An Analysis of a 100 te Storage Vessel

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The object of this paper is to suggest a method of reviewing the possibility of cold whole vessel failure and establish whether a leak before break-condition is more likely than catastrophic failure.

Fracture mechanics concepts have been used to calculate critical crack sizes. Stress levels at operating and test conditions have been calculated using the approaches given in British Standard 5500:1982 which includes the effects of bending, shear and stresses around support saddles.

The beneficial effects of stress relief are reflected in the large crack lengths a stress relieved vessel is able to sustain. It is shown that vessels in this condition should leak before a major failure situation can occur.

1. INTRODUCTION

A number of surveys of operational experience from conventional pressure vessels have been carried out by the Safety and Reliability Directorate of the UKAEA in conjunction with a group of major engineering insurance companies. Evidence gathered for these reviews showed that failure did occur in high quality plant.

No major failures of Liquid Petroleum Gas (LPG) storage vessels have been reported in the UK, and historically the UK has a good record in that there have been no large releases due to spontaneous tank failure. Nevertheless it was against the wider background of pressure vessel experience that it was decided to analyse an LPG storage vessel. The analysis is confined to a static above ground tank used for the pressurised storage of LPG and of 100 te capacity.

Too many variables are available to designers, constructors and operators to claim that all storage vessels may be considered alike. Certain recommendations regarding stress relief inspection requirements, design code application and material selection do have a common denominator effect in so far as they are applied. However information to hand is neither sufficiently detailed in application nor nationally spread in implementation for any firm conclusions to be drawn about the whole UK LPG industry storage problems on the basis of any analysis of one vessel.

2. REVIEW OF PRESSURE VESSEL FAILURE EXPERIENCE

Since 1962 the Safety and Reliability Directorate in conjunction with a group of major engineering and insurance companies, has undertaken surveys of conventional pressure vessels. (Refs 1.2.3). The principal objective of the surveys was to obtain data from which to assess the reliability of pressurized plant built to high standards of construction.

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The surveys were conducted with the objective of identifying failure locations, numbers, causes, method of detection, and operating conditions from a researched population with minimum thickness and pressure parameters. These are listed in the reports.

Evidence gathered for these reviews showed that the majority of failures manifested themselves as cracks, some of which leaked, but there were also a number of catastrophic failures which resulted in a large discharge of contents.

Probabilistic failure predictions based on data gathered for the surveys are possible for generic categories of vessel and could prove useful. Assessment of individual components or small populations of components are somewhat more difficult. While data exists from which judgement on failure probability may be made, other considerations need to be taken into account when analysing the probable behaviour of a specific vessel category. These can best be solved by a deterministic approach based on fracture mechanics theories hence the reason for work described in subsequent sections of this paper.

3. FRACTURE MECHANICS - SOME AVAILABLE METHODS

In recent years fracture mechanics methods have become increasingly important where the control or prevention of fracture is a paramount consideration.

The main property in fracture studies is the material toughness under various conditions of stress, mechanical properties and temperature. Material toughness is a measure of the ability of a material to withstand the stress intensifying effect at a crack tip. In ductile materials it does this by deforming mainly elastically in very low stress situations, or plastically as stress levels increase. At a critical dimension the net section under the crack becomes unstable and fracture results. There can be, and usually is, a degree of stable crack growth before the onset of instability. The object of fracture mechanics is to predict at what size and under what conditions a defect length becomes critical.

The three fracture mechanics approaches used in this analysis have been selected for the following reasons.

a) The Stress Concentration Theory has been developed from tests on vessels varying in diameter from 305mm (12 inches) to 2895mm (9ft 6ins), and from wide plate tests. It has been checked and found to be in reasonable agreement with other vessel failure data. The theory has the advantage of only using well established material properties of yield and ultimate stress levels, elongation and Charpy 'V' notch energy values.

b) PD 6493: 1980 Ref 4 is a British Standard Institution document and should be used in any comparative fracture mechanics exercise. It is however basically a guide to the sizing of tolerable flaws in welded joints and employs linear elastic fracture mechanics (LEFM) and elastic plastic fracture mechanics (EPFM).

c) Numerous techniques are available for extending LEFM into the elastic plastic regime. Crack opening displacement (COD) measurements are currently the most widely used of several modifying parameters and these have been selected for this analysis.

All fracture mechanics theories are sensitive to a number of input data functions, although in this respect the stress concentration theory is less sensitive than others because it relies on standard material property data which is readily and regularly established. Other approaches are dependent upon a number of derived data which include various toughness parameters, energy functions, material resistance, crack opening displacement measurements, compliance functions.

When none of these data are available for a vessel under consideration use has to be made of relevant correlating relationships with known material properties and data from similar steel.

4. MATERIAL AND MATERIAL PROPERTIES

Steel selected for the storage of potentially hazardous substances must have adequate strength, be compatible with tank contents and show little or no deterioration of properties under service conditions. It must remain ductile and maintain its resistance to defect propagation under the most arduous of operating environments, and allow the production of high quality welds. The mechanical properties of the weld metal and heat affected zone should be at least as good as that of the parent plate particularly as surveys of conventional plant show weld and weld affected metal to be the most vulnerable sites for flaws especially in regions of discontinuities.

Mechanical properties for plate to BS 1501:151 and weld metal were obtained for yield stress, UTS and elongation together with a Charpy value at -15°C.

5. FACTORS AFFECTING STRESS ANALYSIS

5.1 Shakedown

Stress levels were calculated for the various loadings to which the vessel is subjected. These included primary and secondary stresses due to pressure, dead weight, geometrical discontinuities.

Various factors will arise during the course of testing and operation which will modify or influence the effect of the stressing system. For example, where peak stress levels are at the value of the material yield stress the phenomenon known as shakedown will occur during the first few operating cycles. In particular the proof pressure test can have a significant effect as illustrated in Fig 2. Describing this figure it can be seen that on loading up during the pressure test, if the strain range associated with the linearised peak equivalent elastic stress is

$$\frac{\sigma_{EE}}{E} = \varepsilon_{max} \qquad \ldots\ldots (1)$$

and if the proof test has an overpressure ratio

$$\frac{\text{Test pressure}}{\text{Operating pressure}} = R$$

then the maximum operating strain range is

$$\frac{\varepsilon_{max}}{R}$$

On unloading from the pressure test

$$\sigma_R = \sigma_{ys} - E\varepsilon_{max}$$

Reloading to operating pressure gives

$$\sigma_{op} = \sigma_R + \frac{E\varepsilon_{max}}{R} \qquad \ldots\ldots (2)$$

$$\text{Then}\ \sigma_{op} = \sigma_{ys} + \left(\frac{1}{R} - 1\right)\sigma_{EE} \qquad \ldots\ldots (3)$$

While these arguments are theoretically valid up to a value of ε_{max} equal to twice the yield strain ε_{ys} the Bauschinger effect may confine complete shake-down to strain ranges not exceeding $1.6\sigma_{ys}$. In a welded structure there may be a difference in yield stress values for parent plate and weld metal. This difference can be an important factor in shakedown analysis.

Residual stresses resulting from welding a plate into a structure are essentially of two kinds. There is the stress caused by general shrinkage and fit up which affects the whole plate. But there is also the more significant stress caused locally by the cooling contraction of the deposited weld metal and which is generally considered to be of yield stress magnitude effective over a distance of the order of the plate thickness.

How residual stresses influence fracture in a ductile material is not fully understood, particularly where critical crack sizes are long (several times the plate thickness).

Additional operational stresses may simply be added and may even cause some defect propagation, but local yielding would relax the stress level before unstable conditions were reached. This implies a form of mechanical stress relief and can result from the application of a proof pressure test.

Since the great majority of defects are sited in weld affected material it is the weld properties which control crack tip behaviour. This is significant where linear elastic conditions are applied and little, or no, plastic deformation occurs. However when elastic plastic or general structural collapse is concerned large areas of the component will be affected and the structural behaviour of the component will be controlled by the yield point of the parent plate. Therefore since weld metal comprises only a small fraction of the total volume of affected material it is the yield stress of parent plate which should be used in shakedown analysis. For vessels which are subjected to fatigue loadings, combinations of stresses from all sources should be kept within minimum plastic limits so that the vessel may 'shakedown' to purely elastic behaviour and plastic cycling causing incremental collapse can be avoided.

5.2 Bulging

In a component whose wall thickness implies plane stress conditions critical crack lengths can be several times the wall thickness in length. In (curved) cylindrical geometries the action of the pressure on the unsupported edges of a through thickness crack will impose additional stresses at the crack tip due to the bending effect. The importance of these additional stresses increases with increasing crack size and pressure, but decreases with increasing diameter until flat plate conditions apply.

5.3 Fatigue

Another mechanism to be considered is fatigue, and this must be allowed for in any analysis in which cyclic loadings can be applied to the component.

A great deal of experimental work has been done to investigate the fatigue phenomena and results indicate that a fatigue crack growth rate 'da/dN' can be characterised in terms of the stress intensity range 'ΔK_1' a constant 'C' and 'n' the slope of the log da/dN V log ΔK_1 curve so that

$$\frac{da}{dN} = C(\Delta K_1)^n \qquad \ldots\ldots (4)$$

6. STRESS LEVELS

It is assumed that the vessel is a horizontal cylindrical unit supported on two symmetrically placed saddles, one welded to the vessel and the other free. (Fig 1).

Maximum stresses resulting from dead weight and pressure loadings will be those associated with the vessel acting as a beam and those stresses produced by reaction at the supports, which are assumed to be rigid for the purposes of this report. Other areas of concern are those around the larger penetrations such as the manway penetration.

Design rules for determining stress levels in the saddle areas are contained in the pressure vessel design code BS 5500:1982.

These rules and formulae have been used to calculate stress levels in the saddle areas for the tank support system. Future modifications to these design criteria are likely as a result of work currently being carried out at Strathclyde University (Ref 5) where tests and calculations suggest that for a rigid saddle support the stresses predicted by BS 5500 are exceeded by a factor of 2, while the stress levels for a flexible support system more closely agree with BS 5500 values. However collapse tests on vessels indicate that even with rigid supports the design code values are safe.

Further analytical work (Refs 6 and 7) confirm that the maximum stress in vessels supported by rigid or flexible saddles occurs in the region of the saddle horn. Results also show that for flexible support saddles there is a useful built in factor of safety when the design is based on BS 5500. The peak stress in the vessel is in general higher than values obtained by use of relevant code formulae but it is restricted to a small area of the vessel and is mainly a bending stress.

7. FRACTURE MECHANICS PROCEDURES AND CRACK SIZE CALCULATIONS

7.1 Stress Concentration Approach

In the 1960s Irvine and Quirk developed a fracture mechanics theory involving only conventional mechanical properties, Ref 8.

Consider half the crack length "ℓ", then if all the load shed by the crack material is carried by a region "S" at the crack tip then

$$\sigma_g.(\ell + S) = \sigma_u.S \qquad \ldots\ldots (5)$$

where S is the width of the stress perturbation at the crack tip which is raised to an average stress of σ_u. (Fig 3(A)).

Fig 3(B) shows a typical plot of experimental results in which σ_g v $\sigma_g \ell$ gives a straight line relationship according to equation (5) rearranged

$$\sigma_g = -\frac{1}{S}(\sigma_g \ell) + \sigma_u \qquad \ldots\ldots (6)$$

This is more usually written as

$$\sigma_u = \sigma_g \left(\frac{\ell}{S} + 1\right) \qquad \ldots\ldots (7)$$

The parameter S' is a material constant which is independent of crack length but which is affected by temperature.

Considerable effort has been made to establish the dependence of 'S' on values of Charpy energy, yield stress and tensile elongation. Ref 9 uses the large volume of data from several steel specifications to obtain a correlation. This shows a fairly weak dependence on charpy energy and yield stress since they are only effective to the power 1/3, but the influence of elongation in the value of 'S' is much more significant as it is effective to the power 2.

The material characteristic 'S' may be related to other material properties of elongation Charpy and yield stress by the formula

$$S = C \times (\text{elongation }\%)^2 \times \left(\frac{\text{Charpy}}{\text{yield stress}}\right)^{1/3}$$

$C = 13.2 \times 10^{-2}$ where Charpy is in Joules
yield stress in MN/mm^2
and S is in cm.

Plate to plate variation in mechanical properties also affect calculations of critical crack size. This is simply illustrated in Fig 4 which shows the effect of changes in σ_u on values of 'S'. Hence the need to investigate the effect of scatter in plate properties.

Where bulging can take place failure can occur at significantly lower values of internal pressure. The actual effects can be calculated by the formula

$$\frac{\sigma_u}{\sigma_g} = \left(\frac{K \ell_c^2}{D^2} + \frac{\ell_c}{S} + 1\right) \qquad \ldots\ldots (8)$$

where the constant K is proportional to the third power of the material yield stress. Results of tests carried out on cylinders ranging in diameter from 12.0 inches to 9 ft 6 inches are plotted in Fig 5. The results indicate that the relationship between K and σ_y is given by the equation

$$K = 1.875 \times 10^{-3} \times \sigma_y^3 \qquad \ldots\ldots (9)$$

Fig 6 shows the effect of the additional bending stresses due to bulging and illustrates that failure pressures are in fact lower where bulging can occur. Since the component diameter 'D' appears in the denominator of the bulging effect equation given above, and is also squared, it is obvious that as a cylinder or sphere increases in diameter the bulging effect tends to vanish and flat plate equations may be applied. In the case of partial penetration cracks the bulging action is a latent effect until snap through of the remaining ligament occurs. In the particular case of the 100 te vessel it was found that the bulging could be neglected.

Table 1 lists crack sizes using equation 7 and typical material properties for weld metal and plate to BS1501:151. Plate crack sizes at operating and test pressures respectively are 3.3 and 2.2 inches (84mm and 56mm) while for unstress-relieved conditions after shakedown the crack size is 2.1 inches (53mm). The equivalent figures for weld metal are 3.6 and 2.6 inches (92mm and 66mm) reducing to 1.0 inches after shakedown (25.4mm).

7.2 British Standard PD 6493:1980

PD 6493:1980 gives guidance for carrying out an engineering critical assessment of defects found in fusion welded joints. The methods proposed are applicable to welds in a number of structural steels of thicknesses 10mm and above, and are aimed at establishing tolerable defect sizes.

It will be assumed that stresses are due to static loads and act in a direction perpendicular to the defect. Dynamic effects are not considered. Since the tank under consideration has been stress relieved residual stresses should be minimal.

Typical Yield stress, ultimate stress and elongation values are available. Charpy impact values show considerable scatter; lower bound values will therefore be used.

No fracture mechanics data in terms of fracture toughness (KIC) or K_c for plane stress conditions, crack opening displacement (COD) or the J integral contour are available. However formulae have been derived correlating some of these factors with formal mechanical properties and these will be used where required.

Several fracture mechanics theories are available - the document lists 12 methods - but there is no universally accepted approach for assessing material behaviour in the plastic regime.

Many empirical correlations exist between fracture toughness as denoted by K_1 and Charpy energy values (Φ). One widely used correlation is the Rolfe Novak and Barsom approach which relates K_1 to Charpy and material yield stress (σys) in the following form

$$\frac{K_{1c}^{2}}{\sigma ys} = \frac{5}{\sigma ys}\left(\Phi - \frac{\sigma ys}{20}\right) \qquad \ldots\ldots (10)$$

It is recommended however that this correlation be used for Charpy values above the transition temperature and for steels with relatively high (110-246 KSI) yield points. At low energy absorption levels little variation appears to exist between slow bend and dynamic Charpy values and a correlation for below transition temperature values for steels with a yield stress range 36-50 KSI is given by

$$\frac{K_{1c}^{2}}{E} = A(\Phi) \qquad \ldots\ldots (11)$$

where for structural mild steels A can be taken as 5. Another general transition range correlation for steels whose yield points vary between 39 and 246 KSI is

$$\frac{K_{1c}^{2}}{E} = 2\,(\Phi)^{1.5} \qquad \ldots\ldots (12)$$

Ref 10 lists 13 Fracture Toughness V Charpy Energy correlations for K_{1c} and K_{1d} values for a wide variety of steels and for above and below Charpy transition temperatures. Selection of an appropriate correlating formula is complicated by the fact that several are based on pre-cracked Charpy and slow bend test specimen data and these data are not available.

Selection of the most appropriate formula must be subjective because of uncertainties in basic data and applicability. It would be prudent therefore to selct the correlation which gives the lowest values of K_{1c} since at low energy levels this is unlikely to be markedly different from K_{1d}. Fig 7 shows Equation 11 to be considerably more pessimistic than Equation 12.

From mill tests a typical figure for the yield stress (σys) of BS 1501:151 material

$$= 265\ \mathrm{N/mm^2} = (38.0\ \mathrm{KSI})$$

Yield stress for weld metal is somewhat higher at

$$= 460\ \mathrm{MN/m^2} = (67\ \mathrm{KSI})$$

The tolerable defect size $a_m = C \left(\frac{K_{1c}}{\sigma_{ys}}\right)^2$ ..... (13)

where C is obtained from PD 6493 Fig 14 and is dependent upon the ratio of applied stress to yield stress

Applied stress = membrane stress + bending stress + Q + F

where $F = \sigma m\ (K_t - 1)$ ..... (14)

Ref 11 gives a value of 1.8 for a machined fillet stress concentration factor (K_t). A welded fillet such as the geometry at the saddle attachment would attract a higher value than this, say 2.5. (BS 5500:1982 (2.3.3)).

Stresses included under the symbol "Q" are self equilibrating and result from mechanical loads differential thermal expansions etc and for the purpose of this analysis are assumed to have a negligible input to the overall applied stress.

Using the procedures outlined earlier gives an

Applied stress = 347 MN/m² (50 KSI)

Equation 11 is used to establish K_{1c} for both plate and weld metal. From this formula using Charpy values of 10°C

$$K_{1c} = 5E\ (\phi) = 88\ \mathrm{MN/m^{-3/2}}\ (80\ \mathrm{KSI})\quad \text{for weld}$$
$$= 52\ \mathrm{MN/m^{-3/2}}\ (47\ \mathrm{KSI})\quad \text{for plate.}$$

Equation 13 gives values of a_m which are listed in Table 1. These crack sizes are acceptance sizes and a guide to the sensitivity requirement of inspection techniques and attract a safety factor of between 2 and 10 (Ref 12).

7.3 Elastic Plastic Fracture Mechanics

It is usual when $\left(\frac{t}{2.5}\right)^{1/2} < \frac{K1c}{\sigma ys}$ for a plane stress - or mixed mode - failure condition to control fracture. Defect sizes may be several times material thickness in length and relatively large plastic zones are formed in front of the developing crack tip. Because of the inherent ductility of carbon and carbon manganese steels they are able to withstand considerable deformation under normal loading rates and temperatures seen in service. Linear elastic considerations are upset and the limits of their validity exceeded when the effects of defects extend beyond the materials elastic regime into the plastic region.

Two of the more generally used parameters for extending LEFM are the 'J' contour integral and the crack opening displacement (COD) measurement.

A value for the 'J' integral can be estimated experimentally and is a function of the area under the load extension curve for cracked specimens. In an elastic-plastic condition it is a measure of potential energy change in the system for an increment of crack extension.

A more widely used technique is that of the COD measurement which is a measure of the deformation at a crack tip at the instant of crack propagation. There is a considerable volume of COD data and several correlations have been formulated with other mechanical properties such as Charpy 'V' notch energy and material yield stress. It is perhaps because of this access to a greater data base that COD has become such a generally used concept.

The technique for extending fracture mechanics into the elastic plastic condition used below is the one of crack opening displacement (COD) measurement (δ). For plane stress non workhardening conditions Dugdale Ref 13 has developed a relationship combining crack length (2ℓ), yield stress applied stress (σ), Youngs modulus (E), and COD (δ) so that

$$\delta = \frac{8\sigma ys\,\ell}{E\pi} \ln \sec \left(\frac{\pi}{2}\frac{\sigma}{\sigma ys}\right) \qquad \text{..... (15)}$$

Burdekin and Dawes have derived a correlation of COD and yield strain to calculate a critical crack length (2ℓ)

$$\ell = \frac{\delta c}{2\pi\sigma ys\,\theta} \qquad \text{..... (16)}$$

Cotton (Ref 13) has modified the Burdekin approach for elastic plastic conditions and derived

$$\delta c = 2\ell\,\varepsilon ys\,\pi \left(\frac{\varepsilon}{\varepsilon ys} - 0.25\right) \qquad \text{..... (17)}$$

Figures 16.6 through to 16.11 of Ref (13) plot COD V temperature for various steels, C Mn, weld metal, and heat affected zone (HAZ) material. Test data are at temperatures between -20°C and -150°C. Considerable scatter is evident but a lower bound COD value for parent plate material extrapolated to +10°C is 0.66mm (25 x 10^{-3} inches). For weld metal and HAZ material CODs at 10°C are respectively 0.2mm (8.4 x 10^{-3}) and 0.19mm (7.8 x 10^{-3} inches). A further extrapolated value for mild steel plate gives 0.45mm (18 x 10^{-3} inches) at +10°C.

Ref (14) gives a value of δc for BS 1501:151 material as 1.15mm (45 x 10^{-3} inches) as ascertained by 3 point bend tests on material machined from test vessels. This figure was reasonably constant over a temperature range from +8°C to +90°C. A further reference (Ref 15) plots COD V temperature for mild steel wide plate tests. Fig 3.11 of this reference gives 0.75mm (30 x 10^{-3} inches) as the COD value at 10°C.

Refs (14) (15) and (16) show that CODs for BS 1501:151 as measured during vessel tests are generally greater than the values obtained by laboratory bend tests.

Table 1 lists crack sizes for a range of COD values.

8. FATIGUE ASSESSMENT

In fabricated components such as the vessel being considered in this paper, critical flaw sizes will be much larger than any defect which may be initially undetected. The difference in size between the initial defect and a critical size defect will depend to a large extent upon the quality of manufacture and inspection coverage.

If we assume that the component will not fail by brittle fracture with little or no extension of an original defect, it is necessary to calculate the number of operational cycles necessary for a small crack to grow to a critical size.

Several steps are necessary for this exercise.

1. Assume an initial flaw size - a_o
2. Calculate a critical crack size using fracture mechanics parameters - a_{cr}
3. Determine cyclic stress range in terms of K
4. Apply standard crack growth rate formula - the Paris equation
   $$\frac{da}{dN} = C(\Delta K)^n \qquad \text{..... (18)}$$
5. Integrate or use iterative procedure between a_o and a_{cr} to obtain number of cycles.

These calculations require a knowledge of the basic laws of crack growth and the material variables contained in the constant 'C' and the index 'n' of the crack growth formula.

A large volume of work has been done to establish the constants 'C' and 'n' and as with other basic data considerable variation exists.

PD 6493:1980 gives C = 1.7 x 10^{-15} and n = 4 for propagation rate per cycle and a 99% probability of survival - the units being mm/cycle and K in $MN^{-3}/2$.

Fulmer Research Institute, (Ref 17) gives C = 10^{-12} and n = 4.0 for "structural steels" - units being in m/cycle.

AGR Materials Programme (Ref 18) gives C = 4.6 x 10^{-12} and n = 3.5 for "structural steels" units m/cycle.

Barsom and Rolfe (Ref 13) give C = 3.6 x 10^{-10} and n = 3 for ferritic particle steels - units inches/cycle. This reference also gives $\Delta K = 1.12\sqrt{\pi}\Delta\sigma\sqrt{a_{av}}$ where a_{av} is the average length of a crack between growth increments and ΔK is the difference between maximum and minimum stress levels.

Assuming a partial penetration defect has perforated the vessel wall with a length equal to the wall thickness and for conservatism using the edge crack constant 1.12, the number of cycles for it to grow to twice the wall thickness is given by

$$N = \sum_{a_o}^{a_{cr}} \frac{\Delta a}{C \times (1.12\sqrt{\pi}\ \Delta\sigma\ \sqrt{a_{av}})} n \qquad \ldots\ldots (19)$$

where Δa is the increment of crack growth and a_{av} is the average crack size between two increments of growth.

Using the PD 6493:1980 constants (as they give the greatest growth per cycle)

N = 150 cycles

Using the Barsom and Rolfe constants which give the lowest crack growth per cycle, gives a value of N in excess of 3×10^3 cycles. Fig C.2.1 of BSS 5500:1980 gives a value for N of approx 2.5×10^3 cycles obtained from smooth specimens. For approximately 35% of operating time the tank will be about half full, ie will contain between 40 and 60 tons. 40% of its time it will contain between 60 and 100 tons and for the remaining 25% of its time less than 40 tons. If these figures taken from actual field record are typical the number of full stress range cycles seen during the tank lifetime will be relatively small, say 350 during service life. It is apparent, therefore that despite the difference in numbers of cycles the use of different constants will produce against the number of cycles the vessel will see, fatigue is unlikely to be a significant problem as a failure mode.

9. CONCLUSIONS

1) Table 1 summarises the critical (and tolerable) crack sizes obtained by the three methods used in this paper. It should be noted that a reserve factor of between 2 and 10 is built into the PD 6493:1980 figures.

2) There is reasonable agreement on critical crack sizes between the two fracture mechanics approaches adopted. PD6493:1980 gives defect sizes which are essentially a guide to the sensitivity required of inspection techniques. Given that these sizes attract a safety factor of up to 10 these figures again show reasonable agreement with the other methods used.

3) A fatigue assessment has been made to estimate the number of cycles required to increase a defect from a length equal to the vessel wall thickness to a length equal to double the wall thickness.

   Selecting data which give the maximum and minimum crack growth per cycle gives the number of cycles for 2a=t to 2a=2t as

	PD6493	Ref 13
No of cycles	150	3×10^3

   The equivalent figure from BS5500 is 2.5×10^3. Providing inspection ensures that no major defect will exist initially, fatigue should not be a significant problem as a failure mode.

4) In a relatively thin walled pressure vessel manufactured from a readily weldable low strength ductile material like BS 1501:151 critical sizes of through the wall cracks will be several times the wall thickness in length. Under normal circumstances the vessel contents will leak from such defects before disruptive failure can occur, but local chilling may defeat this condition particularly in unstress relieved vessels.

   For partial penetration defects propagating under operating conditions, when the crack depth plus its associated "plastic" zone exceeds the vessel wall thickness snap through of the remaining ligament will occur when the stress on it reaches the UTS. If at this point the resulting through thickness crack is sub-critical leakage will result.

5) In the unstress-relieved condition critical crack lengths can be so short (25.4 mm) that a leak before break condition could be excluded.

6) For the future in order to minimise the need to deduce required fracture mechanics parameters, it is necessary to establish the relevant properties from actual vessel material. Transition curves from Charpy tests or at least sufficient test results to establish upper and lower shelf values are required.

   Conventional mechanical properties of yield and UTS stress levels at upper and lower operational temperature are required.

   Crack opening displacement measurements on crack material over an appropriate range of temperatures are also required. BS 5762:1979 describes an approved test procedure.

   Fatigue data which would establish the constant and index in the Paris crack growth formulae would be useful additional information.

10. ACKNOWLEDGEMENTS

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The views expressed are those of the author and do not necessarily reflect the views or policy of the Health and Safety Executive.

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TABLE 1

CRACK SIZES (IN INCHES)

	PLATE				WELD			
	SR		USR		SR		USR	
	OPERATING	TEST	OPERATING	TEST	OPERATING	TEST	OPERATING	TEST
STRESS CONCENTRATION THEORY			After Shakedown				After Shakedown	
With Bending	3.3	2.2	2.1		3.6	2.6	1.0	
Without Bending	6.2	4.0	3.6		6.4	4.5	1.6	
ELASTIC PLASTIC COD APPROACH								
COD 30×10^{-3}	5.3	4.7	2.95	2.7	6.3	5.4	2.4	2.3
20×10^{-3}	3.5	3.1	1.9	1.85	4.2	3.6	1.6	1.5
7×10^{-3}	1.25	1.1	0.7	0.65	1.4	1.2	0.56	0.52
3×10^{-3}	0.54	0.46	0.3	0.27	0.6	0.5	0.24	0.23
PD 6493	0.3	0.23	0.16	0.14	0.55	0.32	0.3	0.28

SR = Stress Relieved
USR = Unstress Relieved

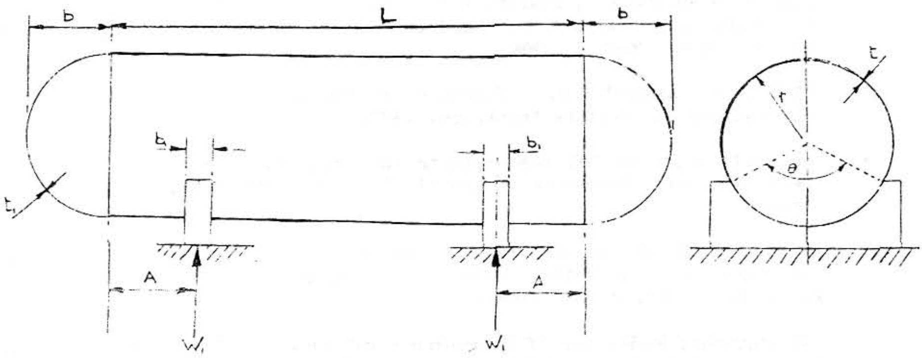


FIG 1

r = 1829 mm (72.0 ins)

t = 19 mm (0.75 ins)

b = 1681.5 mm (66.0 ins)

t_1 = 17.5 mm (0.7 ins)

L = 18062 mm (711.0 ins)

b_1 = 381 mm (15.0 ins)

A = 1350 mm (53.0 ins)

θ = 120°

W_1 = 15810 kgs (34800 lbs)

Design Stress = 163 N/mm² (23635 lbs/sqin)

P = 1.45 N/mm² (210 psi)

Youngs Modulus = 207000 MNm^{-2} (30×10^6 lbs/sqin)

100te STORAGE VESSEL

UNIFORM GROSS STRESS

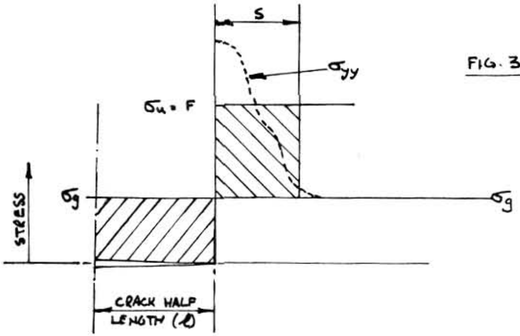


FIG. 3A

Considering equilibrium in plane of crack.

$\sigma_g \ell$ = load shed by cracked material = extra load carried by crack tip material

$F - \sigma_g$ = is proportional to the capacity of crack tip material to carry additional load at failure

σ_{yy} = Distribution of crack tip stress normal to crack plane

F = Failure stress of material at crack tip

$\therefore$ For crack which is just critical:- $\sigma_g \, l \propto (F - \sigma_g)$ for constant material properties.

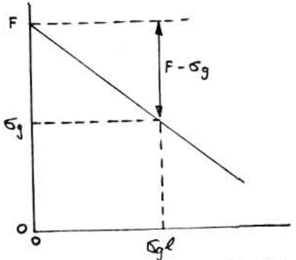


FIG. 3B

Re write as:-

$\sigma_g \ell = S(F - \sigma_g)$ equality of shaded areas in FIG 3A

$F = \sigma_g(\ell \cdot \frac{1}{S} + 1)$

F = UTS (exp. evidence) = σ_u

$\frac{1}{S}$ = a material constant

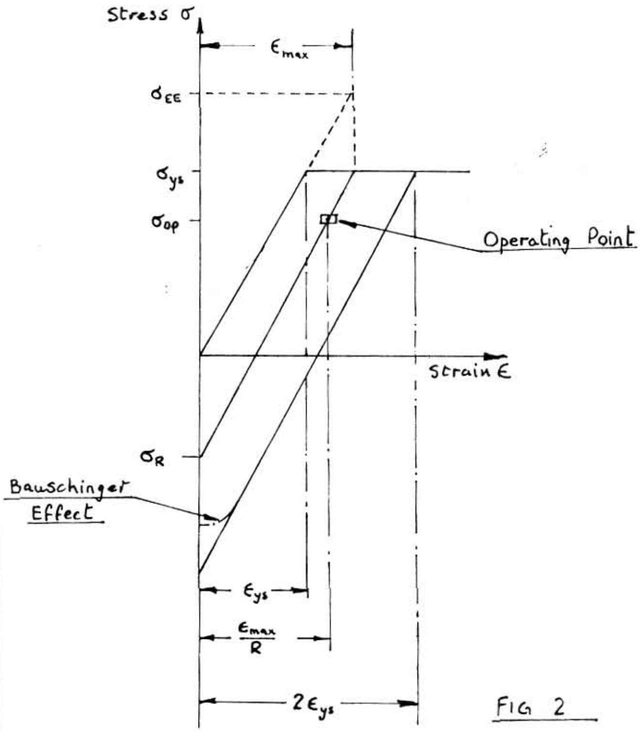
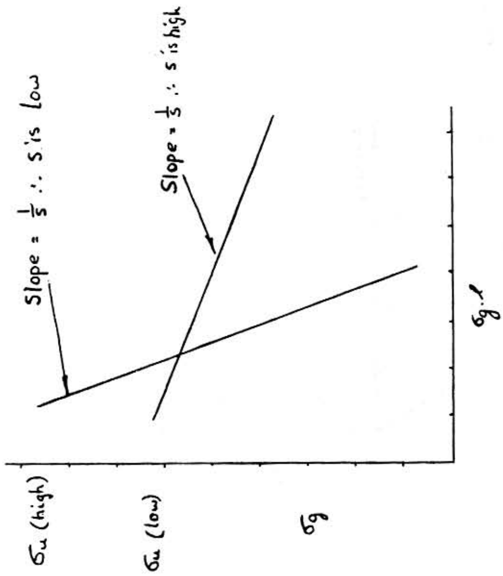


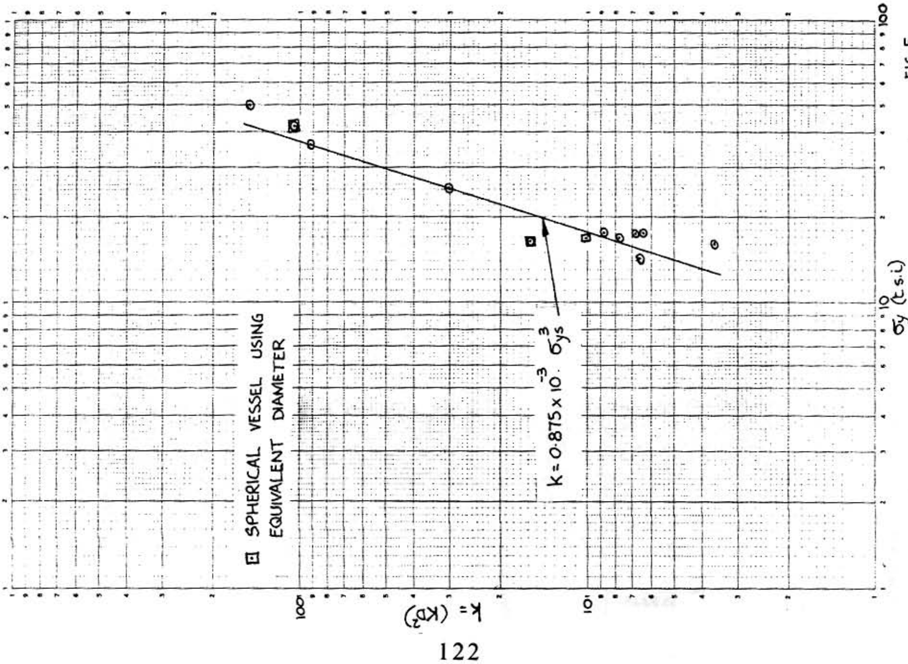
FIG 2

Shakedown - Diagramatic Illustration



σ_g v $\sigma_g l$ Showing Effect of σ_u on 's'

FIG. 4



RELATION BETWEEN YIELD STRESS AND K

FIG. 5

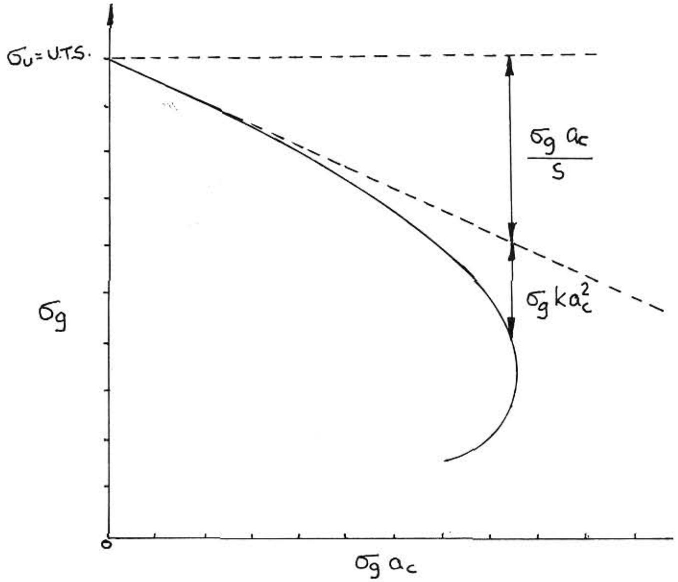
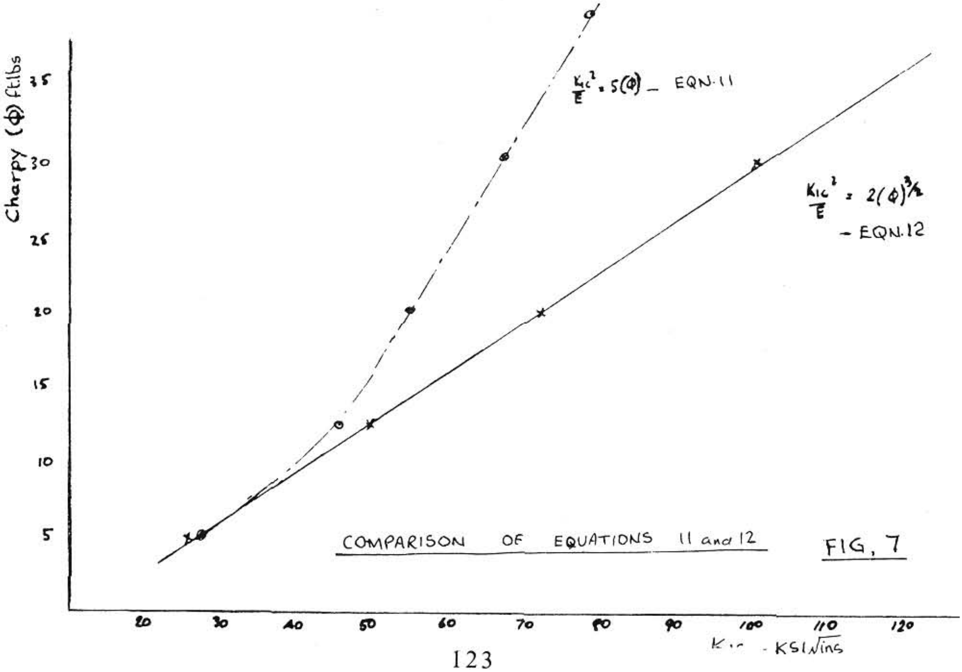


FIG. 6

GRAPHICAL REPRESENTATION OF EQN $\sigma_u - \frac{\sigma_g a_c}{S} - \sigma_g k a_c = \sigma_g$
SHOWING 'DROOP' DUE TO BULGING STRESS $\sigma_g k a_c^2$
CAUSED BY CYLINDRICAL GEOMETRY



COMPARISON OF EQUATIONS 11 and 12

FIG. 7