterized by $J-$integral or the (analogous) Crack Tip Opening Displacement (CTOD, δ)) characterizes the fracture resistance of the material (with their corresponding macroscopic measures of cleavage fracture toughness (J_c or δ_c)). In addition, if severe plasticity develops, and ductile tearing occurs, the evaluation of crack driving forces (J and CTOD) can also support the experimental assessment of crack growing curves (usually referred to as $J-R$ curves), which characterizes the material's resistance against ductile crack propagation (Anderson, 2005). The experimental evaluation of fracture resistance is usually conducted based on standardized procedures such as ASTM E1290 (2008) and ASTM E1820 (2008), which employ three-point bend SE(B) and compact tension C(T) specimens containing deep, through cracks ($a/W \geq 0.45$) in order to guarantee high levels of stress triaxiality and the severity of the defect.

However, in view of interest from the petroleum industry and the increasing demand for safety, reliability and performance of pressurized components, recent work from Cravero and Ruggieri (2005, 2006) showed that tension SE(T) specimens present a much better description of pressurized pipelines crack tip stress fields than conventional bending SE(B) or compact tension C(T) fracture specimens. This behavior is due to the mechanical similarity of loading conditions between the real structures (usually shallow cracked pipelines or pressure vessels) and the SE(T) specimens. Cracked pipelines usually develop low levels of stress triaxiality (as a result of tensile loading combined to membrane stresses and internal pressure), which sharply contrasts to severe fracture conditions found on SE(B) or C(T) conventional specimens. Therefore, defect evaluation of pressurized pipelines and pressure vessels based on conventional deep cracked SE(B) and C(T) specimens can lead to highly inaccurate (conservative) results.

This context motivated the same researchers, Cravero and Ruggieri (2006), to develop procedures for J estimation in SE(T) specimens applicable to homogeneous materials, which are useful both for experimental evaluation of fracture toughness (e.g., J_c) and for $J-R$ curves estimation. However, the available results are limited to J estimation and homogeneous specimens. Therefore, fracture resistance evaluation based on CTOD and applicable to welded structures including weld strength mismatch between base metal and weld metal remains a potential open issue, which encourages the present investigation.

The fracture behavior of steel weldments (weld metal and heat affected zone - HAZ) plays a key role in safety analyses and integrity assessments of critical welded structures, including pressure vessels and storage tanks, piping systems, submarine hulls and offshore oil structures. Typical welding processes introduce strong thermal cycles in the weld metal and surrounding region, which often deteriorate the metallurgical quality and potentially lower the fracture toughness. Experimental observations consistently reveal the occurrence of a variety of crack-like defects in the welded region which are either planar (e.g., lack of penetration, undercut) or volumetric (e.g., porosity and entrapped slag) (AWS, 1987, Jutla, 1996, Kerr, 1976) even if good workmanship and proper selection of the welding procedure are employed.

To reduce the likelihood of structural failure, many codes require the use of weldments with weld metal strength higher than the baseplate strength – a condition referred to as overmatching. An evident benefit of this practice is to shift the plastic deformation into the lower strength baseplate where the fracture resistance is presumably higher and fewer defects occur. However, weld strength mismatch may strongly alter the relationship between remotely applied loading and crack-tip driving forces, including additional difficulties in current assessment practices, as previously shown by Donato and Ruggieri (2007, 2009) for SE(B) and SE(T) specimens. Consequently, accurate estimation formulas for crack driving forces evaluation which are applicable to welded fracture specimens are essential for the development of more refined defect assessment procedures capable of including effects of weld strength mismatch on fracture resistance. Therefore, in the present context, more accurate procedures to estimate CTOD and J for homogeneous specimens and mismatched welds appear essential for the emerging SE(T) specimens.

As a step in this direction, this work presents an exploratory development of CTOD and J estimation procedures considering clamped SE(T) specimens containing square groove welds with center cracks. The primary objectives are to evaluate the effects of weld strength mismatch on crack driving forces experimental evaluation and to develop procedures applicable to welded SE(T) specimens with a wide range of $a/W-$ratios and mismatch levels, including the reference homogeneous specimens. The technique considered includes estimating CTOD and J from plastic work and, to achieve these goals, very detailed non-linear finite element analyses for plane-strain models of clamped SE(T) fracture specimens with center cracked, square groove welds provide the evolution of load with increased load-line displacement (LLD) and crack mouth opening displacement (CMOD) which are required for the estimation procedure. The analyses intend to provide a fairly extensive body of results which serve to directly estimate CTOD and J from experimental data for different materials, geometries and mismatch levels using clamped SE(T) specimens. Additionally, the proposed methodology will support further investigations on the experimental evaluation of critical fracture toughness and $J-R$ ($\delta-R$) crack growing curves for welded SE(T) specimens.

2. PROCEDURES FOR *J* AND CTOD EXPERIMENTAL ESTIMATION – THE *ETA* METHOD

Experimental evaluation of the nonlinear energy release rate, represented by the $J-$integral, is usually conducted based on laboratory measurements of load-displacement records obtained from fracture mechanics specimens (Fig. 1).

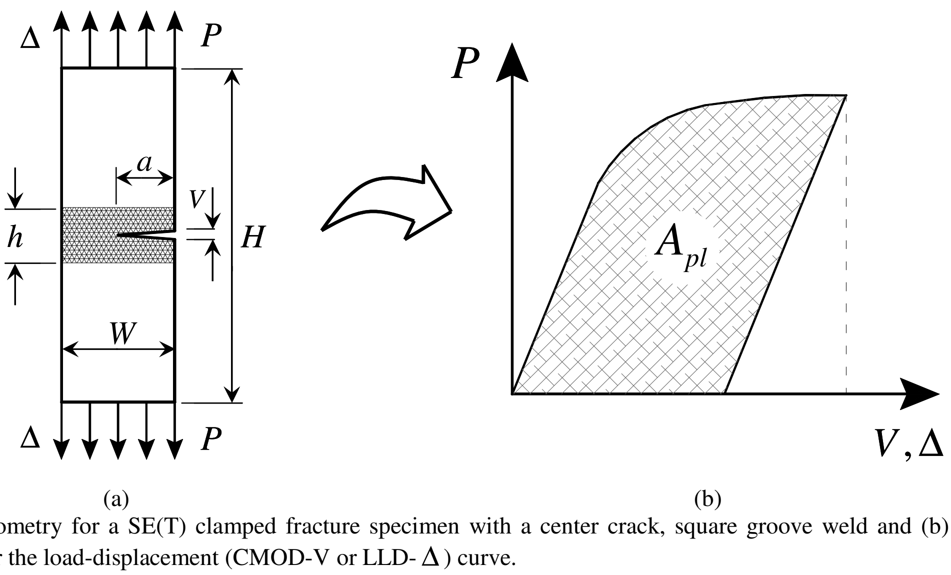


Figure 1. (a) Geometry for a SE(T) clamped fracture specimen with a center crack, square groove weld and (b) definition of the plastic area under the load-displacement (CMOD-V or LLD- Δ) curve.

The computations of J consider the elastic and plastic contributions to the strain energy for a cracked body under Mode I deformation as (Anderson, 2005)

$$J = J_{el} + J_{pl} \; , \tag{1}$$

where the elastic component, J_{el}, is given by (Anderson, 2005, ASTM E1820, 2008)

$$J_{el} = \frac{K_I^2}{E'} \ . \tag{2}$$

Here, E' represents the Young Modulus for plane strain condition (defined as $E' = E/(1-\nu^2)$) (Anderson, 2005) and the elastic stress intensity factor, K_I, is defined for SE(T) specimens as

$$K_I = \frac{P}{B \cdot \sqrt{W}} \cdot f(a/W) \ , \tag{3}$$

where P is the applied load, B is the specimen thickness, W is the specimen width and $f(a/W)$ defines a nondimensional stress intensity factor recently studied for SE(T) specimens by Chiodo and Ruggieri (2006). For the clampled SE(T) specimens with $H/W = 4$ (see Fig. 1a for H definition) under research in this paper, the nondimensional stress intensity factor is proposed by these researchers as

$$F(a/W) = 0.2565 + 4.4604(a/W) - 7.0538(a/W)^2 + 18.6928(a/W)^3 - 19.4703(a/W)^4 + 9.2523(a/W)^5 \ . \tag{4}$$

It can be realized that the elastic component of J is only dependent on geometric features and the instantaneous load level. The plastic component, J_{pl}, in its turn, depends upon the plastic area under the load-displacement curve and can be evaluated as

$$J_{pl} = \frac{\eta_J^{WM} \cdot A_{pl}^{WM}}{B \cdot (W-a)} \ , \tag{5}$$

where A_{pl}^{WM} represents the plastic area under the load-displacement curve (see Fig. 1) and factor η_J^{WM} represents a nondimensional parameter which describes the effect of plastic strain energy on the applied J (Rice *et al.*, 1973, Sumpter and Turner, 1976) for the weld specimen (to clearly identify the *eta*-factors related to cracks in the center of the weld grooves, they are denoted here η_J^{WM}). The previous definition for J_{pl} derives from the assumption of nonlinear elastic material response thereby providing a deformation plasticity quantity. Figure 1b illustrates the procedure to obtain the plastic area under the load-displacement curve in terms of crack mouth opening displacement (CMOD or V) or load line displacement (LLD or Δ) data for a center notch weld specimen.

To systematically assess the effect of weld strength mismatch on the mechanical behavior of the specimen (and consequently on $\eta-$factors), the mismatch ratio, M_L, is conveniently defined here as

$$M_L = \frac{\sigma_{ys}^{WM}}{\sigma_{ys}^{BM}} \tag{6}$$

where σ_{ys}^{BM} and σ_{ys}^{WM} represent the yield stress for the base plate metal and weld metal respectively.

Using the connection between J and δ proposed by Shih (1981) and following the previous energy release rate interpretation of the $J-$integral, a similar formulation also applies when the CTOD is adopted to characterize the material's fracture resistance. This way, experimental CTOD evaluation can be conducted considering its elastic and plastic contributions as

$$\delta = \delta_{el} + \delta_{pl} = \frac{K_I^2}{m\sigma_{ys}E'} + \frac{\eta_\delta^{WM} A_{pl}^{WM}}{B\sigma_f (W-a)} \tag{7}$$

Here, factor η_δ^{WM} is analogous to previously presented η_J^{WM} and represents a nondimensional parameter which describes the effect of plastic strain energy on the applied CTOD. In addition, parameter m represents a plastic constraint factor dependent on the stress state and material properties (often assigned a value of approximately 2 in current standards (ASTM E1290, 2008, ASTM E1820, 2008) for plane strain conditions (Anderson, 2005)) and σ_f denotes the flow stress defined as $\sigma_f = (\sigma_{ys} + \sigma_{uts})/2$ where σ_{ys} is the yield stress and σ_{uts} is the ultimate tensile strength. In the present context (center cracked square groove weld), the crack-tip region is symetrically surrounded by weld metal and its mechanical properties should be used for the computations.

The estimation of $\eta -$factors can be conducted based on limit load solutions derived for homogeneous and mismatched specimens, as developed in the last decades by many researchers (e.g. Hornet *et al.*, 1997, Roos *et al.*, 1986, Sharobeam and Landes, 1991, Wang and Gordon, 1992), or can be directly assessed from refined nonlinear finite element analyses. The approach adopted in the present paper is based on finite element computations and was selected due to the accuracy in describing crack-tip stress fields and mismatch effects on crack-tip driving forces. All the details related to the finite element models and computational procedures are presented in the next section.

3. NUMERICAL PROCEDURES

3.1. Finite Element Models

Detailed finite element analyses are performed on plane-strain models for a wide range of 1-T clamped SE(T) specimens ($B = 25.4mm$ and conventional geometry with $W = 2 \cdot B$ and $H/W = 4$) having a center cracked, square groove weld with different groove weld width and weld strength mismatch. The analysis matrix includes 80 models of specimens with $a/W =$0.1, 0.2, 0.3, 0.5 and 0.7 and $h =$5, 10 and 20mm for different mismatch conditions. Here, a represents the specimen crack size, W represents the specimen width, H represents the specimen span and h represents the weld groove width. Figure 1(a) showed the geometry under investigation and the main dimensions for the analyzed crack configurations.

Figure 2, in its turn, shows an example of finite element model constructed for the plane-strain analyses of deeply-cracked clamped SE(T) specimen with $a/W = 0.5$ and a center cracked, square-groove weld. The weld fracture specimen is modeled as bimaterial with no transition region, i.e., the heat affected zone (HAZ) is not considered. All other crack models have very similar features and are not detailed here due to space limitations. A conventional mesh configuration having a focused ring of elements surrounding the crack front is used with a small key-hole at the crack-tip; the radius of the key-hole, ρ_0, is $2.5\mu m$ (0.0025mm). Symmetry conditions permit modeling of only one-half of the specimen with appropriate constraints imposed on the remaining ligament. The half-symmetric model has one thickness layer of 2600 8-node, 3-D elements (~ 5300 nodes) with plane-strain constraints imposed ($w = 0$) on each node of the model. All the finite element models are loaded by displacement increments imposed on the loading points to enhance numerical convergence. Its worth noting that, for a fixed a/W, the developed mesh pattern allows different groove widths (h) to be modeled using the same mesh (simply altering material properties of the elements near the interface), which enhances the stability and comparability of the numerical solutions.

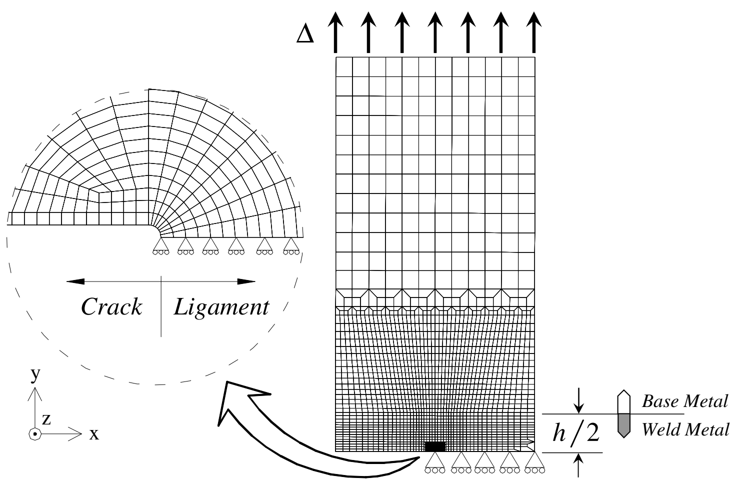


Figure 2. Finite element model used in plane-strain analyses of a deeply-cracked ($a/W = 0.5$) clamped SE(T) fracture specimen. Symmetry conditions were applied and the mesh pattern allows different groove widths (h) to be modeled using the same mesh.

3.2. Computational Procedures

The research finite element code WARP3D (Koppenhoefer *et al.*, 1994) provides the numerical solution for the plane-strain analyses reported here. The code incorporates a Mises (J_2) constitutive model in both small-strain and finite-strain framework. Evaluation of the $J -$integral derives from a domain integral procedure (Moran and Shih, 1987) which yields $J-$values in excellent agreement with estimation schemes based upon *eta*-factors for deformation plasticity (Anderson, 2005) while, at the same time, retaining strong path independence for domains defined outside the highly strained material near the crack tip. Evaluation of the CTOD values, in its turn, derives from the 90º intercept method proposed by Rice (1968) and was conducted using a FORTRAN code specifically developed by the author.

3.3. Material Laws

Evaluation of factor η requires nonlinear finite element solutions which include the effects of plastic work on J (CTOD) and the load-displacement response. The present analyses utilize an elastic-plastic constitutive model with J_2 flow theory and conventional Mises plasticity in small geometry change (SGC) setting. The numerical solutions employ a simple power-hardening model to characterize the uniaxial true stress-logarithmic strain in the form

$$\frac{\varepsilon}{\varepsilon_{ys}} = \frac{\bar{\sigma}}{\sigma_{ys}} \quad \varepsilon \le \varepsilon_{ys}\,; \quad \frac{\varepsilon}{\varepsilon_{ys}} = \left(\frac{\bar{\sigma}}{\sigma_{ys}}\right)^n \quad \varepsilon > \varepsilon_{ys}\ , \tag{10}$$

where σ_{ys} and ε_{ys} are the reference (yield) stress and strain, and n is the strain hardening exponent.

The finite element analyses consider material flow properties covering a wide range of strength mismatch: 40% and 20% undermatch, evenmatch, 20%, 50% and 100% overmatch (with the mismatch level referred to as M_L - Mismatch Level - as defined in Eq. (6)). The higher levels of mismatch (mainly with $M_L \ge 1.5$) are not usual in common practice for pipeline construction, but were included in the analysis matrix for completeness and better understanding of the specimen's mechanical response. The welds are modeled as bimaterials (the heat affected zone, HAZ, is not considered in the present work) with the yield stress and hardening property of the base plate adopted as fixed in all analyses and assigned the following properties: $n = 10$ and $\sigma_{ys} = 412$ MPa. Table 1 provides the material properties used in the numerical analyses (defined in terms of the yield stresses to achieve the desired mismatch levels) of the fracture specimens with square groove welds, which also consider $E = 206$GPa and $\nu = 0.3$. The respective strain hardening parameters for the weld metal are estimated based upon a simple correlation between the yield stress and hardening exponent applicable for typical structural steels (API RP 579-1, 2007, Cravero and Ruggieri, 2007): $n = 5$ and $E/\sigma_{ys} = 800$ (high hardening material), $n = 10$ and $E/\sigma_{ys} = 500$ (moderate hardening material), $n = 20$ and $E/\sigma_{ys} = 300$ (low hardening material). The hardening exponents for the weld metal are given by quadratic interpolation of the previous adopted values for σ_{ys} and n. These ranges of properties also reflect the upward trend in yield stress with the increase in strain hardening exponent characteristic of ferritic steels.

Table 1 – Material properties adopted in the analyses of the weldments.

Mismatch Level	Weld		Base Plate	
	σ_{ys} (MPa)	n	σ_{ys} (MPa)	n
40% Undermatch	247	4.7	412	10
20% Undermatch	330	7.3	412	10
20% Overmatch	494	12.8	412	10
50% Overmatch	618	17.4	412	10
100% Overmatch	824	25.5	412	10
Evenmatch	412	10	412	10

4. PLASTIC *ETA*-FACTORS

Numerical evaluation of plastic $\eta-$factors for the analyzed crack configurations follows from solving Eqs. (5) and (7) upon computation of the plastic area, A_{pl}^{WM} under the load-LLD or load-CMOD curves (see Fig. 1b). The corresponding total and elastic components of J and CTOD are obtained from the numerical computations as already described in the previous section. A key question to resolve with the numerical procedure lies in the choice of the deformation level (CMOD or LLD) at which A_{pl}^{WM} (and consequently factors η_J^{WM} and η_δ^{WM}) is evaluated. In the present study, to solve this question and increase estimation accuracy, Eqs. (5) and (7) were evaluated as increasing linear relations (of J_{pl} *vs.* $A_{pl}^{WM}/[B(W-a)]$ and δ_{pl} *vs.* $A_{pl}^{WM}/[B\sigma_f(W-a)]$ respectively), whose inclinations represent each of the corresponding $\eta-$factors (see Fig. 3 for representative cases). From the same figure can be realized that plastic components of J and CTOD are strongly proportional to the plastic area under Load-CMOD curve, which supports the described approach and provides accurate $\eta-$factors. The same behavior is observed for all other models, even for the use of LLD records.

Computations based on CMOD or LLD records lead to different plastic areas under the loading curve and consequently to different $\eta-$factors to evaluate crack driving forces. This way, to clearly identify each factor and its corresponding data (CMOD or LLD), they are denoted η_J^{CMOD} (*eta*-factor for J estimation in center cracks based on P-CMOD records), η_J^{LLD} (*eta*-factor for J estimation in center cracks based on P-LLD records) and η_δ^{CMOD} (*eta*-factor for CTOD estimation in center cracks based on P-CMOD records).

Attention must be called to one point. The global behavior of a mismatched fracture specimen may be strongly affected by the level of strength mismatch coupled with the weld groove size, as pointed out by many researchers, as for example Eripret and Hornet (1997). Therefore, even though the $\eta-$factors described here remains strictly valid for experimental crack driving force (J and CTOD) evaluation, if critical fracture toughness data (J_c or δ_c) is desired, gross section yielding cannot take place in the tested SE(T) specimens, otherwise the description of the near-tip stress fields by the measured J (CTOD) cannot be guaranteed, conducing to toughness data that do not generally represents the bimaterial system. The usual definition of limit load in terms of (local) plastic instability is based on elastic-perfect plastic analyses, which do not consider hardening response of materials and do not allow a precise evaluation of these limits of applicability. This way, a more precise definition of limit load for mismatched components is considered an open issue and the author has been working on it. Preliminary investigations show that, if hardening is considered for the definition of limit loads, the plastic zones are well embedded within the weld metal with minimal extension to the base metal, which validates all the presented $\eta-$factors for critical toughness measurement.

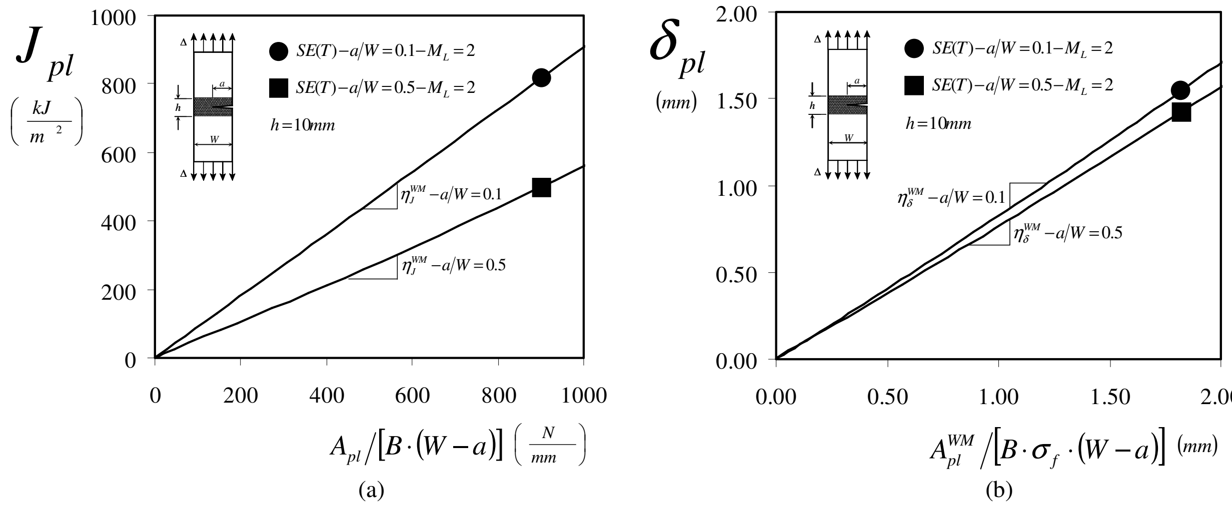
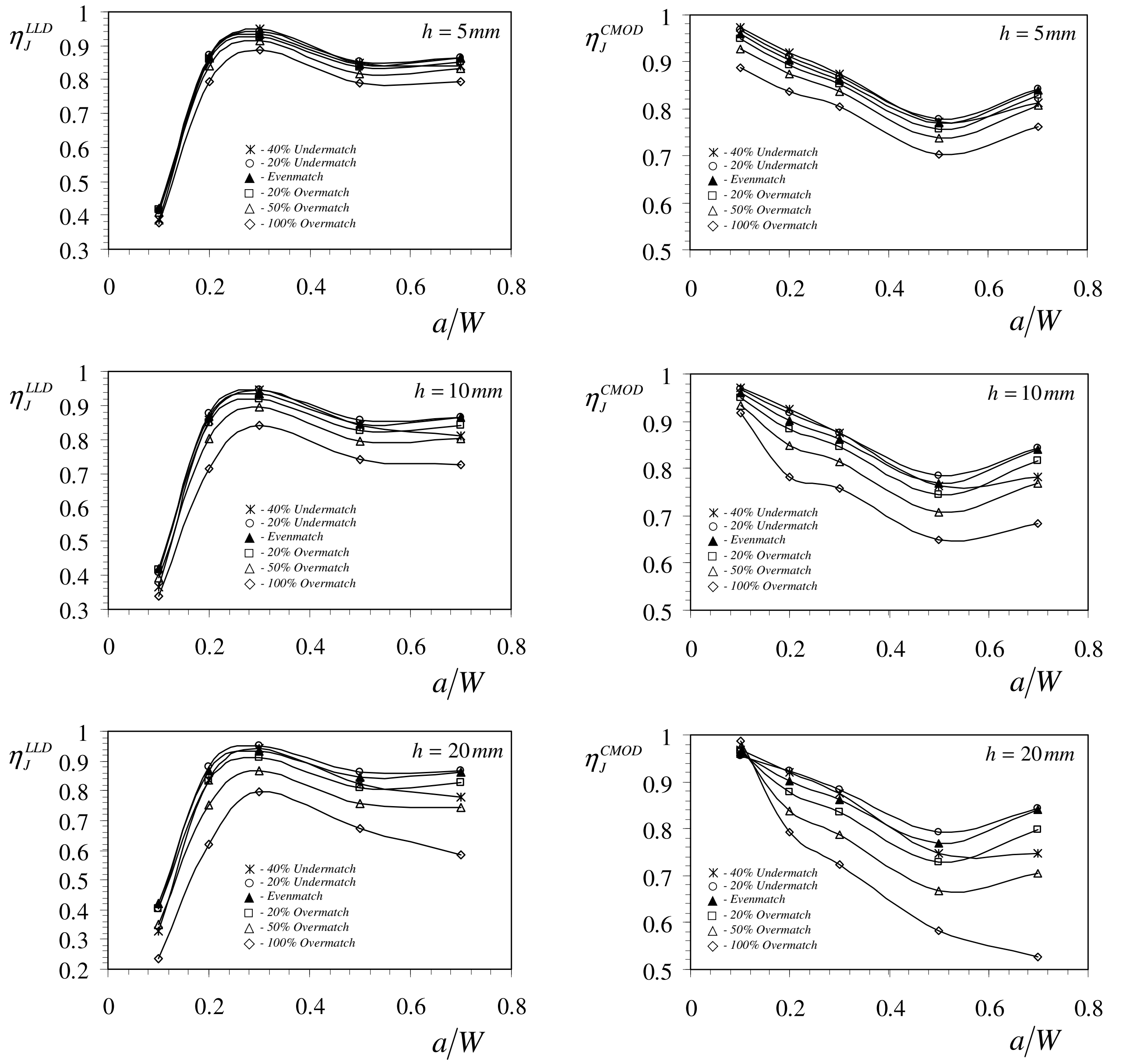


Figure 3. $\eta-$factors estimation schemes based on the linear relations between plastic area under the load-displacement curve (in this case, load-CMOD curves) and (a) plastic J (b) plastic CTOD.

Figure 4 presents the $\eta-$factors obtained from the plane strain analyses and applicable to evaluate J for different weldment properties and specimen configurations. Figure 4a shows the variation of η_J^{LLD} with increased $a/W-$ratio and different mismatch levels with groove sizes $h=5$mm ($h/W=0.1$), $h=10$mm ($h/W=0.2$) and $h=20$mm ($h/W=0.4$). The results displayed in these graphs reveal a strong dependence of η_J^{LLD} to a/W, mainly for shallow cracks ($a/W\leq 0.3$) for all cases of weld groove width. In addition, a significant influence of weld strength mismatch on *eta*-factors can be observed, mainly for the wider weld grooves (for $h=20$mm and $a/W=0.5$, are observed punctual deviations of ~ -10% for $M_L=1.5$ and ~ -20% for $M_L=2.0$). Figure 4b, in its turn, shows the variation of η_J^{CMOD} for the same conditions. The results from these graphs reveal a strong dependence of η_J^{CMOD} to a/W for all crack depths and for all studied cases of weld groove width. In addition, is again observed a significant influence of weld strength mismatch on *eta*-factors, mainly for wider weld grooves (see Fig. 4b - for $h=20$mm and $a/W=0.5$, are observed punctual deviations of ~ -13% for $M_L = 1.5$ and ~ -24% for $M_L = 2.0$). However, it can be seen from both figures that low mismatch levels (between $\pm 20\%$) do not strongly alter determined $\eta -$factors for J estimation, with maximum deviation under 10%.



(a) (b)

Figure 4 – (a) Variation of plastic η_J^{LLD} derived from LLD with increased $a/W -$ratio and different mismatch levels for $h = 5$, 10 and 20 mm groove sizes and (b) variation of plastic η_J^{CMOD} derived from CMOD for the same conditions.

Figure 5, in its turn, presents η_δ^{CMOD} factors obtained from the plane strain analyses and applicable to evaluate CTOD for the same conditions previously described. Here, a different context emerges. The results displayed in these graphs reveal a weak dependence of η_δ^{CMOD} to a/W, but demonstrates a severe and systematic dependence to mismatch level, for all groove width (see Fig. 5a - for $h = 5$mm and $a/W = 0.5$, are observed deviations of ~ 13% for $M_L = 1.2$, ~ 31% for $M_L = 1.5$ and ~ 59% for $M_L = 2.0$). In the case of CTOD experimental estimation, therefore, the consideration of weld strength mismatch is highly advisable for obtaining accurate crack driving forces values.

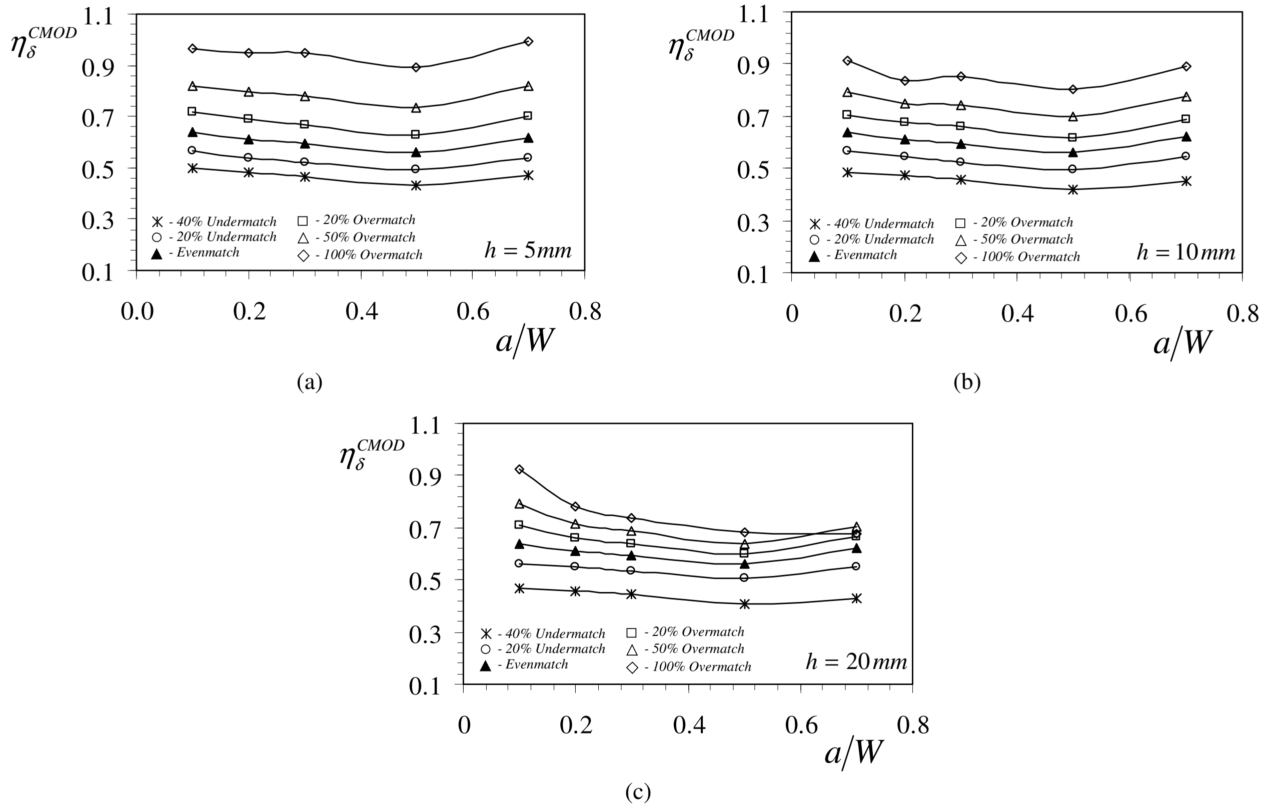


Figure 5 – Variation of plastic η_δ^{CMOD} derived from CMOD with increased $a/W-$ratio and different mismatch levels for (a) $h=5$, (b) $h=10$ and (c) $h=20$ mm groove sizes.

All $\eta-$factors determined for evenmatch condition ($M_L=1$) are in excellent agreement with previous results from the literature (Cravero and Ruggieri, 2007), which encourage the present studies and validate the conducted methodology. In view of the great importance of CTOD fracture tests for integrity assessments of welded mismatched components, Eq. (11) presents a multivariable least square fit with $R^2=0.98$ for the $\eta-$factors shown in Fig. 5.

$$\eta_\delta^{CMOD}=0.18648+0.00513\cdot h+0.61488\cdot M_L-0.49890\cdot\left(\frac{a}{W}\right)+0.00015\cdot h^2-0.08308\cdot M_L^2+$$
$$+0.68914\cdot\left(\frac{a}{W}\right)^2-0.00844\cdot h\cdot M_L-0.00623\cdot h\cdot\left(\frac{a}{W}\right)-0.04457\cdot M_L\cdot\left(\frac{a}{W}\right) \qquad (11)$$

5. MISMATCH EFFECTS ON CTOD EXPERIMENTAL EVALUATION

To assess the effectiveness of the determined $\eta-$factors on crack-tip driving forces experimental evaluation, the present section examines the effect of weld strength mismatch on CTOD measurements for deep ($a/W\cong 0.5$) crack clamped SE(T) specimens with center-cracked, square-grooved welds of different levels of mismatch. The primary objective is to gain further insight into the potential deviation that arises from evaluating CTOD in welded mismatched specimens using estimation formulas based on $\eta-$factors developed for homogeneous materials. The evaluation is conducted recasting Eq. (7) applying load-displacement curves obtained from the finite element models.

For two selected overmatched cases ($M_L=1.5$ and $M_L=2.0$), Fig. 6 compares (for each specimen) CTOD values obtained using $\eta-$factors for homogeneous materials ($M_L=1.0$ - referred to as $\delta_{Hom.}$) with CTOD values obtained using the $\eta-$factors for mismatched specimens (referred to as $\delta_{Mism.}$). The proposed $\eta-$factors used here were presented in Figure 5 and Eq. (11). Figure 6a presents errors in CTOD estimation for the 50% overmatched specimen ($M_L=1.5$), while Figure 6b presents errors in CTOD estimation for the 100% overmatched specimen ($M_L=2.0$). It can be seen that in both studied cases the effect of mismatch on crack driving forces is severe, with underestimations ranging from 23% to 37%. Even for 20% overmatched cases, the analyses conducted by the author presented underestimations up to 12%. Consequently, the proposed methodology for assessing crack driving forces in terms of CTOD should be considered in experimental evaluation of SE(T) welded specimens. It can lead to greater accuracy and safety in structural integrity evaluations, coupled with the possibility of increase in components lifetime. Similar evaluation was previously conducted by the author for $J-$integral results (a general trend of overerstimation in J values was identified) and can be found in Donato *et al.* (2009).

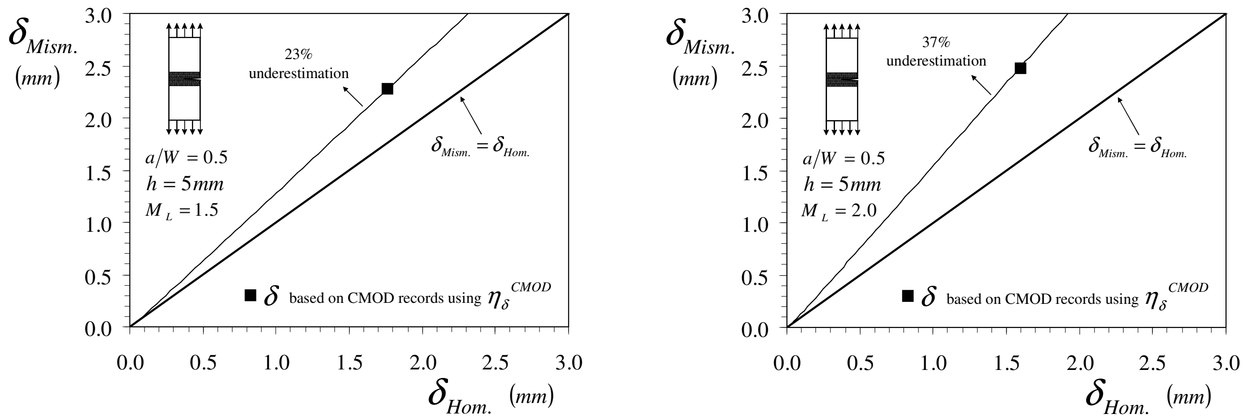


Figure 6 – Comparison of CTOD values evolution using $\eta-$factors developed for homogeneous specimens against CTOD values obtained using $\eta-$factors developed for mismatched specimens (a) for a 50% overmatched specimen and (b) for a 100% overmatched specimen.

6. CONCLUDING REMARKS

This work addresses the effect of weld strength mismatch on CTOD (δ) and J estimation formulas applicable to evaluate crack driving forces (δ and J), fracture toughness (δ_c and J_c) and crack growing curves ($\delta-R$ and $J-R$) from laboratory measurements of load-displacement data using clamped SE(T) specimens. Are considered center-cracked, square grooved tension specimens loaded by clamps. CTOD and J estimation is conducted based on the eta-methodology and appropriate $\eta-$factors are determined for different crack geometries and mismatch levels. The plane strain results reveal that $\eta-$factors for J estimation are strongly altered by $a/W-$ratios and mismatch levels, mainly for wider grooves, where the influence reaches up to 24% for the studied cases. However, low mismatch levels (between $\pm 20\%$) do not strongly alter determined $\eta-$factors for J estimation, with maximum deviation under 10%, which could allow (if errors are considered acceptable) homogeneous procedure application. For CTOD estimation, a different context emerges. $\eta-$factors present a weak dependence to a/W, but demonstrates a severe and systematic dependence to mismatch level, for all groove widths, with deviations up to 59% for the analyzed configurations. In addition, errors up to 37% in CTOD estimation were found if $\eta-$factors for homogeneous specimens are used instead of the proposed ones for mismatched specimens. Therefore, even for low levels of mismatch, the addressed methodology for CTOD estimation should be taken into account by the use of the proposed $\eta-$factors to guarantee higher levels of accuracy in structural integrity assessments. The present analyses, when taken together with previous studies, extend the body of results which serve to determine CTOD and $J-$integral using tension SE(T) specimens with varying geometries and mismatch levels.

7. ACKNOWLEDGMENTS

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