CHAPTER 8

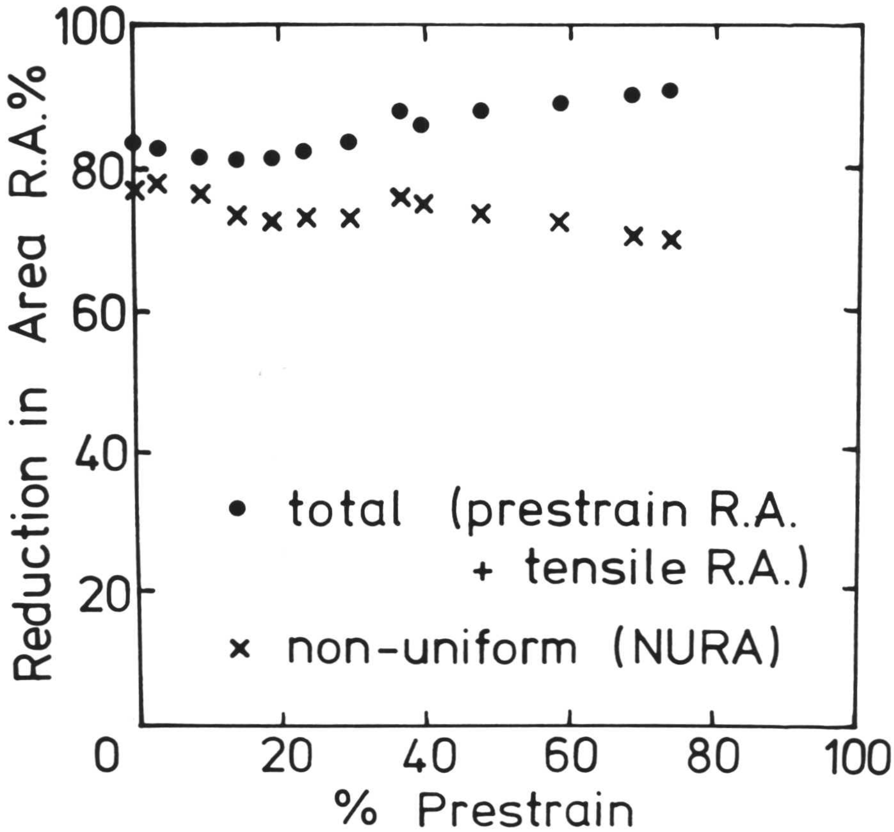
WELD METAL DUCTILITY: REDUCTION IN AREA

8.1 INTRODUCTION

It follows from the Chapter 7 that, except for very short gauge lengths, percent elongation is mainly influenced by uniform elongation, and is thus dependent upon the strain-hardening capacity of the matrix. In contrast, reduction in area is more a measure of the deformation required to produce fracture, and its chief contribution arises from the necking process. The work of Groom (1971) illustrates this. Groom studied the effects of various nominal prestrains on the behaviour of copper and steel tensile tests. Rolling and swageing were used to form embryonic voids. It was found that for all pre-strains, the reduction in area associated with necking alone was essentially constant (Fig. 8.1), showing that for a given inclusion spacing there is a critical level of triaxial tension that must be reached before fracture can occur by void coalescence and the "cup" stage of fracture can propagate. (The slight decrease at large prestrains can be attributed to the inclusion spacing being significantly reduced by large amounts of cold work, since it is the inclusion spacing in the plane through the minimum section of the neck that has to be considered). It is, therefore, correct to consider the uniform and non-uniform components of ductility, and their effects on elongation and reduction in area separately. In summary, therefore, the magnitude of ϵ_u, the uniform elongation, is a function of the strain hardening capacity of the material. In contrast, non-uniform elongation is expected to depend on the concentration and distribution of the stress-concentrating particles which influence deformation behaviour after necking.

8.2 STRESS INTENSIFICATION

The initial stress in the usual tensile test is uniaxial, so that load divided by reduced area gives the true stress. However, this procedure underestimates the peak stress after necking, since the necking process has triaxiality associated with it, as other components of stress are introduced (Dieter, 1968). The stress distribution in the neck of a tensile specimen consists of an axial tension which is uniform across the neck (σ_t), plus a hydrostatic tension (σ_H), which is zero on the periphery and



Figures 8.1: The variation of total and non-uniform reduction in area with prestrain for mild steel. (After Groom, J. D. G. (1971), Ph.D. thesis, University of Cambridge, U.K., Chapter 1).

increases to a maximum on the axis. The effect of this variation of tension across the specimen is to make the mean stress (σ_m) higher than the true flow stress (σ_t). The correction factor was found by Bridgeman (1952) to be

$$\frac{\sigma_m}{\sigma_t} = \left(1 + \frac{2R}{a}\right) \ln \left(1 + \frac{a}{2R}\right) \tag{8.1}$$

where a is minimum radius of the neck cross-section,

and R is the radius of the neck profile (see Fig. 8.2).

This expression was derived using an approximate solution, based on a geometrical analysis of neck formation, and gives the best approximate procedure for obtaining the distribution of stresses and strains during ductile failure. Triaxiality will have a strong effect upon the rate of growth of a spherical void, and consequently upon the strain of fracture at a notch. Once the inclusion/matrix interface bond is broken, which is expected to occur early during testing, the superimposed triaxial stresses promote rapid void coalescence and enhance the 'cup' stage of fracture.

8.3 THE NECKING PROCESS AND REDUCTION IN AREA

During non-uniform elongation it is only the neck region that lengthens substantially. For this deformation process the weld metal is considered microstructurally homogeneous, insofar as the phases by now all have similar strengths (see above), and necking is attributed to variations in inclusion population in the specimen. It has already been discussed in Volume 1 that the microstructure of a weld metal is inhomogeneous. Essentially three main phases are present - allotriomorphic ferrite formed at the austenite grain boundaries, Widmanstätten ferrite, and acicular ferrite consisting of a non-parallel array of bainite plates. However, Tweed and Knott (1987) have shown by microhardness and Tolansky interferometry techniques that allotriomorphic ferrite, which is expected to be the softest of the three phases (Sugden and Bhadeshia, 1988), has preference of flow for the first 5-10% strain. Therefore, at higher strains, resistance to flow seems to be much the same in all three phases.

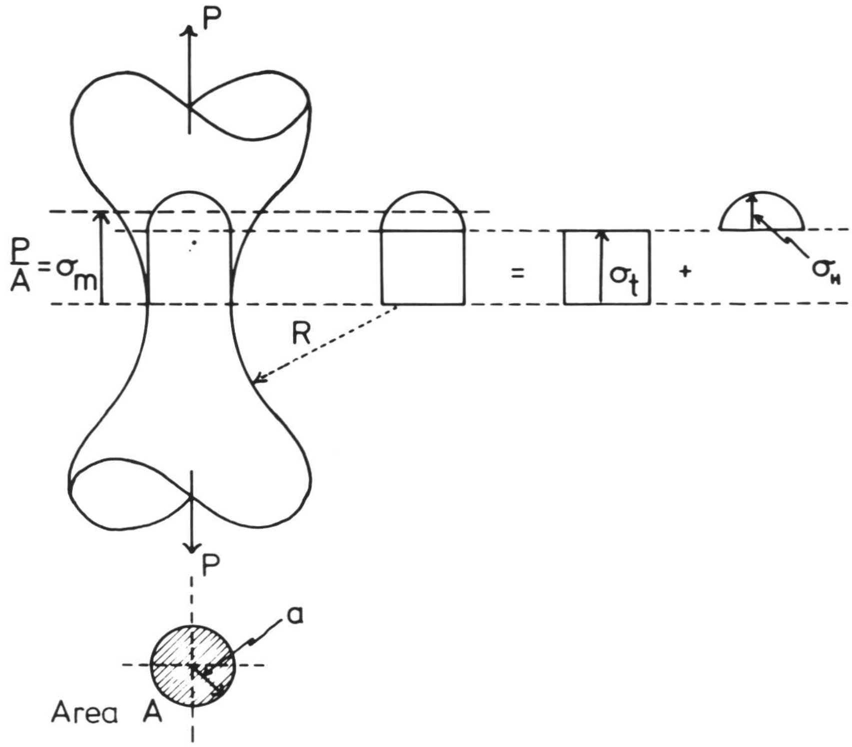


Figure 8.2: Triaxial stresses associated with tensile testing. P and A are the applied load and cross-sectional area of the specimen respectively. (After Marshall, E. R., and Shaw, M. C. (1952), *Trans. A.S.M.*, **44**, 705-720).

The sequence of events that leads to ductile failure in a tensile test specimen can be summarised as follows (Rogers, 1960; Melander, 1981):

(i) voids form after a critical amount of strain;

(ii) at the maximum in the applied load, necking begins with further straining;

(iii) at a critical volume fraction of voids, or at a critical mean free path between voids, strain concentrates in narrow bands connecting the voids;

(iv) separation occurs along these bands.

Figure 8.3 shows the typical inclusion distribution in a low-alloy steel weld deposit.

In this part of the failure process, it is the void geometry that is most important. The overall strain, therefore, will strongly depend upon the inclusion spacing, failure being determined by a geometry criterion dependent upon the spacing of the void nucleating inclusions at the UTS.

The proportion of particles that form dimples (*i.e.* take part in the fracture process) in weld metals has sometimes been reported to be higher than the apparent inclusion density observed on a planar specimen (Tuliani *et al.*, 1969; Siewert and McCowan, 1987), and Widgery and Knott (1978; 1980) interpreted this to mean that as internal necking occurs, inclusions beyond the plane of the specimen are drawn into the fracture of the specimen (Figure 8.4), such that the ratio of the depth from which a given inclusion is sampled, z, to the diameter of the inclusion, x, is constant. However, it should be mentioned that this is not a totally general result, and other particle-containing alloy systems exhibit different behaviours (Dodd and Bai, 1987, p.96).

An early investigation into the influence of inclusions on ductility was done by Edelson and Baldwin (1962) using copper-base alloys. Within experimental error, alloy ductility was found to be dependent upon the percent volume fraction of the inclusions present alone for V_f varying between 0 and 20% (see Figure 8.5). Similarly, Le Roy *et al.* (1981) found the strain to failure of spheroidized steels to decrease with increasing cementite volume fraction (Figure 8.6), and similar data are given in Pickering (1978). However, this relationship was tested for weld metals by Widgery (1976), and he found poor agreement. He considered that the yield

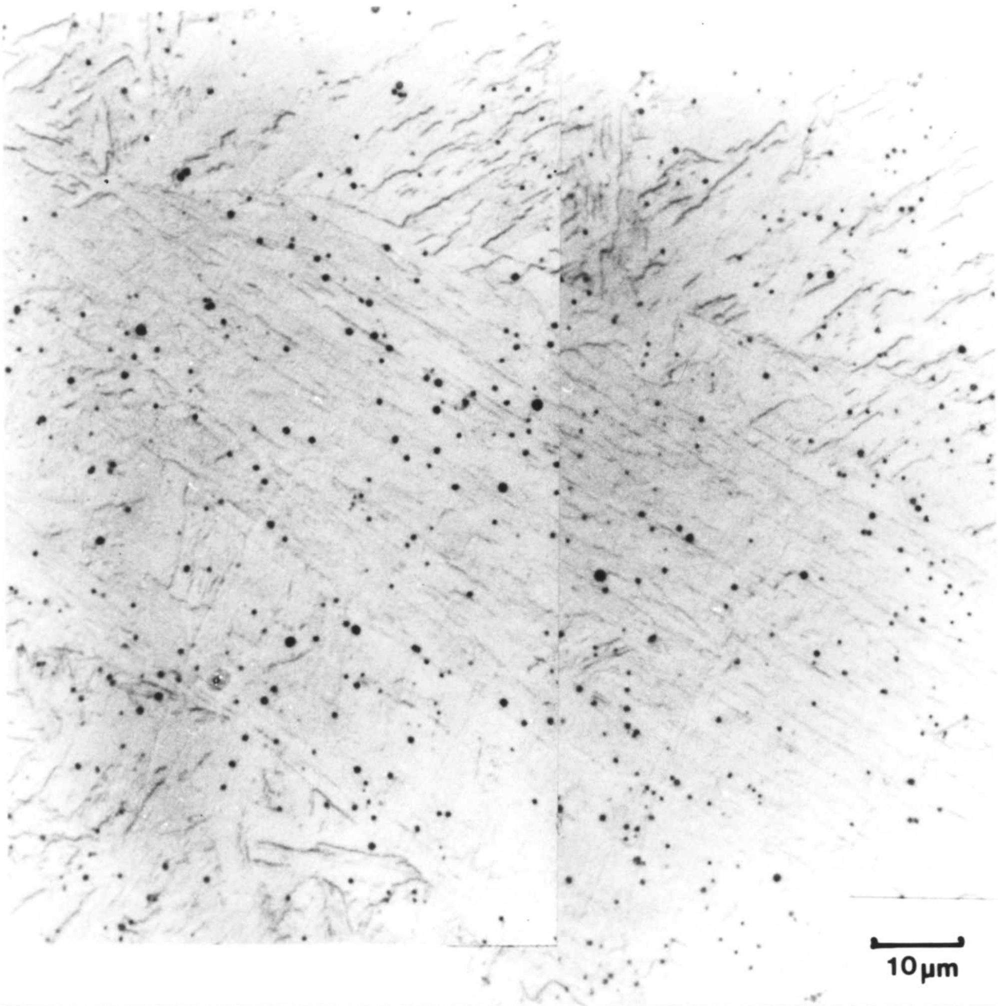


Figure 8.3: Low-carbon 2% nickel steel bead–on–plate weld deposit etched to reveal inclusions. Etchant: Acidified alcoholic $CuCl_2$.

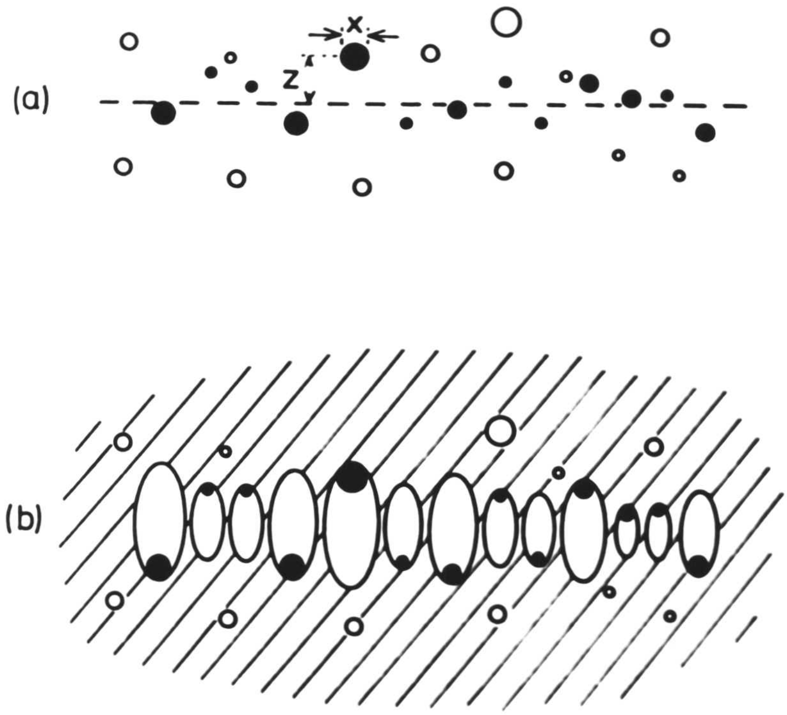


Figure 8.4: Microvoid formation during ductile failure: only certain inclusions (solid circles in (a)) take part in the development of fracture (b). (After Widgery, D. J. and Knott, J. F. (1978), *Met. Sci.*, **12**, (1), 9).

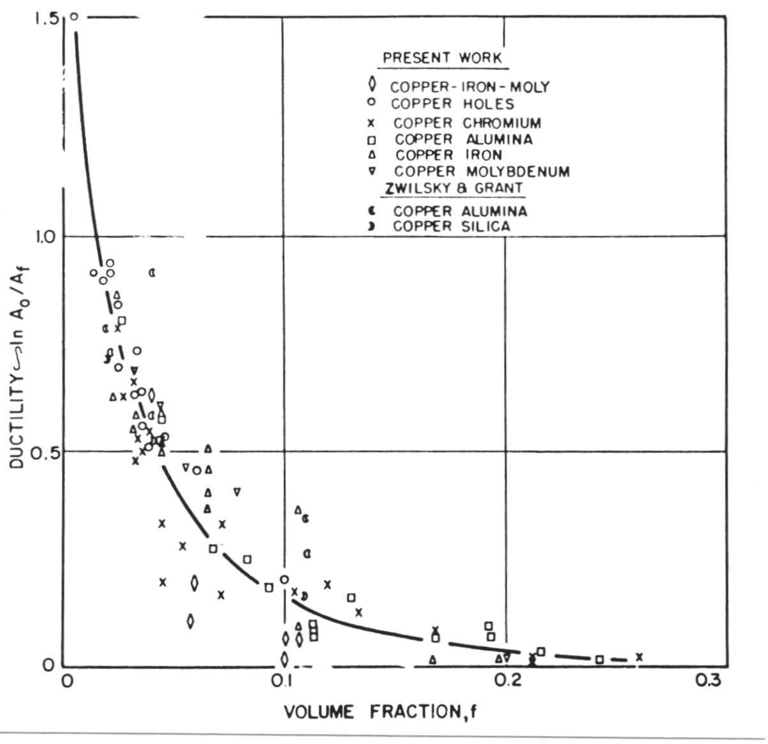


Figure 8.5: Combined plot of ductility of several copper dispersion alloys versus volume fraction. (After Edelson, B. I. and Baldwin, Jr., W. M. (1962), *Trans ASM*, 55, 230-250).

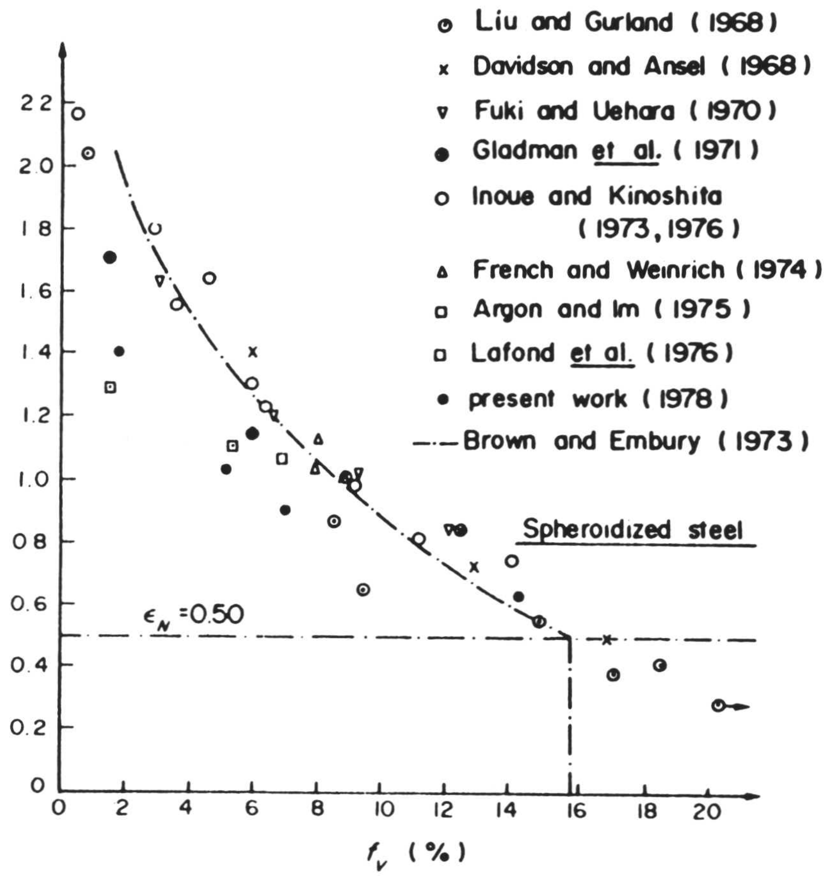


Figure 8.6: Strain to fracture in spheroidized carbon steels as a function of the percent volume fraction of cementite particles (f_v). (ϵ_n is a calculated void nucleation strain). The references on the diagram are given in the original reference. (After Le Roy, G., Embury, J. D., Edwards, G., and Ashby, M. F. (1981), *Acta Metall.*, **29**, 1509-1522).

strength and work-hardening characteristics of the matrix must also be important, and should be taken into account.

In order to see if weld metal ductility could be explained in terms of the volume fractions of inclusions present, data for reduction in area, q, and wt% oxygen were taken from the literature. Ductility, which may be taken as the strain to failure, is based in the reduction in area achieved during the tensile test as follows:

$$\epsilon_{f_A} = \ln\left(\frac{A_\circ}{A_f}\right) = \ln\left(\frac{100}{100 - \%RA}\right) \tag{8.2}$$

where the subscript A denotes that the strain has been calculated from the change in the cross-sectional area of the specimen during testing.

The amount of oxygen in the weld is directly related to the inclusion volume fraction (Pargeter, 1981).

Figure 8.7 plots ductility against wt% oxygen for 116 welds taken from the literature. The nature of the welding process used in each case is also indicated. Although higher ductilities appear to be associated with lower oxygen contents, there is no clear relationship between the two.

It is suggested that this is because

(i) in weld metals, the volume fraction of inclusions varies typically only in the narrow range from approximately 0.002 to 0.01 by weight,

and (ii) unlike the work due to Edelson and Baldwin (1962), and Le Roy *et al.*(1981), which used carefully controlled base metals, the matrix strength is not constant, and can vary quite considerably in a given set of welds.

In fact, it is possible to be more specific. The experimental results in Widgery (1974) contain data for the inclusion populations of 16 welds. Figure 8.8 illustrates how the non-uniform strain, $(\epsilon_{f_A} - \epsilon_u)$, calculated from his results, is strongly dependent upon the volume fraction of the inclusion present. It should be noted that for five of the welds (K, L, S, T, and U) the magnification of the image analyser that characterised the inclusion populations of the welds had been unreliably calibrated. Thus, the number of inclusions per mm^2, geometric mean inclusion diameter, and

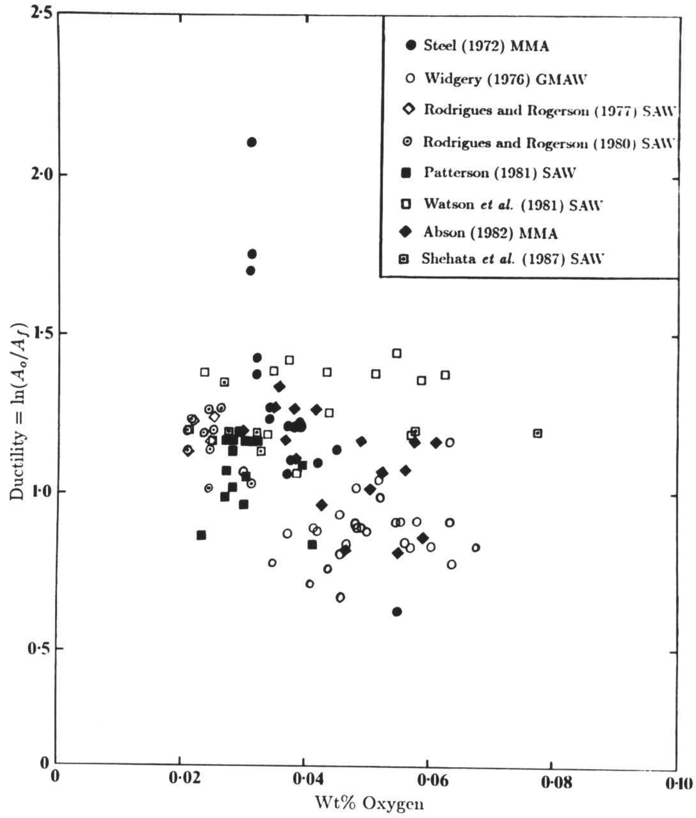


Figure 8.7: The relationship between weld metal ductility and wt% oxygen. The welding process used is also recorded. The references used are cited below.

ABSON, D. J. (1982), *Weld. Inst. Res. Rep.*, Welding Institute, Abington, U.K., No. 194/1982.

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WATSON, M. N., HARRISON, P. L. and FARRAR, R. A. (1981), *Weld. Met. Fab.*, (3), 101-108.

WIDGERY, D. J. (1976), *Weld. J.*, (3), Res. Supp., 57s-68s.

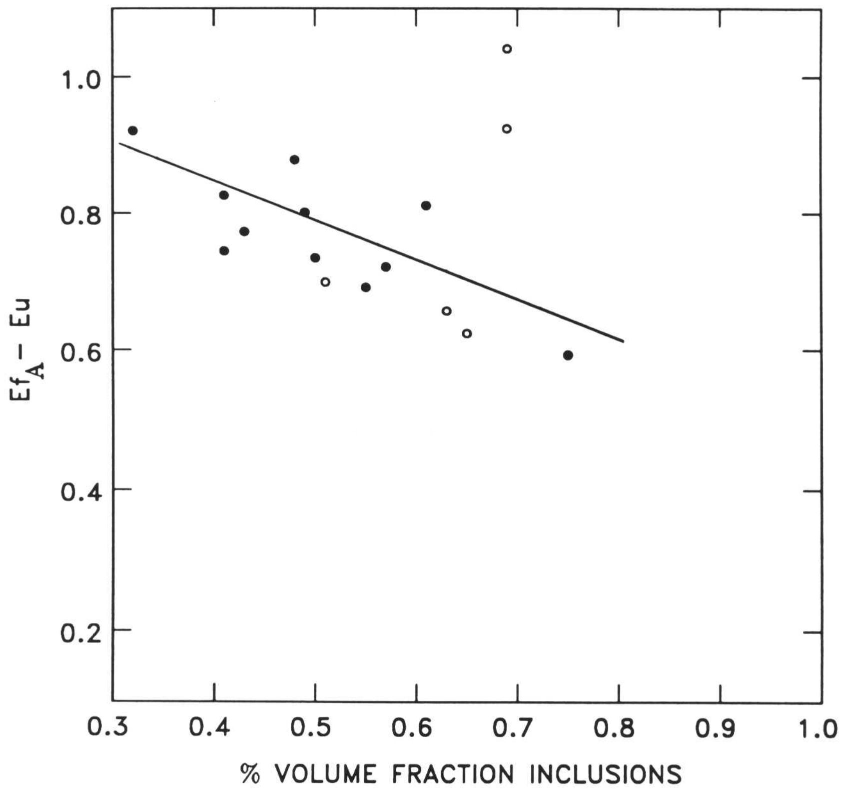

Figure 8.8: The relationship between necking strain and inclusion volume fraction, with 'best fit' line through solid points. [ϵ_{f_A} is the strain to failure, defined in Eqn. 8.2, ϵ_u is the uniform reduction in area achieved during tensile testing. Therefore, $(\epsilon_{f_A} - \epsilon_u)$ represents the non-uniform strain attained during tensile testing]. Data is taken from Widgery, (1974). (The accuracy of the hollow points is uncertain).

average nearest neighbour spacing might, or might not, be correct. Since the first two parameters are needed in order to estimate the volume fraction of inclusions present [according to the method due to Ashby and Ebeling (1966)], the values of f_o, and so the calculated values of reduction in area are questionable. Accordingly, these points have been left hollow. These uncertainties were not mentioned when the results were republished later (Widgery, 1976).

8.4 MODELS

Fracture research has led to a large number of increasingly sophisticated models being formulated to help explain material behaviour quantitatively during ductile failure. These have been the subject of a variety of excellent reviews (Goods and Brown, 1979; Lagneborg, 1981; Embury, 1982; Dodd and Bai, 1987).

It is attractive to consider void coalescence as a gradual process involving the thinning down of the ligaments between voids with increasing strain. Thus, final separation occurs when the ligaments have reduced to zero width. However, McClintock (1968) showed, using detailed calculations, that fracture strains an order of magnitude greater than those observed experimentally are obtained when void coalescence is taken to occur by this internal-necking mechanism. Thomason (1968) modelled void linkage by considering a rigid/plastic material containing a uniform distribution of square prismatic cavities. From this model it appears that the total displacement between two surfaces that is necessary to cause coalescence of cavities is approximately equal to their separation prior to coalescence. For most materials he considers that the distance between adjacent cavities will be small at this point. In a simpler adaptation of their model, Brown and Embury (1973) hypothesized void linkage occurring by joining at an angle of 45°. However, this model has been challenged recently by work due to Ellis (1987), who showed that in a tensile test macroscopic necking will normally have occurred well before the geometric condition is met.

An alternative approach to void coalescence is that fracture will occur when the volume fraction of voids reaches a critical value that is characteristic of the material (Rice and Tracey, 1969; Schmitt and Jalinier, 1982). Although, it is unclear what the effect of the individual sizes of the voids would have, this approach looked suitable for the general class of low-alloy steel weld metals, since it was seen in Chapter 7 that ϵ_A does not appear to change for different matrix strengths. Particle size alone does not appear to have any effect on ductility (Edelson and Baldwin, 1962). In this analysis, which aims to estimate ϵ_A, the strain to failure, the model presented by Gurland and Plateau (1963), and Henry and Plateau (1967) has been used. It is presumed that not only is void growth proportional to macroscopic strain, but also that it is increased by the local strain concentration due to the largest curvature of the void. Void coalescence is then assumed to occur when a critical ratio of hole size in the direction of straining to the hole spacing is reached. This criterion was modified by Gladman *et al.* (1971; 1975) to give a critical ratio of inclusion diameter to 'nearest neighbour' spacing, implying a critical volume fraction of voids for coalescence. This enabled a simpler functional form for strain to failure to be derived.

Decohesion of the inclusion is presumed to occur at negligible strains, and this seems reasonable for weld metals. Widgery and Knott (1978) argued that if void nucleation strain *were* a critical parameter, it would depend upon inclusion size, in which case the inclusion size distribution on a microvoid fracture surface would both show a cut-off at low inclusion sizes, and also not correspond to the distribution measured on a carbon replica of an electropolished surface. These traits were not observed in their work on C-Mn weld metals, and it was concluded that no initiation strain is required, and that inclusions effectively act as voids.

The true strain to failure is given by the following equation:†

$$\epsilon_A = \frac{1}{2}\ln\left\{\left(\frac{\phi^2}{f_\circ^2} + \frac{k}{r^2}\right)/\left(1 + \frac{k}{r^2}\right)\right\} \tag{8.3}$$

where ϕ = critical volume fraction of voids, taken as 0.04,

$f_\circ$ = volume fraction of inclusions,

k = a strain intensification factor,

and r = the length-width ratio of the inclusion where straining is in the length direction (Lagneborg, 1981).

The inclusions are assumed to be spherical ($r = 1$), since they typically have a

† This equation was published incorrectly in Gladman *et al.* (1975).

higher melting point than steel, and therefore solidify in the weld before the steel. It is also assumed that the inclusions are elastically hard (non-deformable).

From Eqn. 8.2

$$\epsilon_{f_A} = \ln\left(\frac{100}{100 - \%q}\right)$$

Therefore

$$
\begin{aligned}
\%q &= 100 - \frac{100}{\exp\left(\epsilon_{f_L}\right)} \\
&= 100 - \frac{100}{\exp\left[\frac{1}{2}\ln\left\{\left(\frac{\phi^2}{f_o^2} + \frac{k}{r^2}\right)/\left(1 + \frac{k}{r^2}\right)\right\}\right]} \\
&= \left[1 - \frac{1}{\sqrt{\left\{\left(\frac{\phi^2}{f_o^2} + \frac{k}{r^2}\right)/\left(1 + \frac{k}{r^2}\right)\right\}}}\right] \times 100 \qquad (8.4)
\end{aligned}
$$

k was taken as 2 (Lagneborg, 1981).

Calculated and measured values of percent reduction in area for results due to Widgery (1974) are given in Table 8.1, and plotted in Figure 8.9.

Steel (1972) and Shehata (1987) performed tensile tests respectively on standard MMA and SA weld metal specimens for which the inclusion volume fractions had been determined experimentally. Table 8.2 gives experimental and calculated values for their welds. It should be emphasized that the welding processes used and the conditions of the welds are in contrast to each other. Steel used MMA as-deposited weld metal, whereas Shehata's was SA narrow-gap, post-weld heat treated. However, it should be noted that experimental evidence shows that the inclusion distribution will not be affected by PWHT (Tweed and Knott, 1983), and so the results of Shehata are perfectly admissible in this analysis. Results from Table 8.2 are plotted in Figure 8.10.

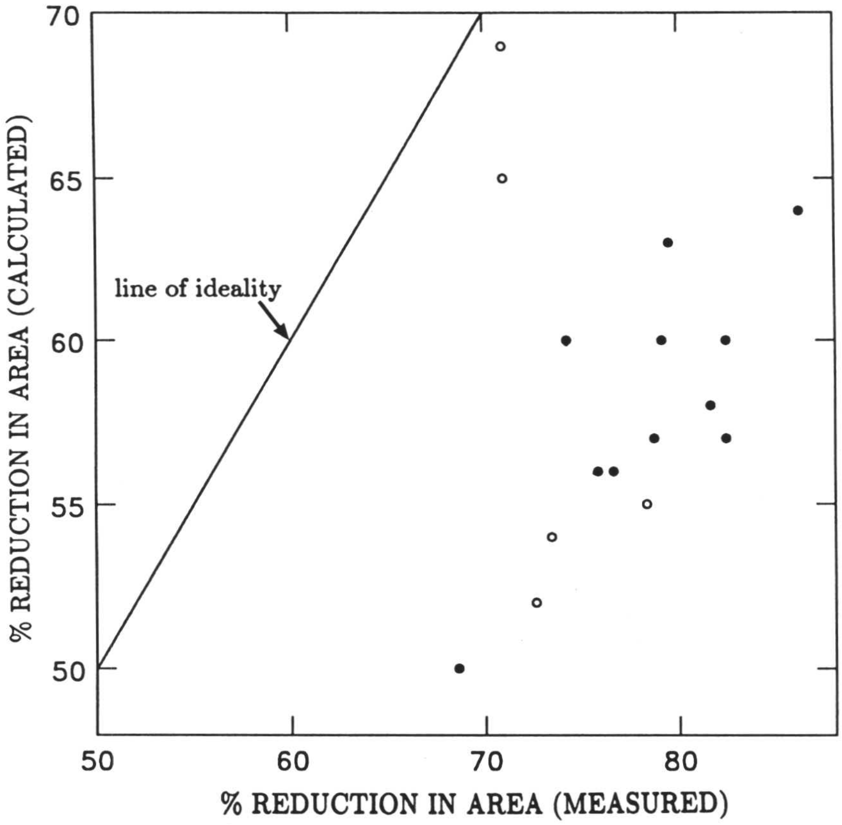

Figure 8.9: Measured and calculated values of percent reduction in area for Widgery (1974). (The hollow points are uncertain).

Weld	q (Measured) (%)	q (Calculated) (%)
A	60	79
B	60	74
C	64	86
D	56	77
E	50	69
F	58	82
G	60	82
H	57	79
J1	57	82
K	55	78
L	69	71
N	63	80
S	52	73
T	65	71
U	54	73
Y	56	75

Table 8.1: Measured and calculated values of percent reduction in area for Widgery (1974).

8.5 DISCUSSION

Figure 8.10 shows the good agreement that exists between the calculated and measured values of reduction in area obtained for the results of Steel (1972), and Shehata (1977). This indicates that, for a given class of materials, the Gladman equation provides a suitable model for the prediction of reduction in area, and that it can be predicted from the inclusion volume fraction.

For the results of Widgery (1974), shown in Figure 8.9, leaving aside the two unreliable points at the top of the graph, the points have a correlation coefficient of 0.75, indicating that the relationship is fundamentally correct. However, Eqn. 8.4 overestimates recorded values for reduction in area by a constant amount of $\approx$15%. Whilst there are many factors that might cause a discrepancy, such as differences

Ref.	Weld	q (Measured) (%)	q (Calculated) (%)
Steel (1972)	X1a	76	81
"	X1b	75	81
"	X13a	82	77
"	X13b	82	77
"	X15a	83	77
"	X15b	88	77
"	X8a	66	74
"	X8b	70	74
"	X16a	70	73
"	X16b	71	73
"	X17a	72	76
"	X17b	71	76
"	X11a	67	69
"	X11b	67	69
"	X18a	70	70
"	X18b	67	70
"	X19a	68	69
"	X19b	68	69
"	X12	47	63
Shehata (1987)	W1	74	85
"	W2	68	80
"	W3	70	82
"	W4	70	66
"	W5	70	69
"	A	75	84
"	B	70	85

Table 8.2: Measured and calculated values for percent reduction in area.

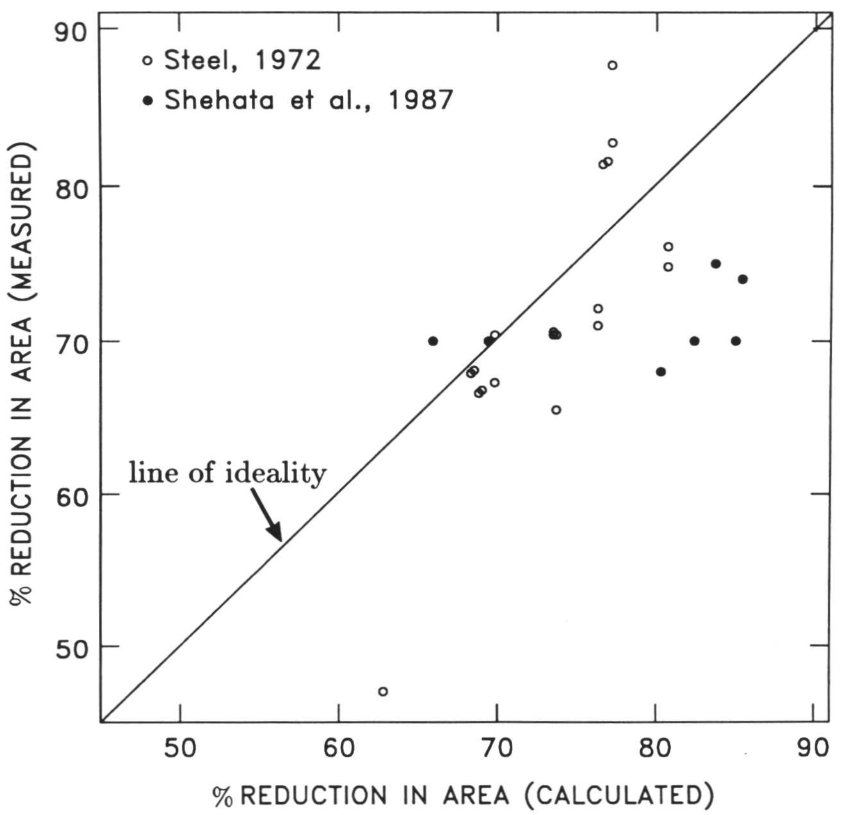

Figure 8.10: Measured and calculated values for percent reduction in area. (Data due to Steel (1972) and Shehata (1987)).

in the composition and hardness of the inclusions, differences in the experimental techniques used for the analysis of the inclusion volume fractions, and differences in the strengths and strain hardening behaviour of the welds, perhaps the most likely cause is that Widgery's specimens were of unusually small dimensions ($l_o < 25$mm). The increased hydrostatic tensile stresses which arise as a consequence of necking mean that the stress distribution across the tensile specimen is no longer planar, and the average stress required to cause flow from maximum load is higher than if only uniaxial stress were present. Ductility will tend to decrease in the presence of a steep stress gradient and triaxial stress field. Since the latter is a function of the test-piece dimensions, values of reduction in area are dependent upon specimen geometry and dimensions (Dieter, 1968). There is a small effect of changes in the strain-hardening exponent, n, during the process of void coalescence, and the lower the value of n, the more rapidly voids will coalesce. However, this is thought to be of secondary importance when compared with the effects of superimposed triaxial stresses experienced in tensile testing. Thus, during the necking stage, when triaxial stresses come into play, either the critical volume fraction of holes required for failure, ϕ, will be different, or, what appears more likely, the value of k, the stress-intensification factor, will be different, for Widgery's smaller specimens. It is, perhaps, significant, that the error between the calculated and predicted values of percent reduction in area for Widgery's welds is constant, which suggests the influence of a constant factor, such as specimen geometry.

Elongation is still predictable for Widgery's specimens (Chapter 7), since necking behaviour is not critical in this case.

8.6 SUMMARY

The parameters which determine weld metal reduction in area have been investigated. For a given set of welds, reduction in area is observed to decrease with increasing volume fraction of inclusions. An even clearer correlation was observed between the volume fraction of inclusions, and the post-UTS strain, indicating that this is the part of the fracture process on which the inclusion population has most influence.

A simple theory has been used to explain the adverse effects of inclusions on the reduction in area of low-alloy steel weld metals. Fair agreement has been obtained with results calculated from theory, and experimental data due to Steel (1972), and Shehata (1987). Discrepancies between predicted and measured values for reduction in area for sub-size (geometrically similar) specimens have been tentatively attributed to differences in necking strain behaviour.

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