EFFECT OF WELD METAL MIS-MATCHING ON DEFECT ASSESSMENT PROCEDURES

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The present paper presents the results of a comparative study on the application of CTOD- and J-design curves as well as the Engineering Treatment Model for mis-matched joints (ETM-MM) procedures to strength mis-matched welds. Inhomogeneous through thickness center cracked tensile (CCT) panels containig 20% undermatched (UM) or 25% overmatched (OM) welds have been tested and local, remote and gauge length strain measured. Defect assessment procedures, developed to predict the failure of engineering structures, use as an input the mechanical properties of either base or weld metal only and are rather sensitive to the mechanical properties and applied strain definitions. Therefore, the significance of the strain definition to be used in those procedures on their predictions of the crack driving force has been discussed.

In contrast to the CTOD and J-design curves the recently developed ETM-MM for welded structures includes the mechanical properties of the weld and base metal. Its predictions of the CTOD (δ_5) have been compared with experimental results. The predictions of the CTOD and J-design curves are conservative only when the strain used is measured in the material with the lower yield strength (local strain for undermatching and remote strain for overmatching). However, very good crack driving force estimates were obtained by using the ETM-MM procedure for both over- and undermatched specimens.

INTRODUCTION

The failure behaviour of a welded structure associated with a defect will certainly be influenced by the differences of the strength levels of the weld metal and base metal. Therefore, the effect of the relative difference (mis-matching) of the yield strengths of the base metal, weld metal (and HAZ) parts on defect assessment procedures and on the toughness values (CTOD and J) of the structural weld joints must be determined. Fitness-for-purpose defect assessment procedures, such as Turner´s J-design curve [1] or PD6493 [2, 3], developed for homogeneous structural materials are based on fracture mechanics relationships between applied strain, required fracture toughness of the material and defect size. To establish such a relationship for strength mis-matched welded joints in the elastic-plastic case adequate definitions and measurements of the crack driving force, crack tip opening displacement (CTOD) or J-Integral and applied strain are necessary. In fact, the experimental way to determine the J-integral for homogeneous structures can not be extended in a straightforward manner to inhomogeneous specimens [4]. Also, direct application of these procedures to the strength mis-matched welded joints may inevitably cause over- or non-conservative predictions depending on the type of material properties data used in the analysis [5-8].

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Moreover, when considering mis-matched structures or laboratory specimens, various definitions of applied strain can be considered: the "*global strain*" from the measurement of the overall elongation of the specimen (or *gauge length strain*), the "*remote strain*" (strain in the base metal not influenced by the presence of a crack and of the weld metal) or a "*local strain*" measured in the weld metal [8 and 9]. The use of these different applied strain definitions will certainly cause different crack driving force predictions of the CTOD and J-design curve procedures [8]. In contrast to those design curves, the Engineering Treatment Model for Mis-Matched structures [10-15] takes into account the different mechanical properties of the base and weld metal (σ_{YB}, σ_{YW}, n_B, n_W) as well as various geometrical factors such as weld height (2H), its ratio to the uncracked ligament, 2H/(W-a) to estimate the crack tip opening displacement, CTOD (δ_5), of a strength mis-matched component. The first version of this analytical procedure[10, 11] which considered an infinite plate in tension with a transverse weld strip containing a through thickness crack was compared with experimental results [7]. Then, on the basis of finite element calculations conducted at GKSS, geometrical correction factors have been included into the ETM-MM model [12-14] and a systematic validation programme of this model using various experimental data sets already been conducted [15, 16].

The present paper presents some new results of this ETM-MM validation programme including the applicability of the present defect assessment procedures (CTOD and J-design curves) to CCT specimens with short through thickness cracks located in transverse butt-weld joints having 20-25% strength mis-match.

EXPERIMENTAL PROCEDURE

Two austenitic steels, namely AISI 316Lmod and X6CrNi 1811 were used to fabricate 10 mm thick electron beam (EB) welded bimaterial CCT panels as shown in Figure 1. The mechanical properties and engineering stress strain curves of both austenitic steels are given in Table 1 and Figure 2, respectively. The undermatched weld joint was simulated by EB welding the lower strength steel (X6CrNi) strip of 18 mm height (2H) on the higher strength 316Lmod steel. For overmatched tensile panels, a 316Lmod steel strip of the same height was used as a transverse weld metal with the X6CrNi steel as a base metal. By using these combinations, CCT specimens with mis-match ratios, M of 0.8 (20% undermatching) and 1.25 (25% overmatching) were obtained for under and overmatched cases respectively.

The experiments were carried out at room temperature with the CCT specimens having short through thickness fatigue cracks (a/W = 0.15, 2W=180 mm) located in the middle of the transverse strip which simulates the mis-matched weld metal. The experimental approach used in tensile panel tests was to measure the crack tip opening displacement, CTOD (δ_5) with GKSS made δ_5 clip-on gauges at the original fatigue crack tip over a gauge length of 5 mm, [17], the crack mouth opening displacement (CMOD), the crack propagation using the DC-potential drop method [18] and the overall elongation (gauge length of 270 mm) as a function of the applied load. Furthermore, the tested panels were instrumented with strain gauges in order to measure the local as well as the remote strains (e_L and e_R respectively). The position of the strain gauges are shown in Figure 3.

EVALUATION OF J-INTEGRAL

The experimental J-integral evaluation from the area under load-load line displacement curves can be significantly influenced by strength mis-match as reported in [4, 7]. In order to show any possible effect of remote (load line displacement) and local displacement (CMOD) measurements on the CCT panels for J determination procedures, the J-integral was evaluated both from the area under load-load line displacement curve (J-V_{LL}) [19, 20] and from the area under load-CMOD curve (J-CMOD) as proposed in [7], by the following formula:

J at crack initiation:
$$J_0 = \frac{K_0^2}{E} + \frac{U_0^*}{B(W - a_0)} \tag{1}$$

J is modified for crack growth in analogy to the procedure reported in [20]

$$J_i = J_{i-1} + \frac{2\Delta U^*}{B(b_{i-1} + b_i)}\left[K_i^2 b_i - K_{i-1}^2 b_{i-1}\right] \tag{2}$$

where i and i-1 indicate two consecutive points on the test record, for U_0^* and ΔU^* as shown in Figure 4, the other symbols are given in the notation. For J-V_{LL} values, U is the area under load-load line displacement curve as presented in Figure 4 while, for J-CMOD, U is calculated from the load-CMOD curves obtained during the testing of the CCT panels.

CTOD- and J - DESIGN CURVES

Defect assessment procedures such as CTOD and J-design curves principally require an applied strain parameter (as a ratio of applied strain to yield strain of the material in which the crack lies, e/e_Y) to establish a relationship with nondimensional CTOD or J. However, the determination of this parameter in defective mis-matched welds transverse to the loading direction presents considerable difficulty since the development of the strain level in the weld metal depends on the relative difference between yield strengths of the weld and base metals as well as on the defect and weld size. In this paper, different strain definitions with corresponding normalizations will be considered :

-The local strain (e_L) normalized by the weld metal yield strain, e_L/e_{YW}.
-The remote strain (e_R) normalized by the base metal yield strain, e_R/e_{YB}.
-The gauge length strain (GLS) obtained from the overall elongation (gauge length of 270 mm) normalized by the base metal yield strain, e/e_{YB}.

Turner`s J-Design Curve

The J-Design Curve procedure proposed by Turner [1] takes the form:

$$J/G_Y = (e/e_Y)^2 \quad \text{for} \quad e/e_Y \le 0.85 \tag{3}$$

$$J/G_Y = 5[(e/e_Y) - 0.7] \quad \text{for} \quad 0.85 \le e/e_Y \le 1.2 \tag{4}$$

$$J / G_Y = 2.5[(e / e_Y) - 0.2] \qquad \text{for} \qquad e / e_Y \geq 1.2 \tag{5}$$

where: $G_Y = \sigma_Y{}^2 \pi a f^2 / E$ and $e_Y = \sigma_Y / E$

for CCT specimens the crack size correction factor, f takes the form of [21];

$$f = f(a/W) = [1 - 0.025(a/W)^2 + 0.06(a/W)^4]\sqrt{1/\cos(\pi a/2W)} \tag{6}$$

CTOD Design Curve

In the earlier version [2] of the British Standards Institution (BSI) Document BSI PD 6493 (1980), the CTOD-design curve takes the form of a relationship between the nondimensional CTOD (Φ) and the ratio of applied strain to yield strain of the material in which the crack lies :

$$\Phi = \delta / 2\pi e_Y a = (e / e_Y)^2 \qquad \text{for} \qquad e / e_Y \leq 0.5 \tag{7}$$

$$\Phi = \delta / 2\pi e_Y a = (e / e_Y) - 0.25 \qquad \text{for} \qquad e / e_Y \geq 0.5 \tag{8}$$

However, the revised version of BSI PD 6493 (1991) [3] is intended to be consistent with the earlier 1980 version of the document in the Level 1 treatment [22]. The Level 1 treatment now gives the CTOD formulation in terms of applied stress. However, in this paper the earlier version of the design curve was used since strain is a much more sensitive parameter than stress to describe the behaviour of the mis-matched transverse welds.

The normalization of the applied strain was for both Design Curves obtained using either the material properties of the weld metal, $\sigma_Y = \sigma_{YW}$ when using the local strain measurements (e_L) or those of the base plate, $\sigma_Y = \sigma_{YB}$ when considering the remote strain (e_R) or the gauge length strain (GLS). In the design curve considerations, $R_{P0.2}$ is considered as the yield strength (σ_Y) of one material.

BRIEF OUTLINE OF THE ETM-MM PROCEDURE

Recently, the early versions [10, 11] of the ETM-MM descriptions have been modified using finite element calculations and slip line field considerations in order to take into account the effects of the geometrical parameters such as weld heigth (2H), H/a, 2H/(W-a) etc. in the fracture process of the mis-matched specimens. Thus, the crack driving force CTOD(δ_5) has been expressed as a function of the applied load [12-14]. Some of the comparisons of those ETM-MM expressions with the experimental results have already been reported in [14-16]. The ETM-MM formulations expressing the CTOD (δ_5) in the weld metal as a function of the applied strain have also been compared [7] with experimental results. However, in this paper, the ETM-MM expressions proposed in [12-14] have been derived using the method presented in [10 and 11] to obtain the crack driving force CTOD (δ_5) in the weld metal as a function of remote strain and as usual including the material properties of the base/weld metals and the geometrical parameters of the specimens such as crack size (2a), width of the panel (2W) and heigth of the weld metal (2H). The use of remote strain for derivation of the ETM-MM formulations is much more practical than expressing the model in terms of either local or GLS parameters since both of them would not be available for structural components.

The ETM-MM expressions in terms of the above mentioned quantities presented below are however valid for strength mis-matched CCT specimens loaded in tension with a crack located in the middle of the weld (parallel to the weld/base metal interface). The assumptions having been made for such specimens are:

- plane stress,
- the plastic zone develops at the crack tip, crosses the weld/base metal interface and fully penetrates into the base metal.

Nevertheless, for other conditions (plane strain, plastic zone remaining within the weld metal, etc.), identical expressions can also be obtained by applying the derivations given in [10, 11] to the equations presented in [12-14]. Contrary to the all other defect assessment procedures, the ETM-MM provides two different sets of expressions for over- and undermatched welds.

Overmatching case

Stage 1 : $F \le F_{YMM}$

$$\delta_W = \frac{\pi a f^2 \sigma_{YB}}{EM}\left(\frac{e_R}{e_{YB}}\right)^2\left[1+\frac{f^2}{2M^2}\left(1+(M-1)\frac{a}{H}\right)\left(\frac{e_R}{e_{YB}}\right)^2\right] \qquad (9)$$

Stage 2 : $F_{YMM} \le F \le F_{YB}$

$$\delta_W = \frac{\pi a f^2 \sigma_{YB}}{EM}\left(\frac{F_{YMM}}{F_{YB}}\right)^{2-\frac{1}{n_{MM}}}\left[1+\frac{f^2}{2M^2}\left(1+(M-1)\frac{a}{H}\right)\left(\frac{F_{YMM}}{F_{YB}}\right)^2\right]\left(\frac{e_R}{e_{YB}}\right)^{\frac{1}{n_{MM}}} \qquad (10)$$

Stage 3 : $F_{YB} \le F$

$$\delta_W = \frac{\pi a f^2 \sigma_{YB}}{EM}\left(\frac{F_{YMM}}{F_{YB}}\right)^{2-\frac{1}{n_{MM}}}\left[1+\frac{f^2}{2M^2}\left(1+(M-1)\frac{a}{H}\right)\left(\frac{F_{YMM}}{F_{YB}}\right)^2\right]\left(\frac{e_R}{e_{YB}}\right)^{\frac{n_B}{n_{MM}}} \qquad (11)$$

where :

$$f = f(a/W) = \left[1-0.025(a/W)^2+0.06(a/W)^4\right]\sqrt{1/\cos(\pi a/2W)}$$

Limit load expression for mis-matched CCT panels;

$$F_{YMM} = 2B((W - a - 1.54H)\sigma_{YB} + 1.54H\sigma_{YW}) \tag{12}$$

$$F_{YB} = 2BW\sigma_{YB} \tag{13}$$

and strain hardening exponent of mis-matched CCT panels again influenced by the strength mis-match and relative size of the weld metal compared to the uncracked ligament is given as;

$$n_{MM} = ((W - a - 1.54H)n_B + 1.54Hn_W)/(W - a) \tag{14}$$

Undermatching case

Stage 1 : $F \le F_{YW} \le F_{YMM} \le F_{YB}$

$$\delta_W = \frac{\pi a f^2 \sigma_{YB}}{EM}\left(\frac{e_R}{e_{YB}}\right)^2\left[1 + \frac{f^2}{2M^2}\left(\frac{e_R}{e_{YB}}\right)^2\right] \tag{15}$$

Stage 2 : $F_{YW} \le F \le F_{YMM} \le F_{YB}$

$$\delta_W = \frac{\pi a f^2 \sigma_{YB}}{EM}\left(M\left(1 - \frac{a}{W}\right)\right)^{\frac{2(n_W - 1)}{n_W + 1}}\left[1 + \frac{f^2}{2}\left(1 - \frac{a}{W}\right)^2\right]\left(\frac{e_R}{e_{YB}}\right)^{\frac{4}{n_W + 1}} \tag{16}$$

Stage 3 : $F_{YW} \le F_{YMM} \le F \le F_{YB}$

$$\delta_W = \frac{\pi a f^2 \sigma_{YB}}{EM}\left(M\left(1 - \frac{a}{W}\right)\right)^{\frac{2(n_W - 1)}{n_W + 1}}\left[1 + \frac{f^2}{2}\left(1 - \frac{a}{W}\right)^2\right]\left(\frac{F_{YMM}}{F_{YB}}\right)^{\frac{4}{n_W + 1} - \frac{1}{n_{MM}}}\left(\frac{e_R}{e_{YB}}\right)^{\frac{1}{n_{MM}}} \tag{17}$$

Stage 4 : $F_{YW} \le F_{YMM} \le F_{YB} \le F$

$$\delta_W = \frac{\pi a f^2 \sigma_{YB}}{EM}\left(M\left(1 - \frac{a}{W}\right)\right)^{\frac{2(n_W - 1)}{n_W + 1}}\left[1 + \frac{f^2}{2}\left(1 - \frac{a}{W}\right)^2\right]\left(\frac{F_{YMM}}{F_{YB}}\right)^{\frac{4}{n_W + 1} - \frac{1}{n_{MM}}}\left(\frac{e_R}{e_{YB}}\right)^{\frac{n_B}{n_{MM}}} \tag{18}$$

where; $F_{YW} = 2B(W - a)\sigma_{YW}$

In these expressions, it is assumed that $F_{YW} \le F_{YMM} \le F_{YB}$ for undermatching and $F_{YMM} \le F_{YB}$ for overmatching.

As it is the case for other defect assessment procedures, a prediction of the ETM-MM procedure is rather sensitive to the method by which the stress-strain curve of the material is being approximated. The Engineering Treatment Model for homogeneous specimens [23], requires that the strain hardening exponent, n should be determined from the engineering stress-strain curve of the material of interest. However, some sensitivity studies are in progresss at GKSS to determine the effect of the use of true stress-strain curves used in the ETM-procedure. As a part of the validation programme, the experimental results are compared with the ETM-MM estimates using the values of yield stress and strain hardening exponent obtained from the engineering stress-strain curves (σ_Y^e, n^e) and from the true stress strain curve (σ_Y^t, n^t). The approximations of the stress strain curves are made as shown schematically in Figure 5 and the respective values are given in Table 2.

RESULTS AND DISCUSSIONS

Two J values (J-V_{LL} and J-CMOD) determined for over- and undermatched panels were plotted against locally measured CTOD (δ_5) values and shown in Figure 6. As generally known, the J-integral should be proportional to the CTOD for any given specimen geometry. Fig. 6 clearly shows that the J-V_{LL} values do not provide single relationship for over- and undermatched specimens due to the global nature of the measurement. Therefore, the J-V_{LL} values obtained from the area under load-load line displacement curve of the overmatched (OM) specimen overestimate the real toughness of the specimen since V_{LL} measurements include plastic deformation of the remote base metal part due to the weld strip overmatching and small crack size (a/W=0.15). The higher strength weld metal part of the OM specimen in fact protects the crack from applied strain and hence forces the base metal to accomodate substantial part of the applied strain which causes larger V_{LL} and hence higher J than the UM specimen for a given CTOD (δ_5). On the other hand, the J calculated from the area under load-CMOD curve provides linear and single relationship with the CTOD (δ_5) for both UM and OM cases since the CMOD measurements do not include the remote deformation of the base metal, Fig. 6. This linear relationship between CTOD(δ_5) and J-CMOD implies that these J-CMOD values obtained have the same validity as those for the locally measured CTOD (δ_5). Therefore, J-CMOD values can be considered as a real measure of toughness and they were used here as a crack driving force in the J-design curve procedure. The value of the constraint factor m (J-CMOD = m $Rp_{0.2}\,\delta_5$, if we take the yield strength of $Rp_{0.2}$= 240MPa) for the UM and OM specimens is 1.8.

J- and CTOD-Design Curves

The influence of the applied strain definition on the J- and CTOD-design curve predictions is shown in Figures 7 and 8 respectively. In these plots, the local strain (e_L) is normalized by the weld metal yield strain (e_{YW}) while the remote strain (e_R) and gauge length strain (GLS) are normalized by the base metal yield strain (e_{YB}). This way of normalization seems to be logical since two of the applied strain quantities, e_R and e_L are measured at the base and weld metal parts respectively. For overmatched specimen the remote strain (e_R) is higher than the local strain (e_L) for a given J as shown in Fig. 7a. The contrary can be observed for the undermatching case, Fig. 7b. In fact, higher applied strain was always measured in the material which has the lower yield strength.

For the overmatching (OM) case shown in Fig. 7a, the J-design curve provides non conservative (or unsafe) predictions of the crack driving force (J) by using the local strain, e_L, measurement normalized with the weld metal yield strain. But, when using the gauge length strain, GLS or remote strain e_R measurement normalized by the base metal yield strain, the J-design curve provides reasonably conservative J predictions. Comparison of e_R and GLS strain curves (which collapse into almost a single curve) suggests that the applied strain can equally be described using either GLS or e_R since the base metal part dominates the deformation of the OM welded panel. For the undermatching (UM) case, Fig. 7b, the use of local strain measurement gives conservative prediction of the crack driving force up to an applied strain (e) four times the yield strain of the weld metal (e_{YW}). In contrast, the remote strain provides gross under-prediction of J beyond elastic regime. With the use of GLS, J-design curve gives a reasonably good J estimate for applied normalized strain up to about 1.5, then becomes unsafe with further plastic deformation.

In order to obtain conservative estimates of the crack driving force by using the J-design curve, the applied strain should be measured in the material with the lower yield strength (base metal for overmatching and weld metal for undermatching) and should be normalized by the corresponding material properties. Similar conclusions have also been reached by Milne and Ainsworth et al [24] with respect to the R6-method. The J-design curve analysis was carried out by Petrovski and Koçak [8] for a 26% undermatched tensile panel with a surface crack in the x-groove weld metal and again similar conclusions were drawn.

Effect of the applied strain definition on the CTOD-design curve predictions is presented in Figure 8. For overmatched panels (OM), Fig. 8a, the use of remote and gauge length strains yields conservative predictions of the crack driving force (CTOD). However, the use of the local strain measurement leads to a over non conservative estimate after a normalized applied strain of about 0.8 (Fig. 8a). Contrary to the OM specimen, in the undermatched case (UM) the crack driving force is highly underestimated by the CTOD-design curve when using the remote deformation or the gauge length strain. Again, use of local strain provides conservative estimate for applied normalized strain up to about 4, then becoming unsafe with further deformation as shown in Fig. 8b. Comparison of both design curve predictions, Figs. 7 and 8 suggests that both procedures react in a very similar fashion to the variation of applied strain parameter for UM and OM cases. Both design curves seriously underestimate the crack driving force, in the UM configuration using practice relevant remote strain parameter. 25% overmatching shields the weld metal region from applied strain causing a moderate increase of CTOD or J; in 20% undermatched welds, applied strain concentrates in the weld region and hence causes a rapid increase of the CTOD or J (compare a and b of Figures 7 and 8 for GLS or e_R).

Finally, for both CTOD and J-design curve applications, the use of applied strain measured in the material with lower yield strength (and normalized by the corresponding yield strain property of the material) generally provides conservative estimates of the J and CTOD for the tension loaded specimens.

However, this observation particularly requires an information on the level of applied strain in the UM weld metal to be used in these defect assessment procedures. However, the local strain value would not easily be available for cracked structural components. None of the design curves can provide reasonably good and conservative predictions for both under- and overmatched cases using single applied strain parameter such as remote strain. The use of a single design curve to provide safe predictions for defective under- and overmatched welded structures is obviously inadequate.

ETM-MM PROCEDURE

The correlation between experimental results and ETM-MM predictions for the UM and OM panels are shown in Figure 9. The ETM-MM estimates were carried out using the yield stress σ_Y, and strain hardening exponent n values obtained either from approximating the engineering stress-strain curve (σ_Y^e, n^e) or the true stress-strain curve (σ_Y^t, n^t) of the materials. This excercise was conducted to determine the effect of these input data on the ETM-MM predictions and the two estimates are presented in Fig. 9a and 9b. As mentioned earlier, the ETM-MM procedure does provide two separate CTOD (δ_5) predictions for the UM and OM cases (contrary to the CTOD and J-design curves) and hence both predictions are plotted in Fig. 9. Furthermore, in these diagrams applied strain was defined in terms of remote strain e_R since all derivation of the ETM-MM expressions have used the remote strain as shown earlier in the formulations.

Using the σ_Y^e and n^e data obtained from the engineering stress-strain curve provides predictions with high accuracy for the 25% OM specimen, Fig. 9a. The CTOD values are slightly underestimated by the ETM-MM curve with the use of σ_Y^t and n^t values, Fig. 9a. However, the use of σ_Y^e and n^e provides over-conservative crack driving force CTOD (δ_5) for the 20% UM case, Fig. 9b. Using the σ_Y^t and n^t values obtained from the approximation of the true stress-strain curve improves the prediction of CTOD (δ_5) for UM panel drastically, Fig. 9b. To attempt to eliminate the over-conservatism of engineering stress strain curve data particularly for UM case, the use of true stress-strain curve provides satisfactory predictions for the UM panel but for the OM case it gives non-conservative prediction based on the remote strain.

CONCLUSIONS

This paper has addressed the issues of the application of CTOD and Turner's J-design curves and ETM-MM procedures to predict the crack driving force on under- and overmatched welds with through thickness center cracked bi-material austenitic steel panels loaded in tension. Although more experimental results are required to fully assess the applicability of both design curves to mis-matched joints and make firmer recommendations, the following conclusions can be drawn from this study:

- The experimental determination of the J-integral from load-CMOD curve and the local measurement of the CTOD by the δ_5-clip seems to well estimate the real crack driving force of 25% over or 20% undermatched welded tensile panels with short through thickness cracks.
- J- and CTOD-design curves provide conservative prediction of the crack driving force of an overmatched specimen when using the remote strain or gauge length strain values normalized by the base metal yield strain. Both design curves seriously underestimate crack driving force in the 20% UM configuration using practice relevant remote strain parameter. For UM specimens, the local strain measurement normalized by the weld metal yield strain provides conservative prediction.
- For both design curve applications, the use of applied strain measured in the material with lower yield strength (and normalized by the corresponding yield strain) generally provides conservative estimates of the J and CTOD for the tension loaded specimens.
- In general, the ETM-MM procedure provides very good crack driving force CTOD (δ_5) predictions for both under- and overmatched welds in terms of remote strain.
- Using the $\sigma_Y{}^e$ and n^e data obtained from the engineering stress-strain curve provides over-conservative crack driving force CTOD (δ_5) for the 20% undermatching case. However, it provides prediction with high accuracy for the 25% overmatching specimen. Using the $\sigma_Y{}^t$ and n^t values obtained from the approximation of the true stress-strain curve certainly improves the prediction of CTOD (δ_5) for UM panel drastically. For the overmatched case, the CTOD values are slightly underestimated by the ETM-MM curve with the use of $\sigma_Y{}^t$ and n^t values.

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SYMBOLS USED

a	half crack length
a_0	half crack length before initiation of crack growth
b	uncracked ligament length: b=W-a
B	specimen thickness
e	applied strain
e_R	remote strain measured at the base metal
e_L	local strain measured at the weld metal
e_Y	yield strain
e_{YB}	base metal yield strain
e_{YW}	weld metal yield strain

E	Young´s modulus
F	applied load
F_{YB}	yield load of the homogeneous base metal specimen without crack
F_{YW}	yield load of the homogeneous weld metal specimen with crack
F_{YMM}	yield load of the mis-matched specimen obtained by slip-line field considerations
GLS	gauge length strain
J_0	J before initiation of crack growth
J-CMOD	experimentally estimated J from the area under load-CMOD curves
J-V_{LL}	experimentally estimated J from the area under load-load line displacement (VLL) curves
K	stress intensity factor
K_0	stress intensity factor before initiation of crack growth
M	strength mis-match ratio, $M = \sigma_{YW} / \sigma_{YB}$
n_B	strain hardening exponent of the base metal
n_W	strain hardening exponent of the weld metal
n_{MM}	strain hardening exponent of the mis-matched specimen
n^e	strain hardening exponent obtained by the approximation of the engineering stress-strain curve
n^t	strain hardening exponent obtained by the approximation of the true stress-strain curve
U	deformation energy
U_0^*	deformation energy before initiation of crack growth
V_{LL}	load line displacement
W	half width of the CCT specimen; 2W=180 mm.
δ_W	CTOD in the weld metal
δ_5	CTOD measured with GKSS made δ_5 clip-on gauges at the original fatigue crack tip over a gauge length of 5 mm
ΔU^*	increment of deformation energy
Φ	non dimensional CTOD
σ_Y	yield strength
σ_{YB}	base metal yield strength
σ_{YW}	weld metal yield strength
$\sigma_Y{}^e$	yield strength obtained by the approximation of the engineering stress-strain curve
$\sigma_Y{}^t$	yield strength obtained by the approximation of the true stress-strain curve

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Table 1 : Mechanical properties of the two austenitic steels

Material	E (MPa)	$R_{P0.2}$ (MPa)	$e_{0.2}$ (%)	R_m^e (MPa)	e_{max}^e (%)	R_m^t (MPa)	e_{max}^t (%)
AISI 316Lmod	195000	300	0.35	610	55.8	950	44.4
X6CrNi 1811	195000	240	0.32	617	60.1	988	47.1

Table 2 : Material properties used in the ETM-MM procedure

Material	E (MPa)	σ_o^t (MPa)	n^t	σ_o^e (MPa)	n^e
AISI 316Lmod	195000	225.6	0.24	256.3	0.14
X6CrNi 1811	195000	162.4	0.285	192.6	0.181

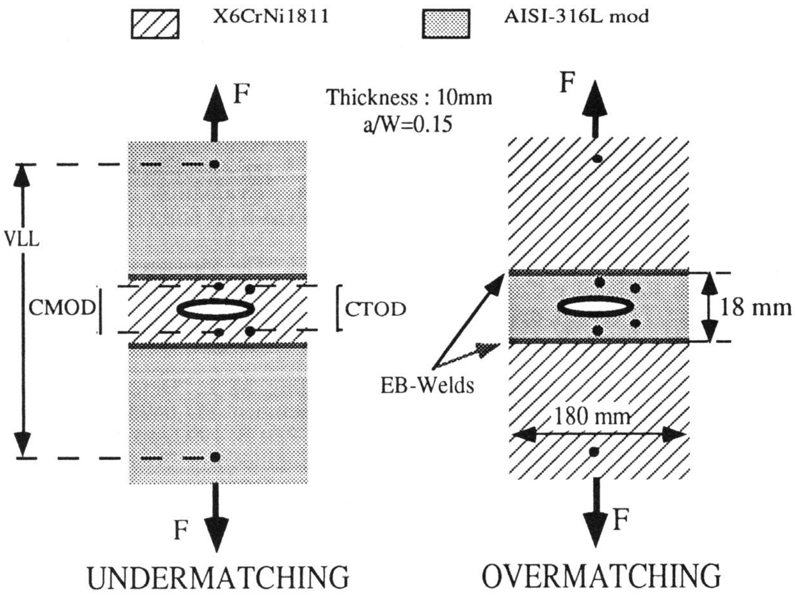


Figure 1 : Schematic is showing the bimaterial CCT specimens having transverse weld metals simulated by electron beam welded strips of 2H=18 mm height. The CTOD was measured at the fatigue crack tip over a gauge length of 5 mm with the δ5 clip-on gauges.

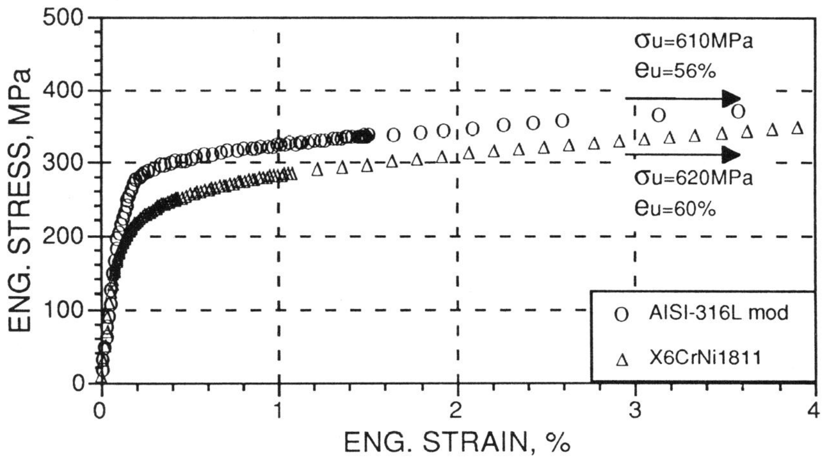


Figure 2 : Engineering stress-strain curves of the two austenitic steels

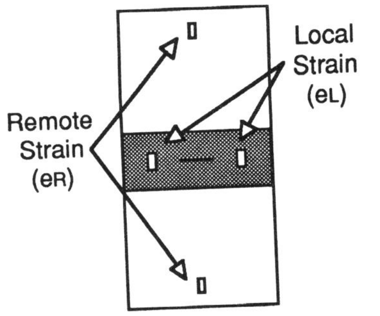


Figure 3 : Schematic showing the positions of the strain gauges for remote and local strain measurements on the CCT specimen. **Note:** e_L positions were far away from the crack tips.

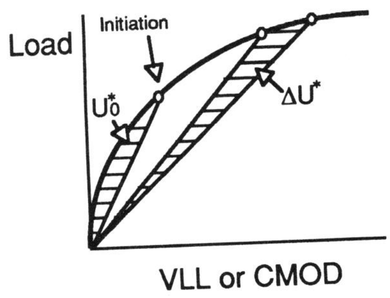
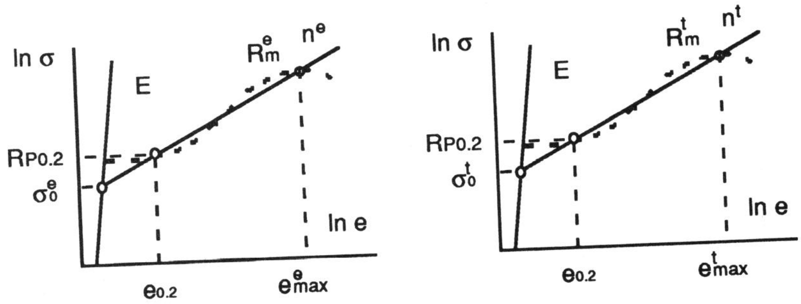


Figure 4 : Definition of the areas used to calculate J-VLL and J-CMOD values.



a)

b)

Figure 5 : Schematic showing the method used for approximating the stress-strain curves for the Engineering Treatment Model, a) Engineering stress-strain curve, b) True stress-strain curve.

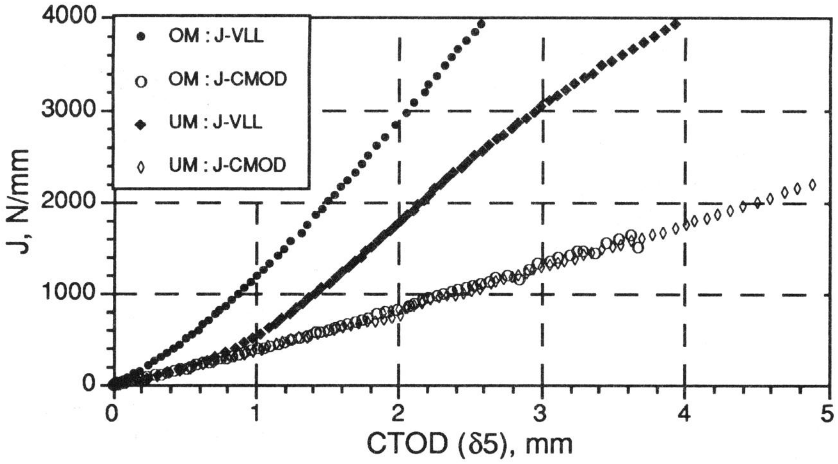
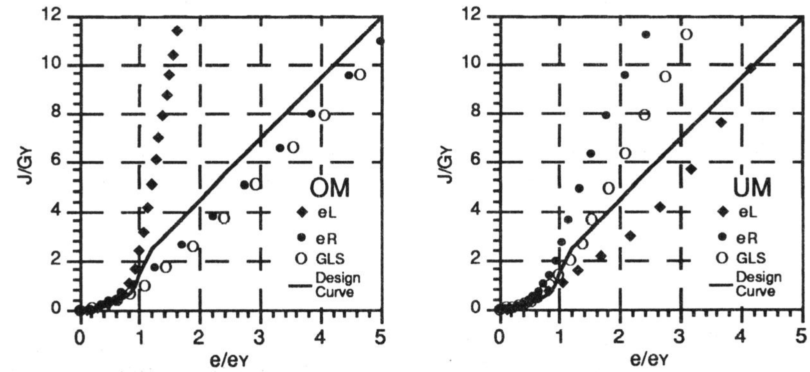
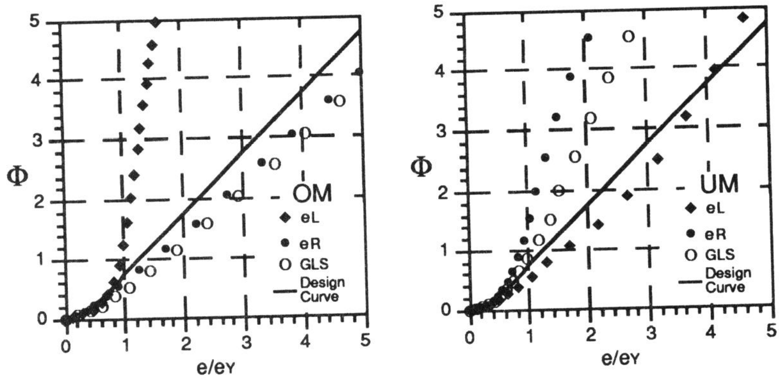


Figure 6 : Relationships between J and CTOD (δ_5) values for the over- and undermatched panels, showing a linear correlation between J-CMOD and CTOD (δ_5) values for both UM and OM panels (J-CMOD = mRp$_{0.2}$ CTOD (δ_5), m=1.8 for Rp$_{0.2}$ = 240 MPa). **Note** : J-VLL is calculated from the area under load-load line displacement curves and J-CMOD from the area under load CMOD curves.



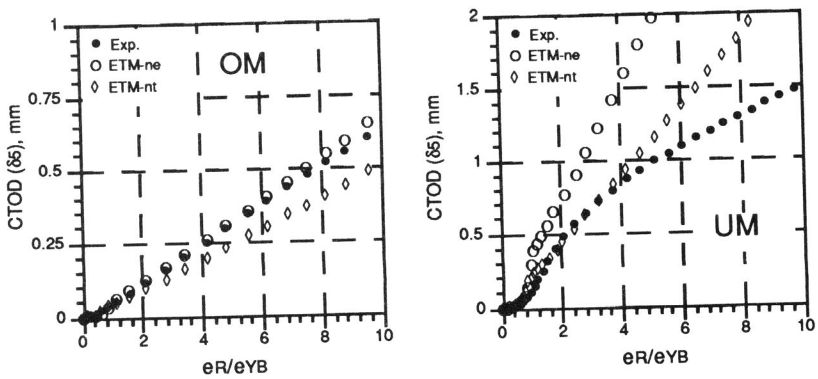
a) b)

Figure 7 : Comparison between Turner´s J-Design curve and experimental results of the OM and UM panels with three applied strain definitions for;
a) 25% overmatching panel b) 20% undermatching panel.



a) b)

Figure 8 : Comparison between the CTOD Design curve and experimental results of the OM and UM panels with three applied strain definitions in terms of non-dimensional CTOD (Φ) vs. normalized applied strain for:
a) 25% overmatching panel b) 20% undermatching panel.



a) b)

Figure 9 : Comparison between the ETM-MM predictions and experimental results of the OM and UM panels in terms of CTOD ($\delta 5$) vs. normalized remote strain for: a) 25% overmatching panel b) 20% undermatching panel. The ETM-MM predictions were made by using both input data sets obtained from engineering stress-strain (σ_o^e, n^e) and true stress-strain curves (σ_o^t, n^t).