THE APPLICATION OF PROOF TEST AND PROBABILISTIC FRACTURE MECHANICS ARGUMENTS TO THE INTEGRITY OF MAGNOX GAS DUCT PRESSURE WELDS

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The long term integrity of the pressure welds in the gas ducts of Magnox nuclear reactors is assessed based on the results of recent, extensive, ultrasonic examinations and arguments based on the fact that all welds survived pre-service, proof pressure tests. Probability density functions are derived to describe the defect populations in the inspected welds and used in a generalised probabilistic fracture mechanics assessment. These calculations show the risk of failure to be remote under normal operating and postulated fault conditions.

INTRODUCTION

In Magnox nuclear power stations, the reactor pressure vessel and steam generators are connected by large diameter (1-2m) steel ducts, 11 to 37mm thick, through which hot CO_2, the primary coolant, is circulated by blowers. The construction of the primary gas pressure circuit of a typical Magnox station is shown in Figure 1. The duct sections are fabricated from rolled C/Mn steel plates, seam and butt welded together in-works using a submerged arc (SMA) machine process and subsequently joined together by on-site, circumferential manual (MMA) welds.

Following manufacture, all these welds were fully radiographed, this being the best inspection technique available at the time, and repaired as necessary. In recent years, ultrasonic examination techniques have been developed which are more sensitive to narrow, crack-like defects. These techniques have been employed extensively at Magnox stations to confirm the quality of the duct butt and seam welds. To date, approximately 20% of accessible welds have been ultrasonically tested, the total length of weld inspected being approximately 5000m. The incidence of defects was low; although about 200 defects were reported, many were volumetric in nature (eg slag) and all crack-like defects were small.

In the light of these inspections, the long term integrity of these welds has been reviewed. Probability density functions are derived describing the length and depth of defects found. These distributions, together with a detailed knowledge of materials properties, operating and

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and fault stresses, enable probabilistic fracture mechanics assessments of weld integrity to be carried out.

In addition, following stress relieving, the sections of ductwork were subjected to a hydraulic proof pressure test, which was about twice the normal operating pressure, and subsequently leak tested. The fact that all sections satisfactorily survived these tests can, under certain conditions, allow margins of safety on operating and postulated fault conditions to be calculated which also take into account possible in-service degradation and crack growth. Since this test places an upper limit on the size of defect that could be present in a given weld, it can also be used in combination with the inspection information as part of the probabilistic fracture mechanics assessment.

SAFETY MARGINS BASED ON THE PROOF TEST

The initial assessment examines the integrity of the butt and seam welds in the presence of large, postulated defects and attempts to demonstrate that even the maximum size of defect that could have survived the proof pressure test, would not lead to spontaneous failure in service. The type of defect of interest is a long, part-penetration crack of uniform depth. This idealised defect represents the most serious case since shorter or very irregular cracks would probably result in a "leak before break" condition and are likely to be excluded by observation.

The maximum size of this defect that could just survive the pre-service, proof pressure test conditions is calculated. In the present instance, this has been carried out using the CEGB defect assessment procedure commonly known as 'R6' (Harrison et al (1)), using materials data and stresses pertaining to the actual proof test conditions. This crack is then subjected to temperatures and pressure stresses typical of normal operating and fault conditions and any margin against failure is calculated, making allowance for in-service crack growth. Margins against creep failure are calculated as well as those against fast fracture, since the top gas ducts operate at temperatures up to 360°C. Lower duct temperatures are limited to about 180°C.

Materials data have been derived from the results of tensile tests carried out on actual welds at the time of construction. Additional tests have been carried out on archive materials left over from construction to provide fracture toughness data for the butt and seam welds over the temperature range of interest (20-360°C). Under these conditions, temperatures are sufficiently high that the weld metal will exhibit upper shelf toughness behaviour so that the initiation of stable, ductile tearing can be taken as the point of failure at both the proof test and in operation. In general, fracture toughness testing was carried out on 25mm thick compact tension specimens and the variation of fracture resistance (J) with crack growth was determined. Checks were made to ensure that ASTM validity requirements on specimen size, expressed in terms of maximum J and extent of crack growth, were satisfied so that plane strain conditions prevailed at crack initiation.

Experiment has also shown that the creep rupture properties of the welds are similar to those of the parent C-Mn steel plates, and can be described by ISO parametric functions for Si-killed, C-Mn steels.

In general, best estimates of materials data are used in the analysis rather than lower bound properties, since the aim is to determine whether a postulated defect will become more severe in service and where it can grow by fatigue. The effect of varying any of the key properties, such as tensile strength or fracture toughness, between upper and lower bounds is also analysed as part of a sensitivity analysis on the safety margins calculated. The exception is that fatigue crack growth is assessed using upper bound crack growth data, since this does not involve a comparison of performance between proof pressure and operating conditions.

In general, the following fatigue crack growth law was used; this describes the performance of ferritic steels when a striation mechanism is operating:-

$$da/dN = 10^{-11}\, C(\Delta K)^3 \quad (\text{m/cycle}) \qquad \ldots (1)$$

where C is a temperature dependent factor, with C = 1 at temperatures less than 250°C, increasing to C = 2 at 360°C.

However, where K_{max} is greater than 0.7 K_c other modes of crack growth can occur, leading to higher crack growth rates. In this regime, the following relationship was used:-

$$da/dN = \frac{0.6 \times 10^{-11}\,(\Delta K)^3}{\left[1 - \left(\frac{K_{max}}{K_c}\right)^2\right]^{3/4}} \quad (\text{m/cycle}) \qquad \ldots (2)$$

To simplify the analysis, any retardation effects due to prior overloads on the structures, such as provided by the preservice proof pressure tests, have been pessimistically ignored. In addition, possible interaction effects between creep and fatigue crack growth have not been considered.

TABLE I - TYPICAL MEAN MATERIALS PROPERTIES OF SMA AND MMA WELDS

Material Property		Temperature		
		20°C	180°C	360°C
Yield stress (SMA)	MPa	329	276	273
Yield stress (MMA)	MPa	432	365	359
UTS (SMA)	MPa	509	450	448
UTS (MMA)	MPa	525	467	462
Fracture Toughness (SMA)	MPa √m	184	163	140
Fracture Toughness (MMA)	MPa √m	222	200	192

Typical material properties used in the integrity assessments are shown in Table I.

During early operation, the proof test is found to give margins against failure for most welds at normal and fault pressure. However, as potential fatigue crack growth proceeds and creep damage accumulates, these margins are slowly eroded, and can eventually be lost. Proof pressure test margins against fast fracture for a typical Magnox station are shown in Table II, after a projected operational life of 25 years.

TABLE II - MARGINS AGAINST DUCTILE FAST FRACTURE GUARANTEED BY THE PROOF TEST AFTER 25 YEARS OPERATION OF A TYPICAL MAGNOX STATION

Weld Type	Factor on Pressure			
	Lower Duct		Upper Duct	
	Normal	Fault	Normal	Fault
Seam welds (SMA)	1.66	1.36	1.52	1.24
Butt welds (SMA)	1.72	1.41	1.70	1.39
Site circumferential welds (MMA)	1.06	<1	1.06	<1

In most cases, margins against failure are in excess of unity and there is considered to be no risk of failure. In this example, the margin for upper duct, circumferential, site welds was lost when the station was commissioned. This is because site welds were subjected to a much lower proof pressure (typically only 40% above operational pressure). This pressure margin is not sufficient to counterbalance the reduction in materials properties between proof test temperature (~20°C) and operating temperature (360°C). The effect of varying the important material properties on the margins calculated is shown in Table III for the case of upper duct, seam welds at fault pressure. It can be seen that there is little variation despite the wide range in properties assumed.

TABLE III - MARGINS AGAINST DUCTILE FAILURE FOR VARIOUS COMBINATIONS OF MATERIALS DATA (AFTER 25 YEARS OPERATION)

Weld Type	UTS Bound	KIC Bound	Margin on Pressure
Upper Duct Seam (SMA) Weld	Mean	Mean	1.24
	Upper	Lower	1.25
	Lower	Upper	1.25
	Lower	Lower	1.28
	Upper	Upper	1.15

For 25 years operation at 360°C, creep rupture stresses for the welds in top gas ducts are typically about half the ultimate tensile strength. Hence, this counteracts the large proof to operating pressure margin and it is not generally possible to demonstrate safety margins against creep failure in excess of unity, particularly when materials exhibit creep properties towards the lower bound. Instead, probabilistic arguments can be developed to assess the risk of sufficiently large defects being present which could give rise to failure of a weld over the operational lifetime of a reactor. Such an analysis is described in the next section.

PROBABILISTIC FRACTURE MECHANICS ASSESSMENT

The duct welds are generally lowly stressed and hence only large defects are likely to cause disruptive failure. However, any critical size defects are likely to have been eliminated by the proof pressure test. Hence, the risk of weld failure can be estimated by calculating the probability of defects being present which are both too small to cause failure during the proof test and large enough to cause failure in service.

Derivation of defect distributions

In order to calculate the probability of a given range of defect sizes being present, one requires a knowledge of the actual distribution of weld defects. This can be derived from pre-service examination results or from in-service inspections. In the present case, the latter results have been used, since these were done using more exhaustive, ultrasonic techniques.

As noted earlier, about 20% of the accessible duct welds were recently tested, giving information on the length and depth of defects present. Since both dimensions are required in a fracture mechanics assessment, the probability analysis has to model such combinations of length and depth, taking into account whether they are distributed independently or whether there is some association between them.

In the past, various functions have been chosen to describe distributions of initial defect size. For example, Becker and Pederson (2) suggested that it is exponentially distributed at the beginning of service, reflecting the expectation that there will be a large number of small defects with a decreasing number of larger ones. A similar function was used in the Marshall report (3) for the defect distribution after initial manufacture.

After a period of service, the distribution of defects would be expected to change since larger defects will grow at a faster rate. Becker and Pederson (2) and Besumer and Tetelman (4) have suggested that the distribution then approximates more to a log normal distribution.

Although the best way of establishing what forms the full distributions of defect sizes take would be to measure a representative sample of defects, the limits of accuracy of NDT procedures prevent a full count of all defects in the welds sampled. It is therefore not possible to test the basic assumptions of previous authors directly. Two alternative approaches are possible, both of which recognise the practical constraints placed on the way in which the defect size samples were taken. These are to derive size distributions conditional on the defect being larger than the threshold sizes detectable by NDT and to estimate the number of such defects per unit run of weld; or to derive the distributions of the size of the largest defect per unit run of weld. There are two practical advantages in choosing the latter of these two approaches. Firstly, because the detectable threshold sizes vary from site to site and between surface breaking and embedded defects (ie MPI versus ultrasonics), more of the available data can be used in the second approach than in the first. Secondly, it is the largest defect in a weld, which is going to cause failure, if failure occurs. This second approach has therefore been adopted in this paper.

The sampling frame used for this approach is that of individual welds. The welds surveyed are roughly similar in size and are large enough for it to be assumed that they will contain a fairly large number of defects, almost all of which will be of sizes below the detection threshold of any NDT techniques.

As noted above, the accuracy of the defect measurement depends on the method of inspection employed. The minimum size of defect measurable by ultrasonics is limited to about 3mm (length and depth). In practice, dimensions less than 3mm were not generally reported during in-service weld inspections because they could not be accurately sized. Upper bounds on depths of defects found by MPI were determined from the results of local grinding to remove the indication. Experience has shown that MPI is capable of detecting defects with surface lengths as small as 2mm.

The size of the largest defects in each of the welds surveyed can be described by the extreme value probability density functions of Gumbel (5). The type II function probably provides the best description of defect distributions in welds, since it is unbounded at the upper extreme, but bounded at zero at the lower extreme. This cumulative distribution function is of the form:

$$D(x) = \exp\left[-\left(\frac{x}{A}\right)^{-B}\right] \qquad \ldots(3)$$

where A is a scale parameter, B is a shape parameter and x is the crack depth normalised by weld thickness. The corresponding probability distribution for depth x, is:

$$d(x) = BA^{B} \exp\left[-\left(\frac{x}{A}\right)^{-B}\right] x^{-(B+1)} \qquad \ldots(4)$$

A distribution can also be fitted to crack shape (or aspect ratio). Analysis of the inspection data showed these to fit Weibull-type distributions, with cumulative distributions taking the form:

$$G(z) = 1 - \exp\left[-\left(\frac{z-\eta}{\theta}\right)^{\beta}\right] \qquad \ldots(5)$$

where η is a location parameter, θ is a scale parameter and β is a shape parameter. Again, the corresponding probability distribution function is:

$$g(z) = \frac{\beta}{\theta}\left(\frac{z-\eta}{\theta}\right)^{\beta-1} \exp\left[-\left(\frac{z-\eta}{\theta}\right)^{\beta}\right] \qquad \ldots(6)$$

Defects whose aspect ratio is known are used to form the sample from which the parameters are estimated. Unlike the depth distribution functions, these are based on all sized defects, not just the largest in a given weld. For both the extreme value and Weibull distributions, the parameters are estimated by using the method of maximum likelihood. Parameter values are found so that the actual sample of defect sizes is the most probable sample.

The two dimensional probability distribution for the largest defect in a weld, having normalised depth x and aspect ratio z, is simply given by multiplying equations 4 and 6. However, it is of greater use to formulate the two dimensional, probability distribution for defect length (L) and depth (x) since these quantities are more relevant to the stress analysis of defects.

Manipulation of equations 4 and 6 then gives:

$$f(x,L) = \frac{BA^{B}}{2wx\theta x^{B+1}} \left(\frac{\frac{L}{2xW}-\eta}{\theta}\right)^{\beta-1} \exp\left\{-\left[\left(\frac{\frac{L}{2xW}-\eta}{\theta}\right)^{\beta} + \left(\frac{x}{A}\right)^{-B}\right]\right\} \ldots(7)$$

This bivariate, probability distribution contains an implicit assumption of dependence between defect length and depth through the term $^{L}/2xW$ in the exponentiation.

Values of the constants found for the fitted probability distributions are given in Table IV, using inspection data from all Magnox stations.

TABLE IV FITTED PARAMETERS FOR ASPECT RATIO AND DEFECT DEPTH DISTRIBUTIONS

Distribution	A	B	Shape β	Scale θ	Origin η
Aspect Ratio	-	-	0.274	2.1	0
Defect depth	0.0368	2.95	-	-	-

Estimation of failure probability

The NDT results, expressed in the form of the bivariate probability distribution, f(x,L), given by equation (7), can be extrapolated to predict the likelihood of serious, critical defects. The distribution can be represented by a set of contours as shown in the probability plot of Figure 2.

Fracture mechanics assessment, using the R6 procedure, is used to define those cracks which are large enough to grow to failure in-service, but not so large as to have caused failure on proof test. Such calculations define the domain of interest on the probability plot, (Figure 2). Firstly, the maximum defect size which could have survived the proof test is calculated (x_2). The smallest crack which could, by a combination of creep and fatigue crack growth, lead to failure after time t at normal operating or fault pressure, is also calculated (initial depth x_1, length L_c).

It should be noted that, in calculating the failure probability of a weld, the critical length of defect, L_c once it snaps through is also taken into consideration. Hence, the failure probability (P) accumulated in a given period of service is then given by:

$$P = \int_{L_c}^{L_T} \int_{x_1}^{x_2} f(x,L)\, dx\, dL \qquad \ldots(8)$$

where L_T is the full length of the weld and f(x,L) is given by equation 7. Should fault overpressure be applied, equation 8 gives the risk attendant on it.

Similarly, the annual risk of creep failure of top duct welds at normal operating pressure can be assessed with in-service critical defect sizes being estimated using ISO creep rupture data. The effect of continued service is to reduce x_1, by allowing more time for crack growth and creep rupture, leading to a further increment of risk. Thus the annual rate of failure (R) can be calculated as a function of time.

$$R = \int_{L_c}^{L_T} \int_{x_1(t+1)}^{x_1(t)} f(x,L)\, dx\, dL \qquad \ldots(9)$$

This probabilistic assessment procedure has been applied to the gas duct welds described earlier. For most welds, however, the annual failure probability is nominally zero since $x_2 > x_1$: this is equivalent to saying there is a safety margin above unity from a proof test argument. However, for welds where no such margins could be demonstrated from survival of the proof test, ie site welds and top duct welds subject to creep failure, the range of critical defects that could give rise to failure can be calculated. The range of defect sizes involved is found to be very small and failure probabilities are correspondingly remote (less than 10^{-6} per reactor year), as shown in Table V.

TABLE V - FAILURE PROBABILITY PER REACTOR YEAR FOR GAS DUCT WELDS DUE TO CREEP AND DUCTILE FRACTURE AFTER 25 YEARS OPERATION OF A TYPICAL MAGNOX STATION

Gas Duct	Shop or Site Weld	Weld Type	Failure Mechanism	Failure Probability per Reactor Year
Top	Shop	Seam	Creep	1×10^{-7}
Top	Shop	Butt	Creep	2×10^{-9}
Top	Site	Butt	Creep	2×10^{-7}
Bottom	Site	Butt	Ductile Fracture	4×10^{-8}
Top	Site	Butt	Ductile Fracture	5×10^{-8}

In a probability analysis, it is also possible to treat quantitatively the variability of materials properties. At first sight, it might appear that to demonstrate a very low failure rate, it would be necessary to have a high degree of confidence in materials properties, ie to use a value removed from the mean by several standard deviations. However, the property does not need to be as reliable as this. The analysis presented here is, in fact, rather forgiving of error in tensile and fracture toughness properties because the probability of failure is proportional primarily to the width of the "window" of defect sizes that can give rise to failure, rather than its location. The window width is related to fatigue crack growth rates, which are not subject to great variability, whilst the location of the window is determined by tensile and fracture toughness properties.

A sensitivity study, to determine the effects of variability in materials properties on the overall probability assessment was performed using normal distributions. The overall failure probability was synthesised using Monte Carlo techniques, and it was found that the risk is relatively unaffected by credible variations in materials properties. In fact, it was found that the use of mean materials properties to calculate failure probability leads to a value which is not appreciably different from that obtained by full Monte Carlo analysis.

It has been assumed in the statistical analysis that the defects found above inspection limits of detection were all such defects that were present in the inspected welds. However, there is always a finite chance of missing a defect. This is difficult to quantify, but as a very pessimistic appraisal, we could follow the Marshall (3) report. This would indicate, for example, that a defect with depth 12mm would be missed with a probability 0.25. The error may be estimated by expanding the sample in proportion to the Marshall probability, as a function of depth. The effect on failure probability was found to be quite modest, increasing by a factor of about 2.5 when allowing for missed defects in this way.

CONCLUDING REMARKS

This paper describes methods which have been developed to demonstrate the long term integrity of the main pressure welds in the primary gas ducts of Magnox nuclear reactors. These methods are capable of general application to many integrity assessments and are based on, either an assurance of safety as a result of the component surviving a pre-service overpressure test, with an implied estimate of zero probability of spontaneous rupture, or, alternatively, a value for the probability of rupture.

The latter method requires a knowledge of the actual distribution of defects in the welds, which is normally gained by inspection. In the present case, such defect distributions have been described by bivariate probability functions, incorporating both defect depth and length, and based on extreme value statistics. However, even where such distributions are not well established, or a guarantee of safety from the proof test cannot be claimed, it may still be useful to apply the procedures outlined here and demonstrate the restricted conditions of defect size which could lead to rupture, ie long, deep cracks with a very narrow, depth range.

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SYMBOLS

A = scale parameter of extreme value distribution
B = shape parameter of extreme value distribution
C = temperature dependent fatigue constant
J = fracture resistance parameter
ΔK = range of stress intensity factor
K_{max} = maximum stress intensity factor
K_c = critical stress intensity factor
L = defect length
L_c = critical defect length
L_T = length of weld
P = failure probability
R = annual rate of failure
t = time
x = defect depth
x_2 = critical depth of defect during proof test
x_1 = critical depth of defect during normal operation or at fault condition
z = aspect ratio
β = shape parameter of Weibull distribution
θ = scale parameter of Weibull distribution
η = location parameter of Weibull distribution
da/dN = rate of fatigue crack growth

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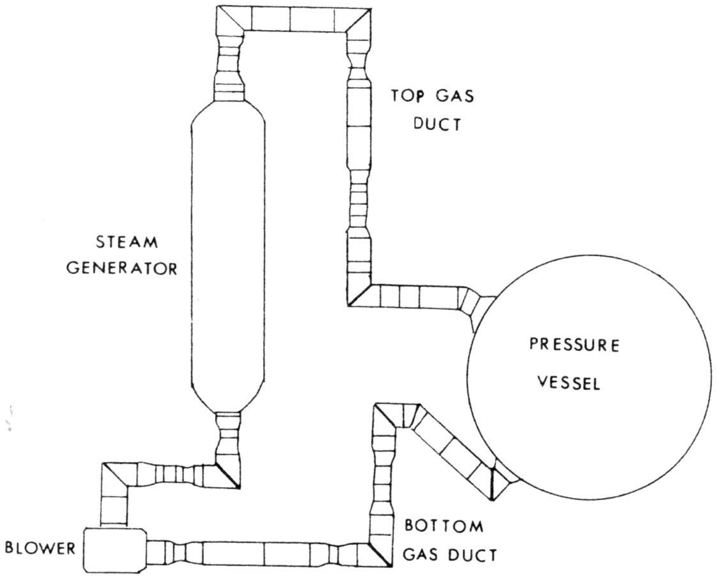

FIGURE 1 - PRIMARY GAS CIRCUIT OF TYPICAL MAGNOX STATION.

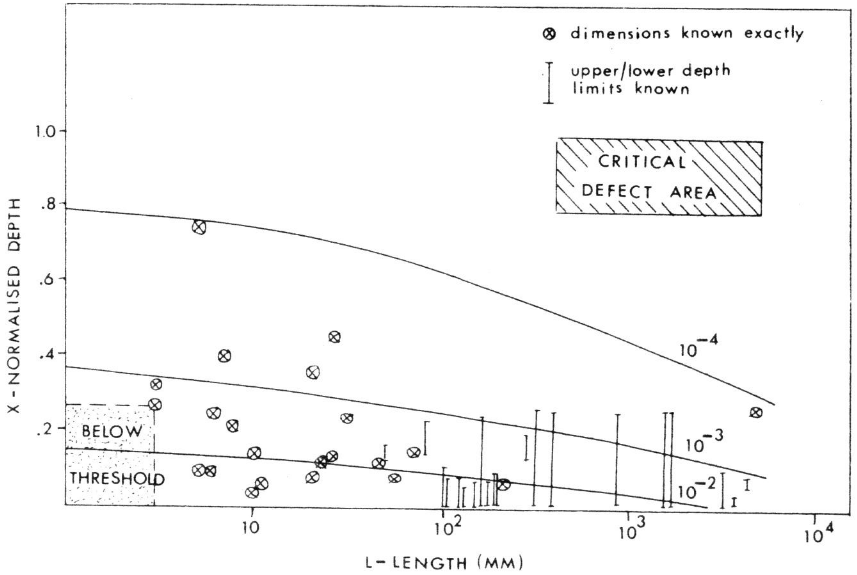

FIGURE 2 - CUMULATIVE PROBABILITY CONTOURS FOR LARGEST DEFECTS IN WELDS (RESULTS FROM 1090 WELDS INSPECTED - DEFECTS PLOTTED WITH DEPTH ≫3MM OR LENGTH ≫100MM).