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Partial safety factors for SINTAP procedure

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PARTIAL SAFETY FACTORS FOR THE SINTAP PROCEDURE

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SUMMARY

This Report describes the background to the determination of partial safety factors to achieve chosen target reliability levels for structural integrity applications where the modes of failure are fracture and plastic collapse. The position on use of partial safety factors in existing Codes is reviewed together with evidence on variability of input data relevant to structural integrity assessments. Calculations have then been carried out leading to recommendations for partial safety factors for use in structural integrity assessments for given target reliabilities and data uncertainties. These calculations have been based on the assumption that failure occurs on the PD6493/BS7910 Level 2/3 failure assessment curve. In the final stage of the project the effects of modelling uncertainties on partial safety factors have been assessed based on experimental results of wide plate tests compared to the standard failure assessment curve.

CONTENTS

1. INTRODUCTION

Within the context of the overall SINTAP project, which aims to provide a unified structural integrity evaluation method applicable across a range of European industries, Task 3.5 aims to provide a useable probabilistic approach which gives quantified answers and methods of incorporating safety factors in structural assessments.

For general structural integrity assessment procedures aimed at fracture/plastic collapse the methods to be adopted will be based on well established fracture mechanics principles. The work of the Structural Assessment Group at UMIST over the past few years has included contributions to developments in application of fracture mechanics per se and the development of a first order second moment computer program based on including distributed variables/data in fracture mechanics assessment procedures. This program (UMFRAP) can determine the reliability index β for different combinations of type of distribution and mean and standard deviation values of the distributions including distributions for defects sizes, applied and residual stresses, fracture toughness, yield strengths, and fatigue crack growth parameters. The programme can be adapted to determine partial safety factors on the main input variables, stress, defect size, fracture toughness and yield strength, to achieve the required target reliability index and hence target probability of failure.

1.1 OBJECTIVES

The overall objectives of the work carried out are as follows:

- to review available data on distributions for input data in structural integrity assessments
- to determine partial safety factors on representative values of input data to achieve given target reliability index figures

2. GENERAL BACKGROUND TO RELIABILITY ANALYSIS AND PARTIAL FACTORS

For general structural assessment purposes it is standard practice to assess safety by a comparison of load and resistance effects using an established design relationship able to predict failure. When there are uncertainties in the input variables, or scatter in the materials data, reliability analysis methods can be employed to determine the probability of failure, i.e. the probability that the load effects will exceed the resistance effects. One convenient method to estimate probability of failure is the first order second moment method (FOSM) where the reliability index β is estimated by an iterative numerical procedure as the minimum distance from the origin to the failure surface expressed in normalised terms of all the variables involved. The failure equation is written in terms of load and resistance effects with the various input variables grouped together appropriately. For normally distributed load and resistance parameters, with means μ_L and μ_R, and standard deviations s_L and s_R respectively, the reliability index β is given by:

$$\beta = \frac{\mu_R - \mu_L}{\sqrt{s_R^2 + s_L^2}}$$

Characteristic values are often taken to represent upper bounds for distributions of load effects and lower bounds for distributions of resistance effects as follows:

$$C_L = \mu_L + n_L . s_L \ , \qquad C_R = \mu_R - n_R . s_R$$

where n_L and n_R are the number of standard deviations above or below the relevant mean values of the distributions chosen to represent characteristic values.

The design point, which is where the probability of failure is greatest, is given by the following expressions, where L_d and R_d represent the design point on the load and resistance distribution curves respectively and are at the same position.

$$L_d = \mu_L + \frac{s_L}{\sqrt{s_R^2 + s_L^2}} . \beta . s_L \ , \qquad R_d = \mu_R - \frac{s_R}{\sqrt{s_R^2 + s_L^2}} . \beta . s_R$$

To avoid having to solve these equations, an arbitrary division can be made for positioning the design point with respect to the means of the distributions which will always give a safe estimate, such that R_d is always just above L_d , for example:

$$L_d = \mu_L + \alpha_L . \beta . s_L \ , \qquad R_d = \mu_R - \alpha_R . \beta . s_R$$

where α_L and α_R are coefficients which strictly depend on the ratio s_L/s_R but typically lie in the range 0.7 to 0.8. A simplification often adopted is to take α_L as 0.7 and α_R as 0.8 as this gives safe estimates without having to solve the equations for different sets of input data.

Partial safety factors are factors which can be applied to the individual input variables in a design equation to give the given target reliability without having to carry out the probabilistic calculations. In effect the overall partial safety factor for load effects is the ratio of the design point value to the value assumed to represent the loading, and the overall partial safety factor on resistance effects is the ratio of the value chosen to represent resistance effects to the design point value.

Thus for load effects, $(\mu_L + n_L . s_L) . \gamma_L = L_d$

where n_L is the number of standard deviations above the mean for the value assumed to represent the loading and γ_L is the overall loading partial safety factor.

For resistance effects, $\frac{1}{\gamma_R}(\mu_R - n_R . s_R) = R_d$

where n_R is the number of standard deviations below the mean for the values assumed to represent the resistance and γ_R is the overall resistance partial safety factor.

If the second set of equations above for load and resistance design points are assumed to represent the same point (a conservative assumption) and it is noted that the ratio of the standard deviation to the mean is called the coefficient of variation, (COV), the following expressions for the overall load and resistance partial safety factors can be derived.

$$\gamma_L = \frac{0.7.\beta.(COV)_L + 1}{n_L.(COV)_L + 1}, \qquad \gamma_R = \frac{1 - n_R.(COV)_R}{1 - 0.8.\beta.(COV)_R}$$

It can be seen that the values of these partial safety factors depend on the target reliability, β, the (COV)s and the number of standard deviations from the mean taken to represent the load and resistance distributions. The relationship between partial safety factors and COV values for selected values of the reliability index β are shown in Figures 1 and 2 for load and resistance effects respectively for values of n_L of 0 and n_R of 1. It can be seen that the partial safety factors for load effects increase linearly with COV and β values whilst those for resistance effects increase non linearly and do not converge for higher values of COV or β.

Now that the overall safety factor has been separated into load and resistance effects, it is possible to divide up the separate γ_L and γ_R values into separate partial factors on different individual input parameters in the design equation for the particular mode of failure.

In the case of failure by fracture or plastic collapse the failure equation can be written as follows:

$$K_I - (K_r . K_{mat} - \rho) = 0$$

where K_I is the applied stress intensity factor (and represents loading effects), K_r is the permitted value of the fracture ratio in the R6/PD6493 assessment diagram approach given by the expression below, K_{mat} is the material fracture toughness and ρ is the plasticity interaction factor for primary and secondary stresses ($K_r.K_{mat}$-ρ represents resistance effects).

$$K_r = (1 - 0.14\, L_r^2)(0.3 + 0.7 \exp\{-0.66\, L_r^6\})$$

where L_r is the ratio of applied load to yield collapse load for the cracked structure.

The relationship between partial safety factors for load effects and resistance effects, characteristic values and the design point is shown in Figure 3. It should be emphasised that these general explanations are presented to assist understanding of the general principles of partial safety factors and their relationship to target reliability and variability/uncertainty of data. The situation is more complicated if the data are not normally distributed and where the load and resistance expressions themselves are functions of multiple variables. In these cases it is much more convenient to make use of specially written computer software, such as the UMIST program UMFRAP.

3. EXISTING CODE PROVISIONS FOR PARTIAL FACTORS

The only Code giving recommendations for partial safety factors in connection with fracture based structural integrity assessments is the BSI Document PD 6493:1991. These recommendations were derived as part of work by Glasgow University/UMIST on defect assessment methodology for offshore structures in the late 1980s and were included as part of an optional appendix in PD6493. For general purposes, most users of PD6493 have tended to use upper bound estimates of loads and defect sizes and to use the lowest of three results for fracture toughness without using partial safety factors, so that there is no real knowledge of the margin of safety. The partial safety factor recommendations are reproduced at Table 1 for comparison purposes with those derived in the present work. The procedure in use at the time involved the then Level 2 assessment curve based on the flow stress parameter S_r and in view of this and of developments in EuroCodes described below it is appropriate that a reassessment of partial safety factors should be carried out.

Limit state design codes have been in use in the UK and some European countries for some years. These will be superseded in due course by EuroCodes although individual member States have the right to place their own values for certain requirements where guidance is given in the EuroCode by 'boxed' numbers. This situation applies to guidance on partial safety factors in EuroCodes. The general value adopted in EuroCodes for the target reliability index β is 3.8 for ultimate limit state conditions in structures for which failure would have major consequences, corresponding to a failure probability of about 7 x 10^{-5}.

The most relevant of the EuroCodes to the SINTAP work is EuroCode 3 which is for steel structures and was published in 1993 although it is not yet in widespread use. EuroCode 3 gives conventional guidance on design of steel structures to avoid failure by plastic collapse and by buckling. As far as fracture is concerned the guidance is given in the form of material selection requirements based on the Charpy V notch impact test for different grades of steel and thicknesses at different minimum temperatures. These requirements are based on an underlying fracture mechanics and reliability analysis but this has been the subject of much controversy and is not considered satisfactory by some authorities including the present lead author. An alternative fracture mechanics based approach has been developed in the UMIST research group as given in Appendix 1 and this has been used in a joint research project by UMIST and TWI to propose revised requirements for assessing bridge steel requirements in the UK. This procedure was tuned to give results comparable to existing UK requirements for bridge steels using a probability of 0.4 in the Wallin correlation between Charpy and fracture toughness tests but taking all partial safety factors as 1.0 because a full probabilistic treatment would require complex assessments of the probability of maximum loading occurring at minimum temperature. These requirements are not linked to a particular target reliability but to general experience.

The general treatments of EuroCode 3 deal with uncertainties in loading and material properties for the failure modes of plastic collapse and buckling. To account for the fact that there is more uncertainty about variable (live) loads than for permanent (fixed or dead) loads, partial factors for variable loads are given as 1.5, those for permanent loads as 1.35 applied to best estimates (mean values) of loading. Because the probability of accidental loading is much less than that for normal design loadings, the partial factors for accidental loads are given as 1.05 for UK applications of EC3 (general EC3 values 1.0). Since these factors have been derived to deal with the appropriate uncertainties in loading for plastic collapse failure with a target reliability index of 3.8 it would seem sensible to adopt the same partial factors for fracture/plastic collapse failure to ensure consistency with existing procedures.

The resistance partial factors on material yield strength γ_M are given as 1.05 for UK applications of EC3 (general EC3 values 1.1), applied to characteristic values of material strength, i.e. mean minus 2 standard deviations.

4. REVIEW OF AVAILABLE DATA DISTRIBUTIONS

Rodrigues et al (1) stated that any model that predicts structural reliability will inherit certain assumptions about load spectra, environmental effects, failure mechanism and material properties. The creditability and usefulness of any particular model is a function of how closely these assumptions conform to reality.

4.1 Yield Strength Distribution

Becher et al (2) fitted a Weibull distribution to the frequency distribution curves of the yield strength and the charpy -V toughness data, published from the HSSST program on pressure vessel steel plate A533. The Weibull distribution was chosen in order to allow a specific lower limit of the material properties to be introduced.

The data used to obtain the distribution to be used for yield stress, in the analysis presented in this report, was obtained from the Health and Safety executive materials database. The data have been analysed using a statistical analysis software SPSS. The data consisted of different grades of steel, namely A, A352, A36, A517Q, AB, AH32, AH36, 43A, 43C, 450, 50A, 50D, 50DD, 50DM, 50DZ, 50E, 50EM, SMU70 with different sample sizes. The data was fitted to Normal, Log-normal and Weibull distribution, from the results obtained it was found that the log-normal distribution gave a better fit for most of the cases considered and so this was adopted for all the analyses carried out to obtain the partial safety factors using a mean value of 350 N/mm^2. The analysis of all the data provided for yield strength showed a typical cov of 0.08 and for calculation of the partial safety factors a cov of 0.1 has been assumed.

4.2 Fracture Toughness Distribution

In order to obtain the type of distribution for fracture toughness, Normal, Log-normal and Weibull distributions were fitted to the data obtained from the Health and Safety Executive materials database. The data have been analysed using the statistical analysis software SPSS. The data consisted of CTOD values on welded joints at different temperatures namely -20, -10, 0. The cases examined were those for fracture tests at the fusion line. They were divided further to as welded, stress relieved, through thickness and surface orientation. From the results obtained, it was found that the Weibull distribution gave a better fit for the data and so this was adopted for the analysis with two different cov values of 0.2 and 0.3.

4.3 Defect Size Distribution

For any probabilistic failure analysis to be credible it is necessary to have a good estimate of the flaw size and distribution. It is also necessary that any function used to describe the defect size distribution should be as accurate as possible at the tail of the distribution. The goodness of fit is not critical for small defect sizes as such defects are not important in determining the probability of failure and they are likely to be more subjective to measurement errors(reliability of the NDT) than that of large defects. For a fixed steel platform the important flaw size distribution is the distribution of defect height in welds in node connection Rogerson and Wong (3).

Burdekin and Townend (4) analysed ultrasonic NDT records to determine the distribution of defect heights (lack of fusion and lack of penetration). The records were 100% ultrasonic NDT of 1.9 km of manual metal arc T-butt welds at tubular node intersection in a series of jacket structure in BS 4360 grade 50D steel. For this data a Weibull distribution was found to give a moderately good fit with a cumulative probability of values equal to or less than size z mm given by

$$F_z(z) = 1 - \exp\left[\left(-\frac{z}{7.38}\right)^{2.39}\right]$$

The full results can be found in Townend (5), a summary of which is produced in this report as Table2. I n general the Weibull density function is defined as

$$f(x, q, a, b) = \frac{b}{a}\left\{\frac{x - q}{a}\right\}^{b-1} \exp - \left\{\frac{- x - q}{a}\right\}^{b}$$

where q = location or threshold parameter.
a = scale parameter.
b = shape parameter.

Rogerson and Wong (3) carried out a detailed analysis of embedded weld defects namely defect height for one particular structure in the North Sea, the survey include about 1000 m of welding for 12 vertical node joints and 6 horizontal node joints in the important splash zone region of the structure. The data was from Magnetic Particle Inspection and Ultrasonic Inspection and was divided into planar and non-planar defects. The defect rate before final NDT and repair was 0.7/m (i.e. 7 defects per 10 m of weld) this figure was reduced to 0.2/m after final NDT and repair. The Weibull distribution gave a good fit for both planar and non planar defects height particularly at the extreme values with the following parameters $b = 0.8, h = 1.12, g = 1.0$.

Table 3 gives the Parameters for the Weibull distribution,

$$n(x) = \frac{b}{h}\left[\frac{x - g}{h}\right]^{b-1} \exp\left[-\left(\frac{x - g}{h}\right)^{b}\right]$$

Where n(x) is the probability density function for defect size.

Rodrigues et al (1) presented analysis of data obtained from node welds in four different North Sea structures fabricated by different European fabricators. Another set of data was obtained from the results of a separate NDT inspection on one of the above mentioned four structures, there was a wide scatter in the number of defects per meter. The data was from ultrasonic testing of 250 m of welding. Exponential and Weibull distributions were fitted to the defect length. However the Weibull distribution was considered to be a better fit for the data than the exponential one, as it can describe all the data by the same function type which gives higher estimates of the large defects than the exponential and hence more conservative.

Becher and Pedersen (2) suggested an exponential distribution for crack depth in pressure vessels when they are taken into operation. This was chosen on the basis that small cracks between 1-2 mm almost certainly exist and the probability of not detecting bigger cracks decreases sharply with the crack size. The function chosen was,

$$f(a) = 2.56 \exp(- 2.56(a - 0.1))$$

where (a) is the crack depth in inches.

Dufresne (6) analysed NDE results (UT or X ray) obtained from 3 European nuclear pressure vessel manufacture before repair. The data on defect length shows that the number of defects per weld varies between 0 -50. It was also found that the log normal distribution is a good approximation for defects larger than 20 mm, the distribution for defect depth was not specified.

Marshall (7) suggested that the mean number of cracks per section in PWR pressure vessel having a depth in the range (a) to (a+da) at the start of service to be

$$N_0(a) = A(a)B(a)$$

Where A(a) is the size distribution of cracks resulting from manufacture and B(a) is the probability of not detecting such cracks in the course of the subsequent pre-service inspections. Marshall found that there are 3.65 defects of all sizes per vessel. In his second report he suggested a function for A(a) mentioned above as

$$A(x) = A \exp(-\,l x)$$

where A = 0.57

$\lambda = 0.16mm^{-1}$, and x is the through wall extent of the defect.

Kountouris and Baker (8) analysed data of surface breaking defects found by magnetic particle inspection in the lower Hull of the Conoco Tension Leg Platform. The length of the weld inspected was of the order of 50 km. Weibull and log-normal distribution have been fitted to the data with an explanation of the variations in defect sizes under the influence of defect location, defect type, weld type, positions in the structure, fabrication and welding process. Analysis was undertaken on defect length, depth and aspect ratio. The results found indicated that the Weibull distribution gave on average a better fit than the log normal distribution.

Kountouris and Baker (9) also analysed embedded defects found by ultrasonic testing in the welds of the lower Hull of the Conoco Tension Leg Platform. The weld length was of the order of 27 km. The work involved a study on the frequency of occurrence of various defect types under the influence of a number of parameters such as global position in the structure, original welding procedure, type of welded joint and defect location within the joint. Weibull and log-normal distribution were fitted to the data (defect length and height).

The R6 document (10) gave three types of distributions for the crack depth, these are:

(1) An extreme value distribution which was derived from data provided by extensive ultrasonic examination of Magnox reactor components. The probability density function for the largest defect depth observed in a sample of one litre of weld metal was described by the Gumbel type II extreme value function given by

Where a is the defect depth, A and B are constants.

$$f(a) = A^B B a^{(-B+1)} \exp - \left[\left(\frac{a}{A} \right)^{-B} \right]$$

Defect length was taken into account by considering the distribution of the aspect ratio Z with a Weibull probability density function g(Z) given by

$$g(Z) = g\left(\frac{l}{2a} \right) = \frac{b}{q} \left(\frac{Z-h}{q} \right)^{b-1} \exp - \left\{ \left(\frac{Z-h}{q} \right)^{b} \right\}$$

Where β, η and θ are constants

(2) An exponential distribution with the probability density function f(a) given by:

$$f(a) = R \quad \exp\{(-g \;\; a)\}$$

where 1/γ is the mean defect depth and R/γ is the mean number of defects per weld.

(3) A normal distribution for the uncertainty for a single defect of the form :

$$f(a) = \frac{1}{s\sqrt{2p}} \exp\left\{ -\frac{1}{2}\left(\frac{m-a}{s} \right) \right\}$$

where μ is the mean depth and σ is the standard error.

The disadvantage of using an extreme value distribution is that some combinations of defect size other than the extreme one and material properties could lead to a worse case than the one obtained using the extreme value. This was investigated by Muhammed (10) for the effect of sample size together with variations in toughness data on the probability of failure. The results obtained show that as the sample size increases and as the variability in the toughness of the material increases as well, the results become less conservative. This was explained by the fact that as the incidence rate increases, defects other than the largest one become critical as the chance of such defects occurring in poor toughness materials increases.

Muhammed (11) fitted Weibull, Log-normal and Exponential distributions to fabrication defects obtained from the fabrication inspection records of two offshore steel jackets in the North Sea fabricated in 1986 and 1991. The data consisted of the results of Magnetic Particle Inspection, Ultrasonic and Radiographic testing of some 3800 welds of total length of about 9.35 km. The data was divided into reportable and repairable defects and further into pre-repair and post repair categories. It was also separated according to inspection methods and planar or non- planar defects.

From the results obtained for defect depth and length, the Weibull distribution gave a better fit for most of the cases considered but in some cases there was little difference between the three distributions which suggested the need to assess the impact of choosing one or other of the distributions.

Cases occur however when defects arise from specific non random causes and distributions for such cases have to be considered separately. In particular large weld defects only arise from specific combinations of faults in materials or procedures and cannot be considered as an extrapolation to distributions of other defect sizes. This is an area of active research work to model the factors which contribute to the occurrence of large defect sizes.

In the present work the aim is to produce recommendations for partial safety factors in the assessment of single defects. Thus the question of distribution of defect sizes is of less importance than the question of errors in defect sizing and detection. The overall distribution of defect sizes is important for full probabilistic fracture mechanics analyses applied to structures.

4.4 Probability of Detection

As no non destructive testing device is able to detect all defects in the structural weld examined, when NDT is carried out on a welded structure, there is a chance that unacceptable defect will be allowed to remain in the component following inspection and repair. The probability of this occurring depends on many parameters, such as fabrication techniques, NDT methods and requirements, reliability of the methods and the operators. Wamuziri (12) suggested that it will be sufficient for cracks to consider only one parameter such as length for through thickness crack or effective through wall extent for part thickness crack.

Harris (13) pointed out that the probability of not detecting a surface crack will decrease as the crack size is increased and it will be nearly unity for small cracks whereas it asymptotically approaches zero with increase in the crack size. However it will never be equal to zero as there will not be absolute certainty that a crack will be detected, the same previous mentioned concepts were suggested by Tang (14).

Harris (13) used the data reported by Tang (14) for not detecting a crack of size (a). The data reported was in the range $a \le 2.5\text{mm}$. He looked at surface defects, because for a given crack size and stress level surface cracks have a substantial higher stress intensity factor than subsurface defects and hence they will grow much faster and are more likely to result in a structural failure. Three functions were fitted to the data namely,

Log-normal $P_{ND}(a) = \frac{1}{2}\,\text{erfc}\left(n \ln {}^{a}\!/_{a^*}\right)$

Weibull $P_{ND}(a) = e^{-\left({}^{a}\!/_{g}\right)^{z}}$

$a^* = g\,(\ln 2)^{1/z}$

Gamma $P_{ND}(a) = e^{-ma} \sum_{n=0}^{k-1} \left(\frac{ma}{n!}\right)^{n}$

$P_{ND}(a)$ is the probability of not detection a crack of size (a) in inches
erfc is the complementary error function

where ν, μ, γ, ζ, a* are the parameters to be adjusted to give agreement of $P_{ND}(a)$ with the experimental data. a* is the crack that has 50% chance of being detected. Although the three models fitted the data well when considering crack depths only in the range covered by the data, the behaviour of the models differ markedly at larger values of a. Harris suggested the use of the log-normal distribution with ν = 1.33 and a* = 1.6mm. The same log-normal equation was modified by Harris and Lim (15) when applied to reactor piping to the following equation,

$$P_{ND}(A) = e + \frac{1}{2}(1-e)\,\text{erfc}\left(n \ln {}^{A}\!/_{A^*}\right)$$

to account for the two dimensional nature of the cracks. Here A* is the crack area which has 50% detection probability and e is the non detection for very deep cracks and n is a parameter which controls the slope of the $P_{ND}(a)$ line.

Marshall (16) gave a function for B(x) with parameters estimated from the answers to a questionnaire sent to 20 qualified operators. The answer were purely subjective options on the probability of detection of planar defects of various heights. The function was

$$B(x) = e + (1-e)\exp(-mx)$$

where e = 0.005, m = 0.16 mm^{-1} and B(x) is the probability of not detecting a defect of depth x.

Becher and Pederson (2) reported in their work on pressure vessels that the probability that a crack with a depth of 2 cm will not be detected is 10 %.

O`Neill and Jordan (17) suggested that the probability of missing a potential dangerous defect by ultrasonic testing is somewhere between 1 and 10^{-2}.

Townend (5) modelled and compared the performance of the PISC and the PVRC programms by minimizing the least squares errors. The best fit for the PISC data was found to be

$$P(z) = 1 - \exp - (z / 57)$$

and for the PVRC data the least square fit was found to be

$$P(z) = 1 - \exp - (z / 6.3)$$

where P(Z) is the probability of detecting a defect of height z.

Townend (4,5) also stated that to account for the effects of the defect detection function in the estimation of the defects in the structure prior to NDT and repair, a discrete approximation can be applied. He demonstrated this by the following example, if 20 defects have been detected in the 10 mm to 20 mm size range and the detection function gives a mean probability of detection for this size of 0.8 then the expected actual number in this range would be 25 defects.

Dufresne (6) stated that the longer the defect, the greater the number of crossings of the ultrasonic transducer and hence the risk of missing a defect is reduced, which was also restated by Marshal (7), such that

$$P_n = 1 - (1 - P_1)^{2b/f}$$

where P_1 is the probability to detect a flaw at each crossing. P_n is the probability to detect n crossing along a defect and 2b is the defect length and f is the diameter of the beam. The beam diameter was taken as 40 mm and 50% probability of detection to correspond to 9 mm length of defect. The same concept was stated by Silk (18) which was demonstrated in the UKAEA Defect Detection Trials.

$$B(x) = 0.005 + 5.53e^{-0.37x}$$

Cameron and Temple (19) modelled the same concept to obtain a function B(x)which takes account of the length of the defect and the ultrasonic scan pitch. The results for a scan pitch

$$B(x) = 0.001 + 10.3e^{-0.57x}$$

of 15 mm or less for a through wall extent defect measured in millimeters for a defect aspect ratio of 1/3 is given by:and for a defect aspect ratio of 1/10, it is given by provided x is less than an acceptable defect size.

Temple (20) gave a model which includes many variables, several inspection techniques Q and human variability. It is given by

$$B_Q = \frac{1 + \exp\left[-\sum_i a_i^{*}\right]}{1 + \exp\left[\sum_i a_i \, (x - x^{*}_i)\right]}$$

where there are i parameters in x. The parameter α_i determine the sharpness of the boundary between acceptance and rejection, the parameters x_i* are the values at which the probability of correct rejection reaches 0.5 if there was only that parameter present.

Berens and Hovey (21) suggested a log odds model for a chance of leaving a defect in the vessel of the form

$$B(x) = \frac{1}{1 + \exp(a + b \ln x)}$$

where x is the defect and α, β are constants.

Again these aspects are important in overall probabilistic fracture mechanics assessments of a structure, but do not influence partial safety factors used in the effect of a single defect on structural integrity.

4.5 Defect Sizing Errors

The measurement of defect dimensions by a non - destructive examination device requires skill and is governed by a large number of uncertainties and limitations. Measurements for accuracy of sizing are valid only for particular categories of defect and method. However, information about the tendency to undersize or oversize defects can be obtained by examining trends between measured and actual sizes or means and standard deviations of sizing errors (measured size - actual size), when data are available.

Sizing errors can have serious consequences since, if significant defects are overestimated, this could result in unnecessary repairs causing financial losses and possibly lead to the introduction of further defects by the repair process. Conversely if significant defects are overlooked this could have serious effects on overall structural integrity.

Based on surface cracks in a weld detected by an ultrasonic device on aluminum specimens, Tang (14) suggested the following calibration curve between actual and measured crack depth.

$$E\left(C_a | C_m\right) = 0.0592 + 0.839 C_m \text{ for } 0 < C_m \leq 0.2 \text{in}$$

This is a normal random variable with zero mean and 0.0342 inches standard deviation. The specimens were precracked in fatigue to produce a small surface “thumbnail” crack, they were inspected by NDT to determine the sizes and locations of the flaws, then tested to failure in a uniaxial tension test. The failed surface was examined to determine the actual size of flaws.

Townend (5) looked at data of lengths of rail containing defects that had been sized ultrasonically before being removed from track. The actual defects were made visible by breaking the length of rail open on a large hydraulic press and then the actual defects were measured using a steel rule. The data was then tested for correlation between ultrasonic sizing error and defect height in mm and linear regression analysis of the data gave a least square line of sizing error versus actual defect size as,

$$\text{Error} = 13.86 - 0.54 \text{ actual size}$$

Errors about this line were normally distributed with a mean of 7.2 mm and a standard deviation of 6.28 mm. It was found that small defects were overestimated in size whereas large defects were underestimated in size, the cross over from overestimating to underestimating occurred at 25.67mm.

Baker (22) analysed data on fatigue cracks from four typical tubular welded joints published by the Department of Energy. All cracked specimens were detected by magnetic particle inspection and subsequently sized using manual ultrasonic and other techniques, The true dimension of the crack were evaluated by destructive sectioning. It was found that for both crack depth and length there is one measurement which deviates significantly from the rest of the data. A linear regression analysis of the measured dimensions and the true dimension was performed on all four cases (including and excluding the extreme observations).

It was found from the results that for the case of defect depth in the two cases (including and excluding the extreme observation) sizing errors are dependent on the actual defect size. Defects of depth up to 7 mm were found to be overestimated whereas defects greater than this value to be underestimated. For the defect length, the reliable case was found to be the one which excludes the extreme observation and in this case the defects were usually overestimated.

The general position on sizing of defects is that only ultrasonic testing is capable of detecting and sizing part thickness defects satisfactorily for structural integrity assessments. Best practice techniques using time of flight methods are capable of achieving a standard deviation better than 2 mm and the best of pulse echo methods may also approach this. Standard commercial testing without qualified procedures may only achieve a standard deviation of 6-8 mm however.

4.6 Loading

As indicated above there are existing recommendations in various codes for partial safety factors on loading. Since these are aimed at allowing for effects of uncertainties on failure by plastic collapse it has been decided to adopt existing EuroCode 3 recommendations in the present work.

5. DERIVATION OF RECOMMENDATIONS FOR PARTIAL SAFETY FACTORS FOR SINTAP

On the basis of the reviews described above it was decided to produce recommendations for partial safety factors to cover different requirements for target reliability and different degrees of variability of the input data. The target reliability levels chosen corresponded to those used previously in PD 6493 with the addition of the standard level adopted in EuroCode 3 and an additional very high reliability level representative of very high structural integrity requirements such as for the nuclear industry, namely 2.3 x 10^{-1}, 10^{-3}, 7 x 10^{-5}, 10^{-5} and 10^{-7}

The input variables considered for these assessments were stress levels, defect size, fracture toughness and yield strength. For stress levels it was decided to consider coefficients of variation of 0.1, 0.2 and 0.3 with a normal distribution and to regard a COV of 0.2 as representing dead load or residual stress effects and a COV of 0.3 as representing live load effects. The recommendations are intended to cover for the assessment of a single defect rather than a full distribution of defect sizes. For defect sizes the evidence appears to be that the uncertainties arise primarily in location of the edges of the defect so that the uncertainties are better expressed as a normal distribution with a standard deviation dependent on the technique used. For best practice ultrasonic testing it is possible to achieve a standard deviation of about 2 mm whilst commercial practice might not achieve better than a standard deviation of 6-8 mm. For the purposes of determining partial safety factors the results are derived in terms of different COV values so that for application purposes it is necessary to know both the best estimate (mean) value of defect size and the standard deviation to determine the appropriate COV. On the basis of consideration of the data bases of material properties provided it was decided to adopt Weibull and Lognormal distributions for fracture toughness data with coefficients of variation of 0.2 and 0.3 and a Lognormal distribution for yield strength with a coefficient of variation of 0.10

Initial calculations were carried out on the basis of the general principles outlined above for determining overall partial safety factors for load effects and resistance effects, treating load effects as the applied stress intensity factor K_I and resistance effects as (K_r K_{mat} -ρ). These were based on α_L and α_R values of 0.7 and 0.8 respectively to be consistent with procedures used in EuroCode 3. Trials were then carried out using the UMIST programme UMFRAP to determine appropriate combinations of partial safety factors for stress, defect size, fracture toughness and yield strength for a range of structural integrity assessments representing different positions around the PD 6493/BS7910 failure assessment diagram, i.e. across the range of different L_r values. Mean fracture toughness values of 3000, 1500 and 800 N-mm$^{-3/2}$ were used with a Weibull distribution and COVs of 0.2 and 0.3, with a mean surface defect size of 3 and 10 mm having a normal distribution with COVs of 0.1 and 0.2 in thicknesses of 20, 50 and 100 mm. The yield strength was taken as lognormal with a mean strength of 350 N/mm^2. Further calculations were carried out with through thickness defect sizes of 50 and 100 mm. In each case the applied stress level was calculated deterministically as the value which would cause failure according to the PD6493/BS7910 failure assessment curve at the mean values of the input variables. The calculations were carried out to determine partial safety factors for target reliability index values of β = 0.739, 3.09, 3.8, 4.27 and 5.2.

It is important to recognise that there is no unique solution for partial safety factors. Even when a preliminary separation is made into load and resistance groups there are still many alternative combinations of partial factors which could be applied to the separate input variables to give the same required target reliability. The most appropriate solutions are those for which the partial safety factors remain approximately constant over a wide range of input values. The ratios between the different factors should be primarily dependent on the relative COVs of the input data but as noted below it was found that there was some effect of the absolute values of some input variables.

When the initial estimates of partial safety factors had been determined, further calculations were carried out over the full range of input variables considered to confirm that the required probability of failure was obtained when these partial factors were applied.

It was found from the wide range of cases analysed that the values of partial safety factors required for a given group of data increased with increasing thickness, with increasing size of through thickness crack and with reducing fracture toughness. An example of this is shown in Figure 4 and 5 for partial safety factors on crack size and stress against L_r for a fracture toughness value of 800 N-mm$^{-3/2}$ and three different thicknesses with target failure probabilities of $2.3x10^{-1}$, 10^{-3} and 10^{-5}. Values have therefore been selected to represent the case of high thickness and low toughness. Partial factors on yield strength will have little effect other than at high L_r values when plastic collapse is the dominant mechanism and hence it was decided to adopt the material factors already in use for EuroCode 3 on yield strength for consistency. For stress partial safety factors the values for $\beta = 3.8$ were chosen as 1.35 and 1.5 for stress covs of 0.2 and 0.3 to represent dead and live load respectively, and to be consistent with EuroCode 3. Other values of the stress partial factors were chosen to follow the general pattern of increasing with cov and with required reliability. The defect size partial safety factors were then determined to achieve the required target reliability values. For each of the data groups values of partial safety factors have been selected for each target reliability and COV to cover for all the other cases. The values recommended are given in Table 4. The partial safety factors on fracture toughness have been converted to be applied to mean minus one standard deviation values as an approximate estimate of lowest of three. It is recommended that sufficient fracture toughness tests should be carried out to enable the distribution and mean minus one standard deviation to be estimated satisfactorily. For yield strength the partial safety factor is applied to the characteristic (minimum specified) value whilst for the stress and defect size input parameters the partial safety factors are applied to the best estimate (mean) value. It must be recognised that the partial safety factors will not always give the exact target reliability indicated but should not give a probability of failure higher than the target value. The results of applying the partial safety factors to different combinations of covs are shown in Figures 4 and 5 where the calculated probability of failure is plotted against the target value. It can be seen that the recommended partial safety factors give ‘safe’ results for the target reliability over the whole range.

It is instructive to compare the values of the partial safety factors recommended in Table 4 with those derived previously for PD 6493:1991. The first important point is that the partial safety factors in PD 6493:1991 for fracture toughness and defect size are significantly lower than those recommended in the present work for target reliabilities of 10^{-3} and 10^{-5} On the other hand the partial safety factors on stress in PD 6493:1991 are somewhat higher than those now recommended. An important issue is that the present calibrations have been carried out assuming that failure occurs in accordance with the failure assessment diagram, whereas in practice it is often found that the diagram gives safe predictions rather than critical ones. The original PD 6493 1991 partial safety factors were calibrated against wide plate test results rather than the theoretical failure assessment curve and this may explain some of the differences.

The general pattern of the partial safety factors given in Table 4 follows expectations in that higher values are given for higher reliability and for higher variability. The values given are more comprehensive than previous recommendations.

6. MODELLING UNCERTAINTIES

The analyses and recommendations given above are based on the assumption that failure will occur when an assessed defect gives rise to a point which falls on the PD6493/BS7910 level 2/3 assessment diagram (based on L_r for plastic collapse at yield strength). There have been a number of research programmes carried out to validate the assessment diagram approach, mainly based on the results of wide plate tests. Data from four such test programmes, two at NIL (23) and two at TWI (24,25) have been used to investigate the effects of the conservatism inherent to the assessment diagram approach on partial safety factors. The NIL data involved nine tests with through thickness notches, six specimens with a central hole with symmetrical notches across a diameter three of which contained welds, and eight specimens with surface defects. The TWI data was in two parts, the first part consisting of fifteen biaxial tests on plates of thickness 25 to 50 mm and the second part sixteen tests from a series carried out by the Swedish Plant Inspectorate. It should be noted that these conservatisms may arise from a number of effects and before any benefit is taken in reducing partial safety factors it must be clearly established that the conditions of the service condition being assessed are similar to those for results of wide plate tests. Under these conditions the inherent conservatism may be considered as a modelling error.

The results of the different series of wide plate tests are shown in Figure 6. The data include results from wide plate tests under uniaxial and biaxial tension, and also tests on plates with through thickness and surface defects. It can be seen that the vast majority of the results give data points which lie outside the standard failure assessment curve with only two results falling inside the curve.

For analysis purposes the data was divided into three sets as follows:

(i) Data set 1 – the NIL results excluding the eight surface defect cases together with the TWI part 2 data.
(ii) Data set 2 – the TWI part 1 biaxial wide plate tests
(iii) Data set 3 – the eight NIL surface defect cases

For each of the data points in each of the data sets the reserve factor was calculated as the ratio of the distance from the origin of the FAD to the assessment point and the distance from the origin to the failure assessment curve. The results for each data set were fitted to a statistical distribution from which the probability of the reserve factor being less than one was calculated and from this the revised target probability of failure was determined. The revised probabilities of failure were then plotted against the partial safety factors obtained in the earlier part of the work as shown in Figures 7-9 for stress covs of 0.1, 0.2 and 0.3 respectively and for fracture toughness in Figures 10 and 11 for covs on K_{mat} of 0.1 and 0.2.

Including these modelling uncertainties in the calculations of partial factors leads to a modified set of factors where it is desired to remove these uncertainties and where they are known to be represented by conditions of the wide plate tests as shown in Table 5. Typically removal of the modelling uncertainty allows a reduction in the generally recommended partial factors of the order of 0.05 to 0.1 on stress, and 0.2 to 1.0 on fracture toughness.

7. CONCLUSIONS

New recommendations have been produced for partial safety factors for use in structural integrity assessments where the primary modes of failure are fracture and plastic collapse. The recommendations are more comprehensive than previously produced and have been designed to be compatible with EuroCode 3 the most relevant code for design of steel structures in futures. The new values are higher for fracture toughness and defect size than previous values but this is compensated to some extent by the new values being lower for stress levels. The effects of modelling uncertainties have also been examined and reductions in partial factors evaluated for applications where standard assessment procedures are used to predict service performance represented by wide plate test results.

ACKNOWLEDGEMENT

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Table 1
Partial factors recommended in BSI Document PD 6493:1991

	p(F) $2.3x10^{-1}$	p(F) 10^{-3}	p(F) 10^{-5}
Stress (COV 0.05)	1.1	1.4	2.0
Stress (COV 0.3)	1.2	1.6	2.4
Flaw size (SD 2 mm)	1.0	1.2	1.7
Flaw size (SD 10 mm)	1.1	1.4	1.8
Toughness (K, min of 3)	1.0	1.2	1.3
Toughness (δ, min of 3)	1.0	1.4	2.6

Table 2
Weibull Parameters for Defect Size Distribution obtained by Townend (5)

Defect Type	Distribution Parameters		
	Defect Dimension	*a*	*b*
Lack of fusion	Depth Length	7.00 3.41	2.18 2.14
Lack of penetration	Depth Length	8.13 361	2.76 3.65
Combined data	Depth Length	7.35 348	2.39 2.41

Table 3
Weibull Parameters for Defect Size Distribution from Rogerson and Wong (3)

Defect Type	Distribution Parameters		
	β	γ	η
Non-planar defects As fabricated	0.70	0.1	0.83
Planar defects As fabricated	1.4	0.1	5.0
All defects As fabricated	0.95	0.1	3.2
All defects after NDT and repairs	0.80	0.1	1.12

Table 4
Recommended partial factors for SINTAP project for different combinations of target reliability and variability of input data based on failure on the assessment curve

		p(F) $2.3x10^{-1}$	p(F) 10^{-3}	p(F) $7x10^{-5}$	P(F) 10^{-5}	p(F) 10^{-7}
		$\beta = 0.739$	$\beta = 3.09$	$\beta = 3.8$	$\beta = 4.27$	$\beta = 5.2$
Stress	$(COV)_\sigma$	γ_σ	γ_σ	γ_σ	γ_σ	γ_σ
Extreme	0.1	1.05	1.2	1.25	1.3	1.4
Dead+Res	0.2	1.1	1.25	1.35	1.4	1.55
Live	0.3	1.12	1.4	1.5	1.6	1.8
Flaw size	$(COV)_a$	γ_a	γ_a	γ_a	γ_a	γ_a
	0.1	1.0	1.4	1.5	1.7	2.1
	0.2	1.05	1.45	1.55	1.8	2.2
	0.3	1.08	1.5	1.65	1.9	2.3
	0.5	1.15	1.7	1.85	2.1	2.5
Toughness K	$(COV)_K$	γ_K	γ_K	γ_K	γ_K	γ_K
	0.1	1	1.3	1.5	1.7	2.0
(min of 3)	0.2	1	1.8	2.6	3.2	5.5
	0.3	1	2.85	NP	NP	NP
Toughness, δ	$(COV)_\delta$	γ_δ	γ_δ	γ_δ	γ_δ	γ_δ
	0.2	1	1.69	2.25	2.89	4.0
(min of 3)	0.4	1	3.2	6.75	10	30
	0.6	1	8	NP	NP	NP
Yield strength	$(COV)_M$	γ_M	γ_M	γ_M	γ_M	γ_M
(on min spec.)	0.1	1	1.05	1.1	1.2	1.5

Notes:
γ_σ is a multiplier to the mean stress of a normal distribution
γ_a is a multiplier to the mean flaw height of a normal distribution
γ_K or γ_δ are dividers to the mean minus one standard deviation value of fracture toughness of a Weibull distribution
γ_M is a divider to the mean minus two standard deviation value of yield strength of a log-normal distribution

Table 5
Recommended partial factors for SINTAP project for different combinations of target reliability and variability of input data including wide plate modelling uncertainties

		p(F) $2.3x10^{-1}$	p(F) 10^{-3}	p(F) $7x10^{-5}$	P(F) 10^{-5}	p(F) 10^{-7}
		β = 0.739	β = 3.09	β = 3.8	β = 4.27	β = 5.2
Stress	$(COV)_\sigma$	γ_σ	γ_σ	γ_σ	γ_σ	γ_σ
Extreme	0.1	1.05	1.14	1.20	1.25	1.36
Dead+Res	0.2	1.1	1.20	1.28	1.35	1.48
Live	0.3	1.12	1.30	1.41	1.50	1.72
Flaw size	$(COV)_a$	γ_a	γ_a	γ_a	γ_a	γ_a
	0.1	1.0	1.4	1.5	1.7	2.1
	0.2	1.05	1.45	1.55	1.8	2.2
	0.3	1.08	1.5	1.65	1.9	2.3
	0.5	1.15	1.7	1.85	2.1	2.5
Toughness K	$(COV)_K$	γ_K	γ_K	γ_K	γ_K	γ_K
	0.1	1	1.2	1.4	1.5	1.9
(min of 3)	0.2	1	1.5	2.2	2.6	4.1
	0.3	1	2.2	NP	NP	NP
Toughness, δ	$(COV)_\delta$	γ_δ	γ_δ	γ_δ	γ_δ	γ_δ
	0.2	1	1.44	1.96	2.25	3.61
(min of 3)	0.4	1	2.25	4.84	6.76	16.8
	0.6	1	4.84	NP	NP	NP
Yield strength	$(COV)_M$	γ_M	γ_M	γ_M	γ_M	γ_M
(on min spec.)	0.1	1	1.05	1.1	1.2	1.5

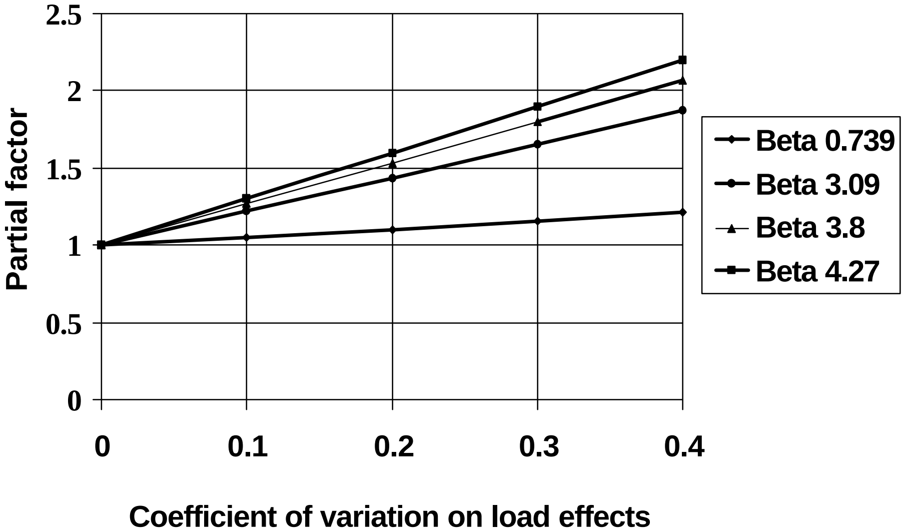


Figure 1
Effect of COV on partial factors for load effects

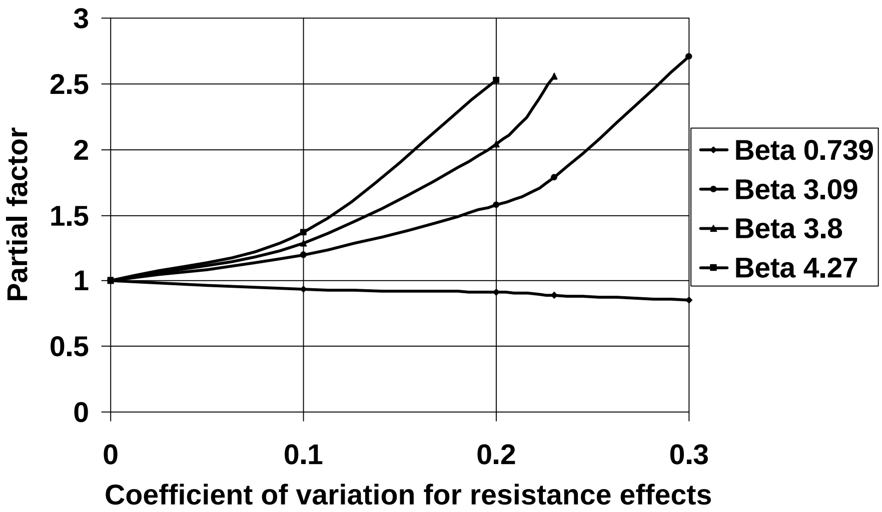


Figure 2
Effect of COV on partial factors for resistance effects

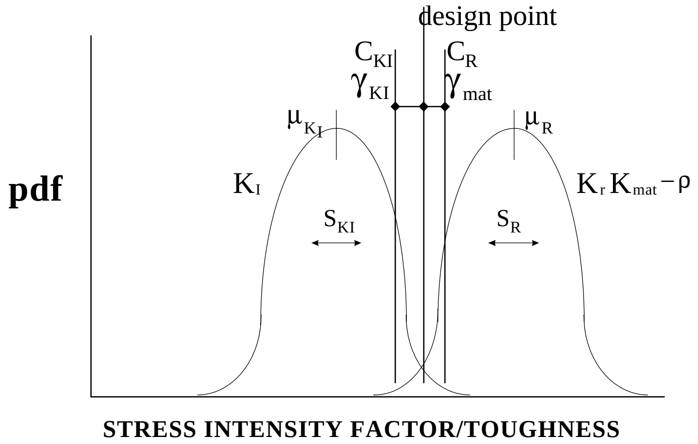


Figure 3
Reliability analysis and partial factors for fracture

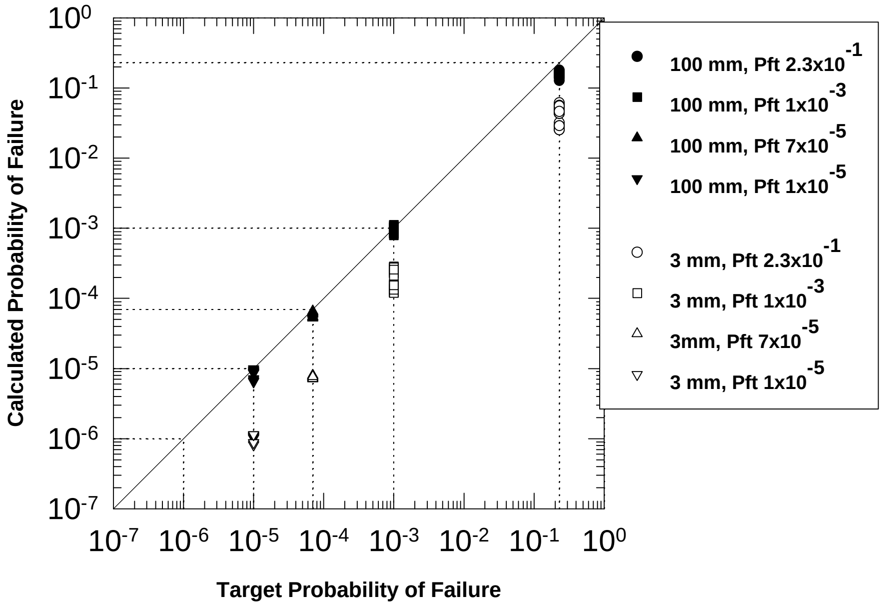
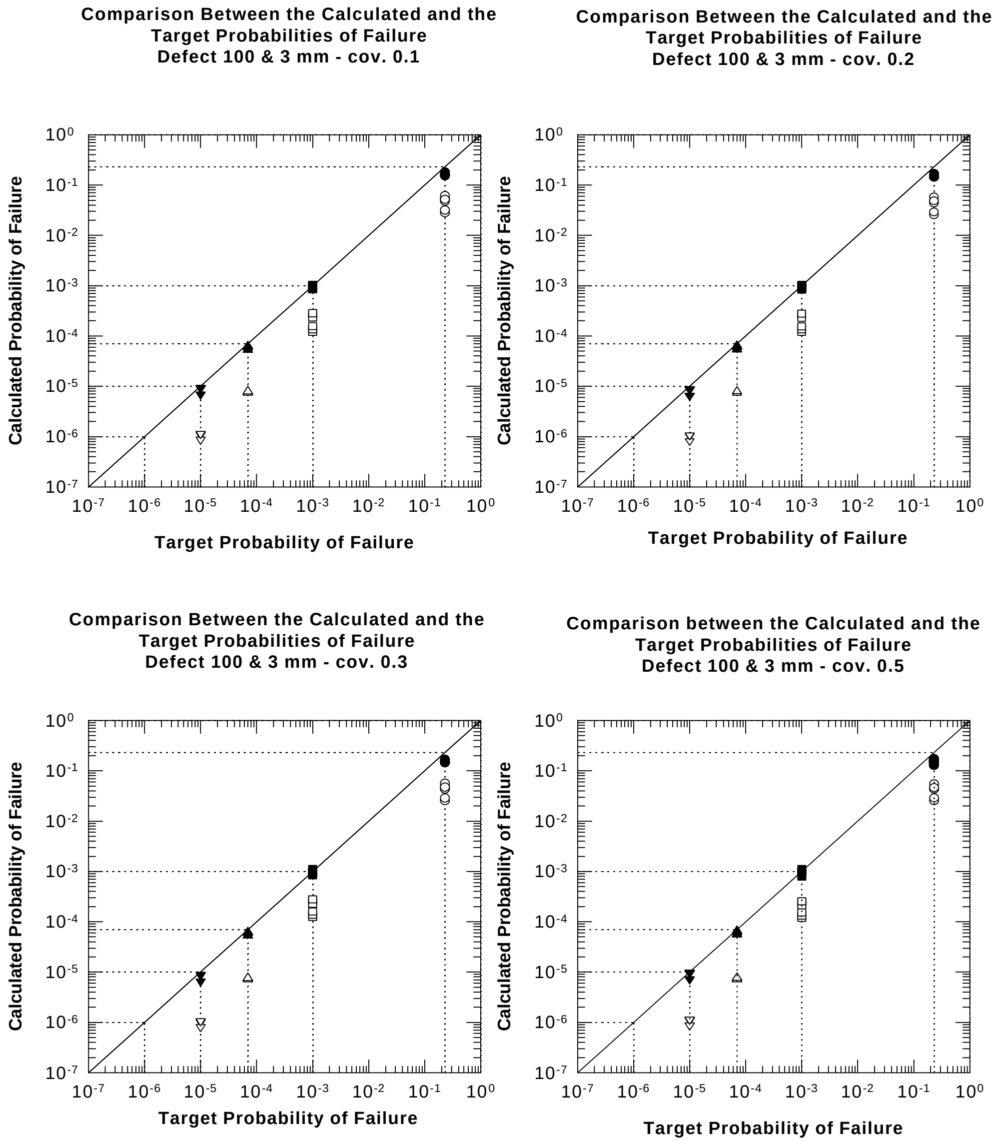


Figure 4
Comparison between the calculated and the target probabilities of failure defect 100 & 3mm covs. 0.1, 0.2, 0.3, 0.5



*** Solid symbols are for Through Thickness Crack 100 mm**
***Hollow symbols are for Edge defect 3 mm**
***Target Probabilities of Filure considered are $2.3\text{x}10^{-1}$, 10^{-3}, $7\text{x}10^{-5}$, 10^{-5}**

Figure 5
Comparison between the target and calculated reliability levels for 100 mm through thickness crack and 3 mm edge cracks at different target levels

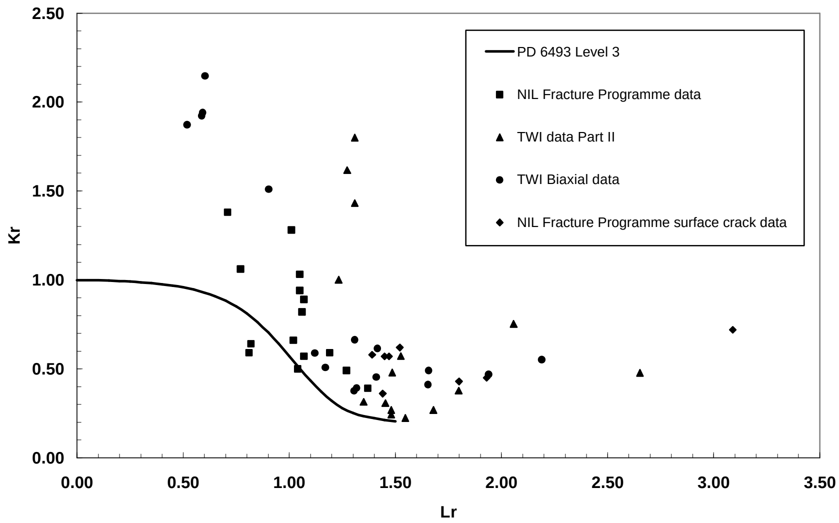


Figure 6
BS7910 Level 3 assessment curve and all the experimental data sets 1, 2 and 3

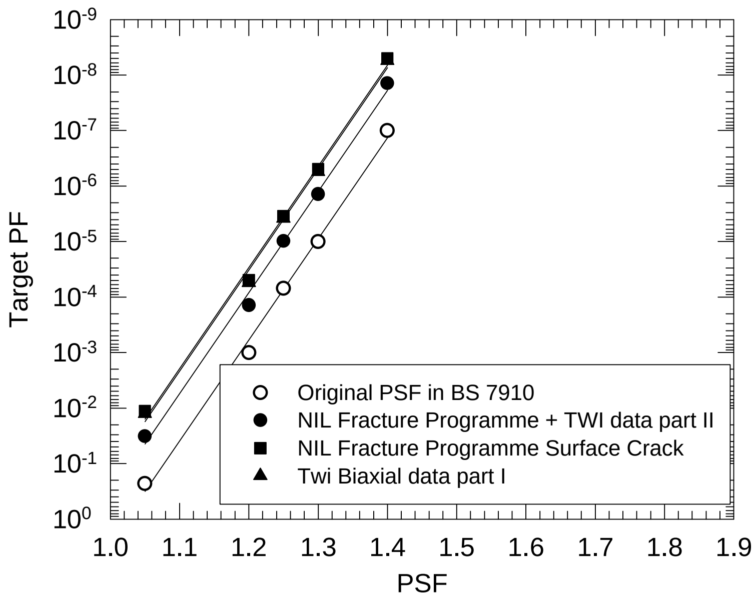


Figure 7
The effect of the Model Uncertainty on the PSF for stress, cov 0.1

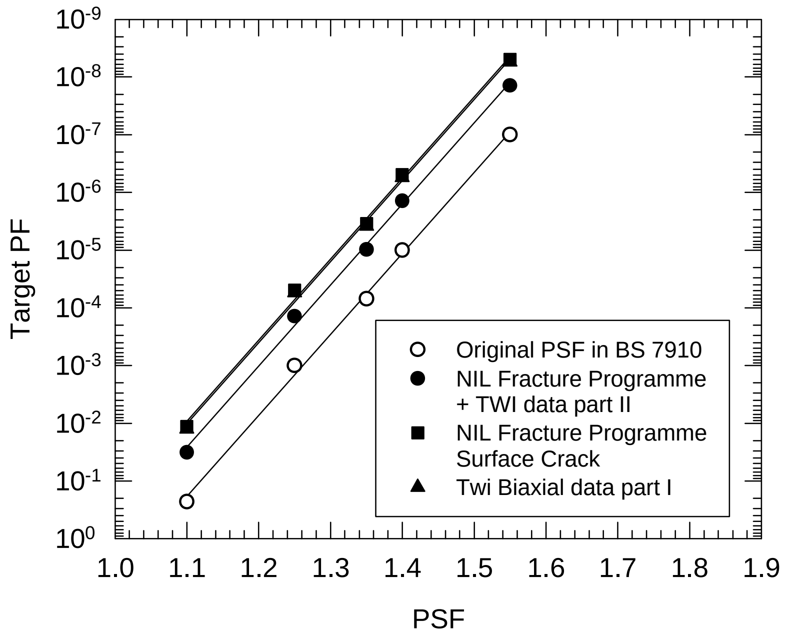


Fig 8
The effect of the Model Uncertainty on the PSF for stress, cov 0.2

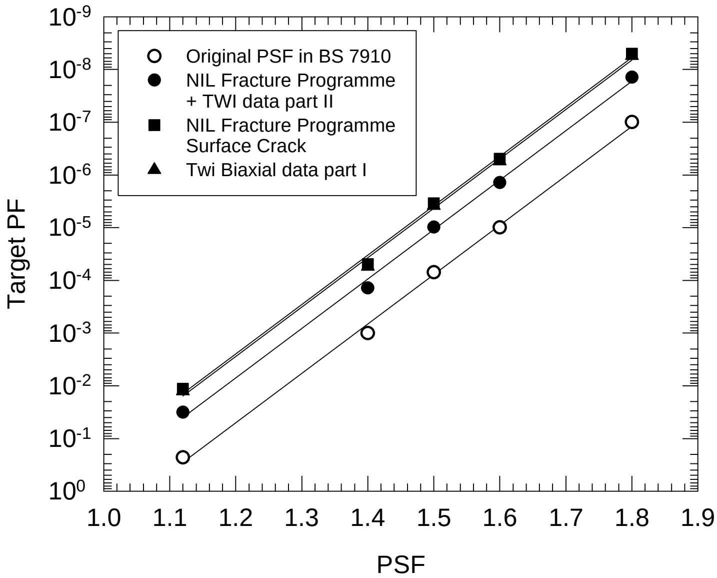


Fig 9
The effect of the Model Uncertainty on the PSF for stress, cov 0.3

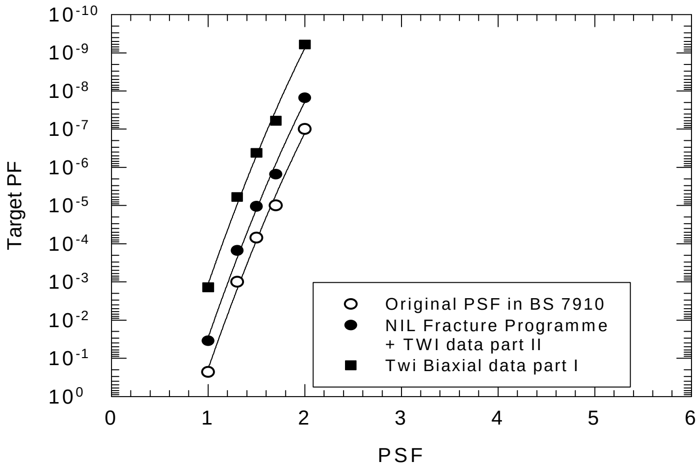


Fig 10
The effect of the Model Uncertainty on the PSF for fracture toughness, cov 0.1

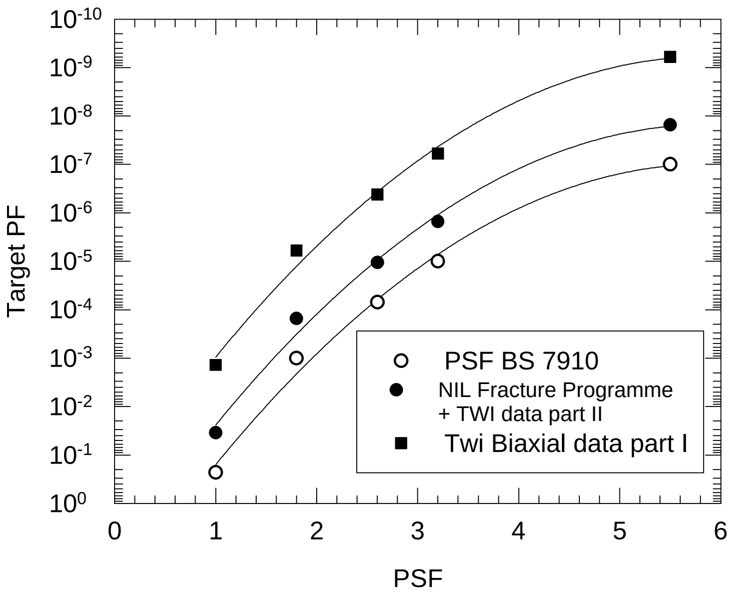


Fig 11
The effect of the Model Uncertainty on the PSF for fracture toughness, cov 0.2

APPENDIX
Material Selection against Brittle Fracture Procedure Proposed by UMIST/TWI

1. Basis

1. A suitable steel grade should be selected for each element to achieve sufficient reliability against failure by brittle fracture, taking account of the following factors:

- applied and residual stress levels
- steel strength grade
- steel toughness quality
- member shape and weld geometry
- element thickness at the welded joint
- appropriate assumed crack imperfections
- minimum service temperature
- loading rate

(2) The steel toughness quality should be chosen on the basis of the required toughness in terms of fracture mechanics assessment and the toughness properties of the material.

(3) For steel covered by Part 2 of ENV 1993, unless a more accurate assessment is made, the limiting thicknesses for different grades of steel and minimum service temperatures given separately in Tables may be deemed to satisfy these requirements. For different stress or flaw size assumptions or for other materials, or where a more detailed assessment is required the appropriate toughness quality for a given thickness and strength grade should be determined using the procedure given in 2.

2. Procedure

2.1 Criterion and stress intensity factor calculation

(1) The toughness of a structural element subjected to a given design tensile stress σ_{Ed} should be taken as sufficient if:

$$K_{IEd} \leq \frac{K_{mat}}{\gamma_k}(K_r - \rho) \qquad (1)$$

where K_{IEd} is the applied stress intensity factor for the given design tensile stress σ_{Ed} and assumed reference flaw size, K_{mat} is the design fracture toughness at the minimum design temperature, γ_k is the partial factor on fracture toughness to achieve the target reliability, ρ is the plasticity correction factor for the secondary stresses, K_r is the permissible value of the fracture assessment diagram parameter at load ratio L_r given by:

$$K_r = \frac{1}{\sqrt{1 + 0.5\,L_r^2}} \qquad (1a)$$

$$L_r = \sigma_{net} / f_y(t), \qquad (1b)$$

where σ_{net} is the net section stress on the cracked section due to primary applied tensile stress σ_p and $f_y(t)$ is the thickness dependent nominal yield strength. In view of the limited sizes of cracks compared to the gross cross section of a bridge member the net section stress may be taken as equal to the applied stress in the region of a defect.

(2) The applied stress intensity factor should be determined from the following:

$$K_{IEd} = Y.\sigma_{Ed}\sqrt{\frac{\pi.\gamma_a.a_d}{1000}} \quad , \text{ (MPa}\sqrt{\text{m}}) \qquad (2)$$

where Y is the factor to allow for defect aspect ratio and position, γ_a is the partial factor on crack size and a_d is the depth of the design crack imperfection (mm).

(3) For bridges the design tensile stress σ_{Ed} should be taken as:

$$\sigma_{Ed} = \gamma_\sigma.M_K.\sigma_p + \sigma_s \qquad (3)$$

where σ_p is the primary tensile stress due to permanent actions and frequent variable actions, γ_σ is the partial factor on applied stresses to achieve the target reliability, M_k is the factor for weld toe stress concentration effects and σ_s is the tensile value of the self-equilibrating secondary stresses such as residual stresses.

(4) For bridges the value of σ_s for welding residual stresses relevant for defects contained within **as-welded welds** should be taken as $f_y(t)$, the thickness dependent nominal yield strength of the parent steel, but may be reduced depending on the loading parameter L_r as follows:

(5)

$$\sigma_s = f_y(t)\left(1.4 - \frac{L_r}{1.2}\right), \qquad 0 < \sigma_s < f_y(t) \qquad (4)$$

For welded components which have been subject to a suitable post weld heat treatment to reduce residual stresses, the value of σ_s should be taken as 0.3 $f_y(t)$.

(5) The plasticity correction factor ρ should be calculated from:

$$\rho = 0.1\psi^{0.714} - 0.007\psi^2 + 0.00003\psi^5 \qquad (5)$$

where $\psi = \sigma_s / f_y(t)$

(6) The value of $Y.M_k$ should be derived from:

$$Y.M_k = p\left(\frac{a}{t}\right)^q \text{, where p and q are constants for the particular weld geometry,} \qquad (6)$$

(7) The design crack imperfection should be taken as follows:

$$\begin{aligned} t < 15 \text{ mm}, &\quad a_d = 3 \text{ mm} \\ 15 < t < 80 \text{ mm}, &\quad a_d = 0.15\, t \\ t > 80 \text{ mm}, &\quad a_d = 12 \text{ mm} \end{aligned} \qquad (7)$$

These 'reference crack' dimensions have been chosen to allow for the possibility of limited fatigue crack growth at welded details in service. For these conditions to be appropriate it is essential that all weld toe geometry regions lying in fatigue sensitive parts of the bridge should be subject to non destructive testing and demonstrated to be free from defects.

(8) The thickness dependent nominal yield strength $f_y(t)$, (N/mm^2) should be determined from:

$$f_y(t) = f_y\left(1 - 0.25(t / t_0) / 235\right) \qquad (8)$$

where: t is the element thickness
t_0 is a reference value of t equal to 1 mm.

2.2 Design fracture toughness

The design fracture toughness K_{mat} should be determined by either:

(1) the **minimum** of three valid standard fracture toughness tests for K_{Ic} or equivalent at the minimum design temperature, or:

(2) correlation with the **mean** of three Charpy V notch impact test results as follows:

$$K_{mat} = 20 + \left(77\left[\exp\left\{\frac{(T_{min} - T_{27J}) + 18 - T_k}{52}\right\}\right] + 11\right)\left(\frac{25}{b_{eff}}\right)^{0.25}\left(\ln\left(\frac{1}{1 - P_f}\right)\right)^{0.25}, \qquad (9)$$

where T_{min} is the minimum design temperature, T_{27J} is the temperature for 27 Joules energy absorption in the Charpy test for the grade of steel concerned (average of three test results), b_{eff} is the effective thickness of the material concerned at the welded joint and T_k represents the effect of the standard deviation in the correlation between K_{mat} and T_{27J} for a required probability level given by K. Wallin as:

$$T_k = 13(0.5 - P_f) \qquad (10)$$

The value of K_{mat} from equation 9 is an estimate of the fracture toughness in MPa√m with a probability P_f of not being exceeded at a temperature T_{min}. It should be noted that the temperature T_{27J} is the temperature for average of Charpy test values but if this is a minimum specified value it will represent the minimum average for an expected distribution.

2.3 Design Temperature

(1) The value of the design temperature T_{min} for an external structure exposed to the weather should be determined from the minimum steel temperature at which the design stress σ_{Ed} applies together including any allowance for radiation effects considered necessary.

(2) The value of T_{min} should be taken as the value with a return period of [100] years.

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