1. Introduction

The most extensive use of the EDY (Effective Degradation Years) formula is in the prediction of the occurrence of stress corrosion cracking (SCC) in alloy 600 components including steam generator tubing and control rod drive mechanism (CRDM) nozzles. This report addresses only the CRDM issue, and proposes other aspects of the SCC process that should, or could be considered along with the formula itself. The topics addressed in this report include: (a) stress analysis of the CRDM and head assembly, (b) use of laboratory data in the prediction of cracking, (c) use of information from discarded heads in predictive tools, and (d) the incorporation of microstructure, degree of cold work, surface finish, and other physical and metallurgical information to particularize the application of the formula to a specific head, or a specific CRDM.

2 Background – Mechanistic

The susceptibility, or time-at-temperature model being used to compute an effective description of aging of NSSS components is based fundamentally on the Arrhenius, or thermal activation model in which the rate of a single process, P, is a function of temperature (T, in degrees Rankin) and an activation energy (Q) expressed as:

$$P \propto \exp(-Q/RT) \tag{1}$$

in which $\propto$ is the proportionality symbol and R is the universal gas constant (1.103×10^{-3} kcal/mole-°R). This is a tried and true formula for the description of a great many kinetic processes that are dependent on thermal activation, including materials' deformation processes resulting from dislocation motion (Ref. 1). The initial application of this formula to vessel head penetrations may be attributable to Scott. In the very idealistic sense, "Q" should represent only one process, with an unchanging dependence on temperature. Consequently, the formula should not be extended to characterize a phenomenon which is dependent on multiple processes, each perhaps with a different dependence on temperature, or used over a temperature range so large that successive processes take place in specific ranges of temperature (oxidation of iron is a familiar example). Such a mis-application of this formula would result in calculation of a Q-value integrated over the multiple processes. Pragmatically, however, a single expression may be used for even complex, multi-process phenomenon, as long as the temperature range is relatively small, perhaps a few tens of Kelvin degrees. A small temperature range is the case for reactor heads, for which T ranges from about 560°F through about 605°F [575°K to ~590°K).

The computation of EDY for a particular reactor component is based on the years of full power operation (EFPY) normalized to 600°F by incorporating the activation energy expression (Eq. 1) to achieve:

$$EDY_{600^\circ F} = \sum_{j=1}^{n} \left\{ \Delta EFPY_j \quad \exp\left[-\frac{Q_j}{R}\left(\frac{1}{T_{head,j}} - \frac{1}{T_{600^\circ F}} \right) \right] \right\} \tag{2}$$

For applications to "PWSCC degradation of a reactor head", the Q_j = 51 kcal/mole - the activation energy for crack initiation for Alloy 600. The summation over the index, j, allows for accounting of periods of time during which the component may have been subjected to different temperatures.[1] For example, this would apply to the head of a unit that had started up with one set of operating conditions (e.g., a relatively high head temperature), and was backfitted at some subsequent outage to provide cold leg flow diversion toward the head. Reference 2, MRP-48, contains Table 2-2, a listing of the vessel head temperature history for all domestic plants. The NRC Inspection Manual Change Notice 02-037, dated 10/18/02 contains explicit information on the calculations of EDY.

Using data supplied to the NRC by the MRP (Materials Reliability Program), figure 1 is a plot of the EDY calculations for domestic plants, using firm data at 2/28/2001 and an approximation to update all values to 12/31/2002. The symbols filled with red designate plants that have discovered CRDMs with cracks. This plot also shows an approximated calculation of the EDY (= 11.9) for Davis-Besse in February, 1996, which is the earliest that Nozzle #3 is suspected to have begun to leak, according to the root cause report. The plot shows that most plants either finding leaks, or making repairs, are well into the high susceptibility range. The exceptions are the D. C. Cook 2 plant, which repaired one leaking crack at EDY = 9.5, and has been free of leaks since (now at EDY = 13.9), and the Millstone 2 plant, which repaired non-leaking cracks in three nozzles at EDY = 11.6, after experiencing a clean NDE exam at EDY = 10.1.

The Millstone, Cook, and possibly the Davis-Besse plants' experiences challenge the choice of 12 EDY as the medium-to-high susceptibility threshold. However, there are many plants (18 to be exact) with EDY > 12 and clean inspection results. This suggests that there may be other factors besides time and temperature that are controlling crack propagation in CRDM nozzles. If so, those considerations are not factored into the susceptibility model in its present incarnation.

Returning for a moment to the explicit use of the Arrhenius formula, it should be pointed out that the current practice is to compute domestic power plant EDYs relative to 600°F, or ~589°K. The industry document describing crack growth rates (CGRs) of Alloy 600 (Ref. 3) also uses this same Arrhenius formula to put the CGRs on a parallel footing, but the reference temperature used is 617°F (325°C or 598°K), and the activation energy used is 31 kcal/mole – the activation energy for crack growth in Alloy 600[2]. At some point, I believe it would be sensible to select a uniform reference temperature for use in these and similar modeling applications.

The second issue - whether to change the value of the activation energy in the EDY formula - requires either proof, or rationalization that the CRDM failure process is controlled by crack growth, with little time required for nucleation of the crack, or vice versa. Leaving the value of the activation energy in the EDY formula unchanged at 51 kcals/mole indicates that the crack nucleation phase dominates component life, with proportionately little time required for crack growth. Based on the evidence from VHP failures, it appears that crack growth is the more

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[1] There is no good reason that "Q" should be subscripted. Q has the same value, no matter what is the temperature.

[2] None of that discussion pertains to Alloy 82/182 weld metal. The research toward determination of activation energies for either crack nucleation or growth in Alloy 182 or 82 is insufficient at the present time. What research is available suggests that the activation energy for crack growth in Alloy 182 is about 125% of that of Alloy 600.

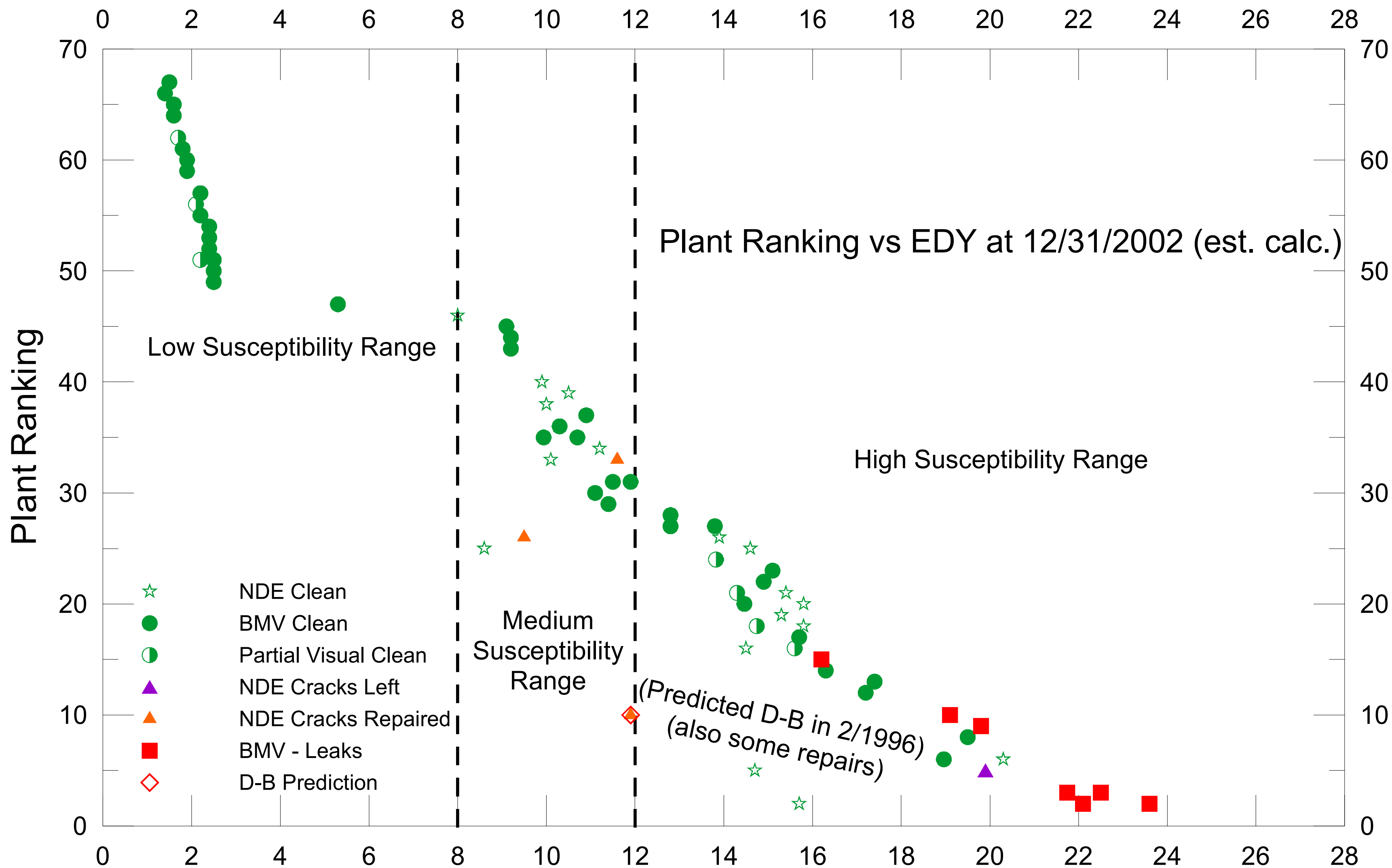

Figure 1. Ranking of domestic plants according to the EDY formula, showing results of inspections, evidence of leakage, and repairs. Many plants are shown with multiple symbols, indicating a “clean” inspection at inspection opportunity, followed by a different finding at a subsequent inspection (e.g., Oconee 2: clean NDE @ EDY=15.7, leaks and circ. flaws @ 22.1)

dominant contribution to the failure process. That conclusion leads to the adoption of Q = 31 kcal/mole in the EDY formula. There would be two consequences of this change: (a) all of the EDYs for individual plants compute to a higher number, compared with the current calculations (using 51 kcals/mole), and (b) each plant approaches the threshold for transitions from low-to-medium, or medium-to-high susceptibility, more rapidly. This has the consequence of affecting long-term planning, and decisions of whether to repair the penetrations, or replace the head. The ranking of the plants would remain unchanged.

3. Background - Historical

Reference 3 provides an excellent review of the vessel head penetration degradation situation up to the time of its publication in 1994, and much of the material in this section is a summary of information found in more detail in that document. The discovery in France of the leaking CRDM nozzle at Bugey Unit 3 came against a background of twenty years of SCC problems in Alloy 600 steam generator tubing, together with incidents of leaking pressurizer nozzles and heater sleeves at many plants. Subsequently, axial cracks were found in the CRDMs of Ringhals Units 2 and 4. Indications of cracks were recorded at Oconee Unit 1 as early as 1994, and cracks were detected unambiguously, and repaired, in 2000. Since then cracks have been discovered in several USA plants, both in the Alloy 82/182 J-welds, and in the Alloy 600 CRDM nozzles.

In the US, the initial safety concern centered on the development of circumferential cracks. Stress analysis shows that a CRDM with a throughwall, circumferential crack of about 320° extent has reached net section failure criteria, and may be ejected (depending on other restraining fixtures), with a loss-of-coolant consequence and an elevated probability of core damage. However, the axial cracks in the Davis-Besse CRDMs, which allowed long-term leakage of primary coolant onto the upper surface of the head, showed that boric acid corrosion of low-alloy steel could reach proportions that threatened the integrity of the primary boundary. Therefore, both circumferential and axial cracks lead to serious safety concerns, but for different reasons.

In the late 1960s, and years before nickel-base alloys, and Alloy 600 in particular exhibited in-service failures in steam generators, Coriou published laboratory results describing the pure water, stress-corrosion crack growth characteristics of Alloy 600. Those results could not be duplicated immediately, but over time the experiments were replicated, and the parameters of SCC growth rates became well-defined. The stress corrosion cracking of steam generator tubing, coupled with the cracking in thicker section Alloy 600 components and attachment welds resulted in the initiation of a great many laboratory programs to evaluate the SCC growth rates in both forms (thin- and thick-walled) of these alloys. These studies were augmented by an intensification of field observations, including metallographic analyses and correlation of the susceptibility of cracking with other material properties. Ref. 4 is the state-of-the-art document describing crack growth rates in PWR coolants, although the alloy also cracks in oxygenated, pure water (i.e., BWR) environments as well.

When the susceptibility model was initially developed (Ref. 3), it was intended that several parameters could be factored into the formula. In addition to time and temperature, stress was incorporated into the original formulation. The fourth power of the stress was selected as a factor in the formula, based on Yonezawa's studies of crack initiation in cold-worked nickel-

based alloys. Other parameters were considered, including hardness, yield strength, and carbide coverage of the grain boundaries. Initially, the susceptibility expression took the form of an expression for time, normalized to a reference time, and contained stress, referenced to a reference stress, and a materials susceptibility factor, K, similarly referenced.

$$t = t_{ref} \left[K_{ref} / K \right] \left[\sigma_{ref} / \sigma \right]^4 \exp \left\{ Q / R \left[1/T \; - \; 1/T_{ref} \right] \right\} \tag{3}$$

The difficulty of unequivocally computing stress for individual heads or nozzles soon led to elimination of stress from the equation. Similarly, lack of appropriate description of the microstructure of the Alloy 600 nozzles, exacerbated by confusing variations in the results of laboratory testing that addressed microstructural effects, led to elimination of the materials susceptibility factor from the equation also. This left only time and temperature in the index, leading to the formulation used at the present time.

So, in a sense, this manuscript attempts to bring the development full circle. In the intervening nine or so years since the initial presentation of the susceptibility model, there has been a substantial increase in the amount of laboratory data and plant experience, that both support the model's fundamentals, while at the same time suggesting that there may be some possibility of improvement.

4. Temperature

The most obvious quantity to consider is the value of temperature associated with a particular head, or a particular nozzle. I believe that most licensees are using either design temperatures, or cold leg temperatures (for those plants that have diverted cold legs), or the results of a thermal/hydraulic calculation in the equation. Very few plants, if any, have immersion thermocouples that measure temperatures on or very near the inside diameter of the head. The author has participated in several discussions as to whether design-based temperatures, or the results of T/H analysis are truly representative of the actual head underside conditions. Questions have also been raised as to whether the underside temperatures are constant, or vary with time over a range, depending on variables such as core configurations and coolant pump speeds. Also, some engineers have questioned whether the temperature is uniform over all the CRDMs, since the more centrally located nozzles are situated more directly over the core, and may be more directly impacted by the upflow, and therefore at a higher temperature than the peripheral nozzles.

5. Stress Analysis

It is often repeated that stress-corrosion cracking requires three things (a) time, (b) a material-environment combination with cracking susceptibility, and (c) stress. We cannot do anything about item (a). The sections that follow discuss item (b) in some detail. But first – a few words about the most neglected factor in these considerations - stress.

4.1 General Procedures for Stress Calculation

Plausibly successful finite element analyses (FEA) of the stress in vessel head penetrations (specifically, pressurizer nozzles) became available in the late 1980s, with the advent of faster computers that could handle the geometrically and procedurally complex process involved in the (a) dimensionally-produced interference and (b) welding-induced residual stress calculations that go into the analyses. The general procedure used to model the installation and welding procedure is to design a mesh for a CRDM, and a separate mesh for the reactor head bore such that the two have a specific interference fit, usually of the order of 0 to 10 mils. After assembling the two, (all in the calculational sense) a volume of metal at a typical weld deposition temperature is placed in the J-weld prep of the model and allowed to cool. The stresses set up by thermal contraction of the weld deposit are calculated incrementally as the temperature drops, using the continuously varying, temperature dependence of the stress-strain response, thermal conductivey, elastic properties, etc., and the computational results are saved. That process (deposit a weld metal bead, cool, contract, calculate continuously) is repeated until the J-groove is filled. The last step is to model the redistribution of stresses that occurs during the initial hydrotest (to 1.25 normal operating pressure (NOP) and near-ambient temperature). This redistribution is significant; therefore this calculational step is important. Finally, the completed model is raised to the operating temperature and pressure of the reactor, after which the stress distributions that are calculated are presumed to be in their permanent state.

At the present state of the art, the interference fit is assumed to be uniform along the whole length of the bore through the head thickness, less the “J”-shaped opening allowed for deposition of the weld. In reality, the contact surface and the contact pressure between the CRDM and the head are probably not uniform. Additionally, the coefficient of friction probably varies over the contact surfaces. The stress-strain properties of the weld metal are required by this model, not just at the final temperatures, but for all temperatures over which the contraction occurs. In practice, FEA developers generally have the stress-strain curves at a few discrete temperatures, and interpolate and extrapolate to obtain the continuum of properties that is needed. In the CRDM nozzle, hoop stresses are the driving force for axial cracks, and axial stresses are the driving force for circumferential cracking. The centerhole position in the reactor head is the only axisymmetric location; the sidehill nozzles all require a full 3-D treatment, and a circumferential crack in a sidehill nozzle probably follows the sinusoidal shape described by the J-weld if it were mapped onto a plane. These are very complex calculations.

The process described above applies to an error free assembly of the J-weld and nozzle structure. In reality, many assemblies involve removal of weld beads that fail inspection, back-gouging or sanding, sometimes peening and rewelding. The possibilities of exceptions to the idealized procedure are almost endless, and some modeling is required to scope the effects these will have on the final distribution of stresses. Also missing in all of this is consideration of the intrinsic residual stresses in the tubes (as a result of production, roto-straightening, cleanup machining, etc.).

This process also lacks any accurate experimental verification that the calculated stresses and the actual stresses have the same amplitude and distribution. There are various techniques, with varying degrees of workability and accuracy, that may be used to measure the residual stress

state in components. These include hole drilling and other strain-gaging dependent material removal procedures, X-ray, or neutron, or electron beam diffraction techniques, and Mossbauer spectroscopy. To our knowledge, no CRDM assembly has ever been evaluated using any of these techniques[3]. There is one reference (1994 EPRI Alloy 600 Workshop, Reference 5) to a physical sectioning experiment that indicated an intrinsic residual stress of 18 ksi. In my estimation, test programs to measure actual stress distributions in CRDM nozzles (or a mockup of actual proportions and assembly procedures) should have a very high priority. A lot of funding is going into presumably accurate FEA, and at some point - the earlier the better - those endeavors need to establish a credible link with reality – an experimental validation. Better descriptions of vendor-to-vendor procedural differences, and the impact of these on residual stress distributions, are needed to complete the background of this picture and provide a feeling for the possible spread in the values.

As part of the fabrication process of the replacement head for Oconee-3, B & W - Canada fabricated mockups of the vessel head penetration, weld prep and weld deposit. RES is engaged in a dialogue with Duke Power to procure at least one of those mockups. While we do not have a Statement of Work prepared for testing of this mockup, we anticipate that experimental measurement of residual stresses would be an important aspect of an NRC-funded research program. The MRP is also considering a program involving the fabrication of about eight to ten mockups, from B & W – Canada, for programmatic objectives that have not been described to the NRC. The author has received some information that stress analysis would be a part of a future MRP test program.

The introduction of a crack into the model, and the ensuing fracture mechanics treatments are very time consuming. The elastic, and elastic-plastic fracture toughnesses, K or J, are generally calculated from formulas involving crack tip opening displacement and stress. Since the stresses are high, at least at the beginning, some cracks may be controlled by elastic-plastic considerations. As the mesh is “unzipped”, simulating crack extension, the stress distribution, and K or J need continuous recalculation to account for the change in compliance of the nozzle and head combination.

4.2 Incorporation into susceptibility evaluation

At this point in time, the results of CRDM residual stress analyses are taking the kind of shape which makes them useful in the near term for this application. The results could be used to predict the susceptibility for crack initiation (using the Yonezawa fourth power formulation), or to predict crack growth rates, using a fracture-mechanics-based treatment. In any case, in order to keep things non-dimensional, the variables will have to be normalized by some reference variable, as is shown in Eq. 3. Although it seems daunting to think about in the context of the moment, an individual treatment of both components (a time for initiation, with Q = 51 kcals/mole, plus a time for propagation, with Q = 30 kcals/mole) can at least be considered.

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[3] Several experimental analyses of residual stresses for pressurizer nozzles have been completed. Preliminary results using an electron diffraction technique (SEM channeling patterns) will soon be available. The author’s opinion of this work is that the calibration of these patterns leaves a lot to be desired.

Some results for center-hole nozzles are reasonably well-accepted, and generate K-values that couple with known crack growth rate laws to produce amounts of crack extension that seem reasonable. Some studies (Ref. 6) indicate that the principal stress (computed by resolution of the axial, tangential and radial stresses) is inclined to the axial direction (see Figure 2), suggesting that the crack plane would be similarly inclined, oriented normal to the principal stresses to support crack propagation in Mode I. It is possible that the recent salvaging of cracked CRDMs from discarded heads (North Anna 2, and the Oconee plants in the near future) will allow fractography to be completed on naturally formed, in-service cracks.

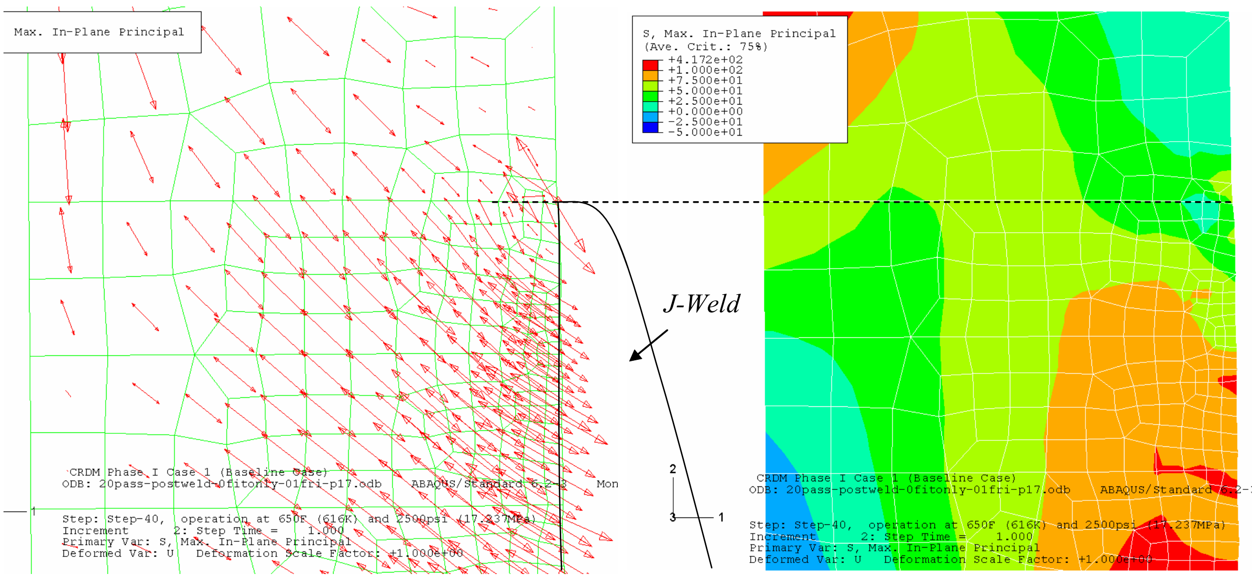

Figure 2 Distribution of maximum in-plane stress in the tube near the J-weld root for a center-hole with a 20-pass weld nozzle.

Once we have convincing evidence that FEA modeling is successful, and we understand the impact of vendor procedures, operational procedures, and other relevant considerations on the resulting stress distributions, then we will have a useful tool that is ready to be incorporated into a susceptibility evaluation.

5. Microstructural Analyses

For Alloy 600 steam generator tubing, it is well-established that coverage of Alloy 600 grain boundaries by a carbide distribution enhances resistance to IGSCC in a very significant way, and there are experimental results that thicker sections of this alloy behave similarly. There is virtually complete agreement in the literature on this conclusion, but also a caveat that intragranular carbide distribution may have a negative effect (i.e., increases the rate of crack propagation).

In order to apply this concept as a factor in the susceptibility index, we must have an evaluation of grain boundary coverage for the nozzles in question. This may be obtained either from archived materials (which are rare for the older plants, at least in the US), from CRDMs contained in discarded heads, or from metallographic replicas taken during plant outages. The

procedure for taking replicas from working plants is well-established, and it is possible that some vendors may have a database of results. Also, replicas, or observation of full metallographic mounts, of CRDM materials of discarded heads would be useful in calibrating the susceptibility model. This information could be factored into the computation of the susceptibility index.

6. Influence of Yield Stress

Figure 4 shows the dependence of Alloy 600 crack growth rate on yield strength of the test samples. For this study (Reference 7), grain boundary carbides were kept low (< 35%), and the yield strength differences were due to other microstructural variables, but not due to cold work, which was 0% for all specimens. Another aspect of this same study showed that the effects of cold work were dynamic for lower yield heats (< 400 MPa), and moderate for higher yield heats. Given that the yield stress can be somewhat dependent on the microstructure of Alloy 600, and on the details of the carbide distribution in particular, it can be difficult to separate out any unique dependence on yield from the dependence on intergranular carbide coverage.

The descriptions above reflect the fact that these variables are not single-acting, and often the trend attributable to one is trumped by the adverse influence of another. These considerations must be taken into account when attempting modifications to the susceptibility calculation.

7. Field Experience

The costs of 100% volumetric inspection of heads, which is required at every outage for the high susceptibility plants, dictate that replacement of the head is less expensive, both financially, and in terms of dose. In 2003 alone, nine US plants will replace heads. Potentially, this makes available a large amount of material for research and testing to determine crack growth rate properties and microstructural correlations. While the materials are contaminated and slightly activated, resulting in substantially increased testing costs, the data obtained may be worth the increased effort.

The MRP has produced a listing of the cross-correlation of Alloy 600 heats in the heads of US plants. Sorting through list allows compilation of a table showing which other plants have head penetrations fabricated from the same heats of materials as those found in a specific, discarded head. Table 1 shows such a cross-correlation for the North Anna 2 head, removed in November, 2002. Some of the correlations shown are moot, since the heads at North Anna 1 and Surry 2 are also going to be replaced in 2003. The other plants shown might acquire some benefit (or possibly not!) from better definition of the properties of the materials contained in their head penetrations.

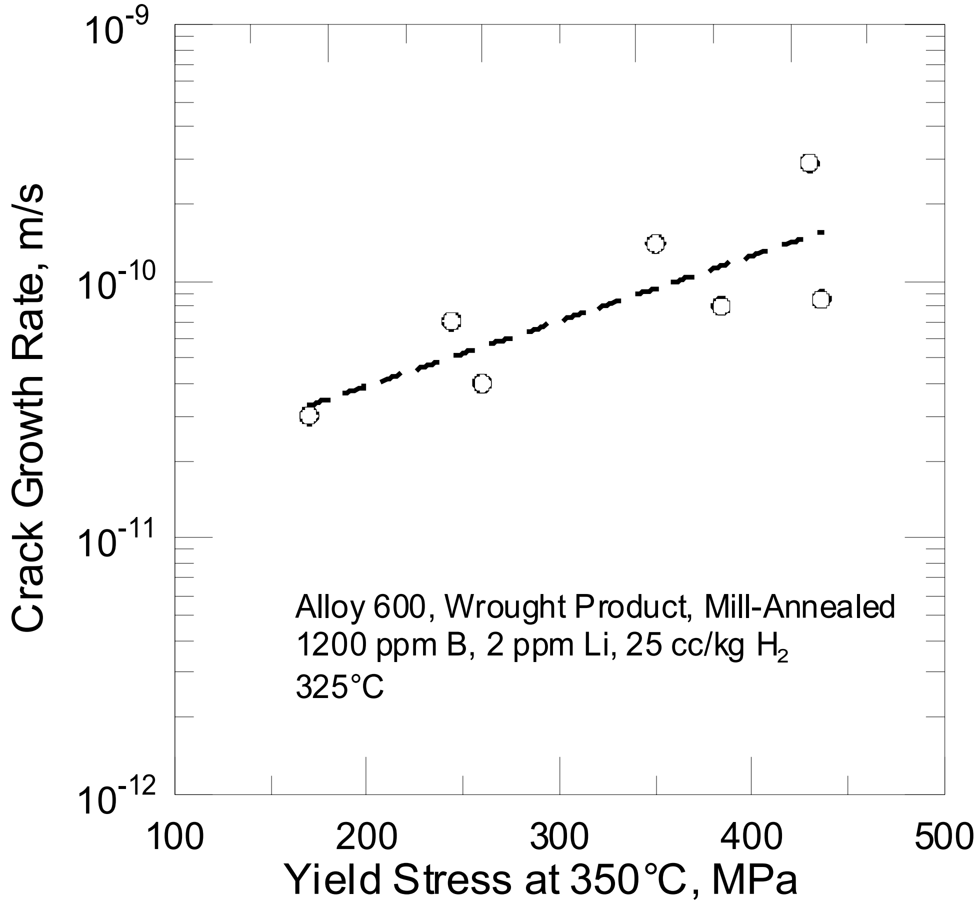

Figure 4. Stress corrosion crack growth rate of alloy 600 in simulated PWR environment as a function of yield strength.

Table 1. Cross-correlation of the CRDM penetration heats in the North Anna 2 head with those in other heads of operating plants.

Heat Identification	Other Plants With Heads Containing Same Heat of Material
755534, 755535, 755536, 755537, 755538, 570892, 568011, 710209	North Anna 1, Sequoyah 1
710147	North Anna 1, Sequoyah 2
71207, 71208,710210	North Anna 1, Sequoyah 1, Sequoyah 2
71206	North Anna 1, Surry 2, Sequoyah 1, Sequoyah 2
772024	Watts Bar-1, Watts Bar-2, Catawba-1, McGuire-2

8. Expected, Future Improvements

Figure 1 suggests both the benefit and the deficiencies in the susceptibility model. As discussed earlier, the model accurately predicts the discovery of cracking in several plants, and offers a borderline (or worse) prediction for others (Cook 2, Millstone 2 and perhaps Davis-Besse). Conversely, many plants with computed values well into the high susceptibility regime show no signs of cracking. Plant experience such as that at Oconee Unit 2, which had a "clean" inspection at EDY = 15.7, and found cracks during the inspection at EDY = 22.1 is testimony to the statistical nature of the failure process(es).

The current regulatory environment that forces complete and thorough inspections of all plants will produce a wealth of data that will add much statistical weight to the database. However, the plants in the high susceptibility regime that are not experiencing degradation by cracking may be expected to wonder whether the expense of inspection is warranted. It seems reasonable to expect requests for relaxation may be filed from plants in this situation, perhaps reverting to a more complete treatment of susceptibility using the supplemental factors discussed in this document. Information on yield strength of heats of CRDM nozzle material is readily available. The author is aware that databases on carbide distribution are maintained by at least one vendor. This information could be plausibly used by a licensee seeking relaxation from inspection requirements. For the NRC, acquiescence could be very difficult, since many of the critical variables seem to exhibit synergisms with each other that are far from understood.

There are several, on-going laboratory research programs that are expected to produce substantial information on the relationship of Alloy 600 crack growth rates to yield strength, cold work and microstructure. A coordinated program in Japan has been underway for about three years, with published results expected mid-2003. Some individual presentations evolving out of that program have been orally presented at selected meetings (ICG-EAC, and the March '03, NRC-sponsored nickel-base alloy conference). The author expects that the 11th Environmental Degradation Meeting in August 2003 will be a forum for presentations of many of the Japanese results. The NRC is funding SCC testing at Argonne National Laboratory. Among the tasks in that program is evaluation of the CGRs of Alloy 600 from Nozzle #3, and Alloy 182 from the J-weld of Nozzle #11 taken from the discarded Davis-Besse head. Testing programs in Sweden, France and Spain are continuing also. Lastly, the ICG-EAC group has commenced a round robin test program on Alloy 600. With rare exceptions, most laboratories will test the samples (which will all be identically prepared) at only one stress intensity factor level, and in either a BWR or PWR environment, depending on the most available laboratory setup. In the more distant future, a similar round robin test program for Alloy 182 has been conceptualized, and materials have been procured.

The situation with Alloy 182/82 is a little worse, in the sense that there is far less crack growth rate data available, and the dependence of that data on the variables discussed in this report is much more sketchy. There is, however, general consensus that crack growth rates are about a factor of five faster in Alloy 182 than in Alloy 600. The MRP is introducing an effort to collect, validate and collate crack growth rate data for Alloy 182 using the MRP-55 document

preparation procedure as a model. More optimistically, I believe that there is more crack growth rate testing on Alloy 182 now underway (in the worldwide sense) than there is for Alloy 600.

Test programs for thick section Alloy 690 and its companion weld metals, Alloy 152/52 are virtually non-existent. The NRC-funded EAC program at Argonne will begin testing these alloys in 2004.

The NRC continues to fund development of finite element models of nozzles at various locations on the head, including effects of other critical variables, such as yield strength and weld deposit geometry. The author is not aware of published stress analysis data from non-domestic sources. The activities of the NRC-instigated International Cooperative Program on SCC and NDE (Non-Destructive Examination) of Dissimilar Metal Welds and Alloy 600 may provide a forum for presentations on stress analysis and developmental NDE technologies from a wide-range of sources.

9. Summary

The time-at-temperature model emerged about ten years ago in a form that contained several variables other than temperature. Initially, little reliable results were afforded through the use of stress, microstructural or yield strength factors in the formula, and they were omitted to avoid misleading results. At this point in time, there is a great deal more data available, and a better understanding of much of it. Many of the chances for improvement hinge on data that is emerging from on-going programs, making helpful improvement a real possibility in the near term.

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