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Potential drop detection of creep damage in the vicinity of welds

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Potential drop detection of creep damage in the vicinity of welds

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ABSTRACT

Recent research studies indicated that directional Alternating Current Potential Drop (ACPD) measurements could be exploited for nondestructive evaluation (NDE) of creep damage. Unfortunately, creep damage is often concentrated in welded sections where the inspection is less straightforward. In this work, a directional square-electrode ACPD probe with spring-loaded contact pins was used to evaluate different levels of creep damage in the vicinity of weldments. The electric transfer resistance is measured in two orthogonal directions to detect creep-induced material damage. This is particularly difficult close to weld/base-metal interfaces because the electric conductivity might change significantly from one side of the interface to the other and therefore exhibit an apparent anisotropy caused by the influence of the boundary. The feasibility of directional ACPD evaluation of creep damage in the vicinity of such transition regions was studied by experimental, analytical, and numerical means. FE simulations validated the analytical predictions and experimental observations and offered an opportunity to further investigate critical effects, such as the inclination angle of the interface relative to the surface.

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LIST OF SYMBOLS

a	Nominal electrode separation
A	Induced electric anisotropy
A_a	Normalized “apparent” anisotropy
$A(x_s, y_s)$	Source point on the quarter space of electrical conductivity σ_1
$A'(x_s, -y_s)$	Reflected image of the source point on the quarter space of electrical conductivity σ_1 in every direction
$B(x, y)$	Sensing point on the quarter space of electrical conductivity σ_1 for the same side of the interface and of electrical conductivity σ_2 for the opposite side of the interface
D	Directionality factor of the probe
$E(r)$	Radial component of the spherical electric field
f	Frequency of the injection current
F	Gauge factor
G	Geometrical gauge factor
h	Distance from the centerline of the square-electrode probe to the boundary separating the two conducting quarter-spaces
I	Injection current
I_1	Current injected in the axial direction
I_2	Current injected in the lateral direction
I'	Reflected image of the injection current
I''	Transmitted image of the injection current

J	Current density
M	Material gauge factor
r	Distance from the source
R	Measured transfer resistance
R_0	Initial electrical resistance
ΔR	Change in resistance
R_1	Axial transfer resistance
R_2	Lateral transfer resistance
R_{10}	Initial axial transfer resistance
R_{20}	Initial lateral transfer resistance
$R_p^{(11)}$	Parallel resistance for a probe on the first side (σ_1) of the boundary
$R_n^{(11)}$	Normal resistance for a probe on the first side (σ_1) of the boundary
$R_p^{(12)}$	Parallel resistance for a probe when the injection point is on the first side (σ_1) and sensing point is on the other side (σ_2) of the plane boundary
$R_n^{(12)}$	Normal resistance for a probe when the injection point is on the first side (σ_1) and sensing point is on the other side (σ_2) of the plane boundary
$R_p^{(22)}$	Parallel resistance for a probe on the second side (σ_2) of the boundary
$R_n^{(22)}$	Normal resistance for a probe on the second side (σ_2) of the boundary
R_a	Normalized average resistance
R_r	Reference resistance for an ideal square-electrode probe
t	Transition layer thickness in finite element simulation
$V(r)$	Electric potential distribution
V_o	Electric potential very far away from the injection point

V_1	In-phase voltage drop in the axial direction
V_2	In-phase voltage drop in the lateral direction
$V^{(11)}$	Electric potential when the injection and sensing points are located on the first side (σ_1) of the plane boundary separating two conducting quarter spaces
$V^{(12)}$	Electric potential when the injection point is on the first side (σ_1) and sensing point is on the other side (σ_2) of the plane boundary separating two conducting quarter spaces
$V^{(21)}$	Electric potential when the injection point is on the second side (σ_2) and sensing point is on the first side (σ_1) of the plane boundary separating two conducting quarter spaces
$V^{(22)}$	Electric potential when the injection and sensing points are located on the second side (σ_2) of the plane boundary separating two conducting quarter spaces
Λ	Resistance coefficient
δ	Electromagnetic skin depth beneath the surface
ε	Axial strain
ε_e	Elastic strain
ε_p	Plastic strain
μ	Magnetic permeability of the material
ν	Poisson's ratio
η_1	Normalized resistance in the axial direction
η_2	Normalized resistance in the lateral direction
$\eta_{1,2}$	Normalized resistance
ρ	Resistivity of the specimen

ρ_0	Initial electrical resistivity
$\Delta\rho$	Change in resistivity
ξ	Normalized resistance ratio
ξ_g	Geometrical factor of the normalized resistance ratio
ξ_m	Material factor of the normalized resistance ratio
σ	Electrical conductivity

CHAPTER I

1. INTRODUCTION

1.1. STATEMENT OF THE PROBLEM

Creep occurs in engineering applications where mechanical stresses act over long periods of time at elevated temperatures. Creep causes large permanent strain well in excess of the maximum elastic strain possible in the same material. Creep strain is accompanied by microstructural changes commonly referred to as creep damage which initially include dislocation formation and movement, precipitate formation and coarsening, and generation of diffusion vacancies. Based on these changes, creep is identified as three-stage process which is summarized in the plot of strain versus time shown in Figure 1.1 [1]. An instantaneous strain is observed after the load is applied followed by the deceleration of the strain rate due to strain hardening process until a steady creep rate is achieved which characterizes the primary creep stage. The secondary creep stage is characterized by a slow but constant plastic flow of material representing a balance between strain hardening and the softening and damage processes. Whereas in the tertiary stage of creep, due to the onset of internal- or external-damage processes, a decrease in the resistance to load or an increase in the overall cross section stress occurs, which accelerates the strain rate and material eventually fails [1].

Ultimate failure usually occurs when directional clusters of grain boundary voids coalesce first into microcracks and then give rise to a terminal crack that quickly grows to critical size and causes rupture. Detectability of creep damage in its early stages is of major importance because cracks may develop and propagate very rapidly as rupture approaches. These cracks are most likely to originate in the critical areas of pre-existing defects or stress concentrations. The most critical locations tend to be in the vicinity of welds. Depending on their location, creep cracks have been classified into four types as shown in Figure 1.2 [1]. Type I cracks develop in the weld metal. Type II cracks form in the weld metal and propagate towards the coarse grains in the heat-affected zone (HAZ). Type III cracks originate in the coarse-grain region of the HAZ, whereas Type IV cracks develop in the transition region between the fine-grain region of the HAZ and the base metal, where the creep strength is typically lower. This transition region between the HAZ and the base metal tends to have the highest concentration of diffusion cavities and is the most likely area for creep crack initiation due to hoop, residual or bending stresses [2-7].

Monitoring creep damage accumulation in its early stage has been a major concern in the NDE community for quite some time. Studies have been conducted in the past where cavities were detected shortly before the rupture using NDE techniques. Problem arises in predicting the remaining life of the component under creep condition for which many NDE techniques have been investigated over the years. One of the commonly used NDE techniques in creep damage detection is replica metallography [1]. This technique provides high resolution quantitative data to identify defects such as voids and microcracks. However, in some replica samples subsurface defects could not be detected. Also studies on headers and end caps damage showed that macrocracks initiate below the surface before aligned cavities are visible on the replica [8]. Due

to these limitations, researchers have been searching for other possible options. In this subject, various NDE techniques are being studied to develop a technique that is sensitive to early stage creep damage degradation and that can detect localized creep. From numerous NDE methods, potential drop measurement emerged as one of the most promising options [1].

Figure 1.1 Typical creep curve with different stages of creep damages (from Ref. [1]).

Figure 1.2 Different types of creep cracks in weldments (from Ref. [1]).

1.2. LITERATURE REVIEW

1.2.1 Potential drop techniques

Potential drop (PD) techniques help characterize geometrical variations and detect surface-breaking cracks depending on the distribution of electric potential on the surface of the material. Based on the penetration depth of the injected current as a result of either the characteristic of the specimen, or the geometry of the electrode, or the electromagnetic skin property, PD techniques are divided into direct-current potential drop (DCPD) and alternating current potential drop (ACPD) techniques. Both these techniques rely on geometrical and material properties and the inspection frequency.

DCPD techniques have been used to measure the thickness of metal plates, pressure vessels, boilers, tubes, and castings since the 30's [9]. Figure 1.3 shows the schematic diagram of potential drop measurement using an in-line probe configuration. The current is injected at the outer two points and the potential difference is measured between the inner two other points. This type of measurement mostly uses spring-loaded electrodes but in the case of in-situ

measurements, permanently installed welded electrodes are used. Since a high contact resistance is observed between the measuring electrodes and the inspection material, potential drop method is always considered as a four-point measurement method. The transfer resistance for four-point configuration is

$$R = \frac{\Delta V}{I} = \rho \Lambda, \quad (1.1)$$

where ρ is the resistivity of the specimen and Λ is the resistance coefficient. In the presence of a crack, the resistance coefficient depends on length, breadth and orientation of the crack, whereas in the absence of crack, it only depends on the electrodes separation and the plate's thickness. The variation in the potential drop observed for a given crack correlates to the crack geometry based on the configuration of the electrode, the resistivity of the material and the excitation current.

The disadvantage of DCPD method is that low transfer resistance obtained for a given injection current is constrained only by the separation of the electrode and the geometry of the inspection specimen. As a result, to accurately measure the potential on larger geometries, injection currents on the order of 10 A has to be used [10].

On the other hand, Alternating current potential drop (ACPD) measurement is another electromagnetic technique used mainly for monitoring crack growth [11,12], estimating the depth of surface breaking defects [13,14] and evaluating material properties such as conductivity or permeability [15,16]. It uses a four-point probe where current is injected into the inspected specimen through one pair of electrodes and the resulting voltage difference between two other points on the surface is measured using a second pair of electrodes. In typical ACPD inspections the injected current is limited to a shallow electromagnetic skin depth δ beneath the surface, which can be controlled by the inspection frequency as follows

$$\delta = \frac{1}{\sqrt{\pi f \mu \sigma}}, \quad (1.2)$$

where f is the frequency of the injection current, μ is the magnetic permeability of the material and σ is the electrical conductivity. At very low frequencies, the current distribution asymptotically approaches the static distribution of the direct current potential drop (DCPD) method with a geometrical penetration depth roughly equal to half the distance between the injection electrodes. The main advantage of ACPD mode of operation over DCPD is that the same injection current produces much larger potential differences [17]. Using this technique the measured resistance increases when the probe overlaps a defect because the injected current is forced to travel around and underneath the defect, such as a surface breaking crack, which leads to increasing potential drop between the sensing electrodes [18,19]. Figure 1.4 shows the current distribution in intact and cracked specimens using DCPD and ACPD inspection techniques [20]. In the DCPD scenario the injected current spreads out through the whole cross section of the specimen limited only by the electrode separation whereas in the ACPD case the penetration depth is readily controlled by the inspection frequency through the electromagnetic skin depth.

Figure 1.3 Schematic diagram of potential drop (PD) measurement.

Figure 1.4 Current distribution in intact and cracked specimens using DCPD (a) and ACPD (b) inspection (from Ref. [20]).

1.2.2 Geometrical gauge factor

The following work was investigated in Ref. [21], i.e., E. Madhi and P. B. Nagy, in *Rev. Progr. Quant. Nondestr. Eval.* Vol. 30, AIP, New York (2011) pp. 1623-30.

An effective sensor based on directional potential drop methods was developed for in-situ monitoring of creep in metals, specifically power plant steels [22]. The sensor consists of four chromel thermocouple wires spot-welded on to the test material in a square-electrode configuration as shown in Figure 1.5. This set-up allows for transfer resistance measurements in the axial and lateral direction quasi-simultaneously to measure the potential drop and distinguish

the creep-induced anisotropy in the electrical resistivity. Large isotropic variations in electrical resistance due to thermal exposure are suppressed with this sensor by monitoring the resistance ratio.

Figure 1.5 Spot-welded chromel thermocouple wire electrodes in a 4-mm square electrode configuration on to a stainless steel 304 specimen (from Ref. [23]).

The gauge factor of a strain gauge is

$$F = \frac{\Delta R / R_0}{\varepsilon}, \quad (1.3)$$

where ΔR is the change in the resistance, R_0 is the initial electrical resistance and ε is axial strain within the elastic limit of the gauge material. The gauge factor is also the sum of geometrical and material contributions

$$F = G + M. \quad (1.4)$$

The geometrical gauge factor is $G = 1 + 2\nu$, where ν is Poisson's ratio. The material gauge factor is $M = (\Delta\rho/\rho_0) / \varepsilon$, where $\Delta\rho$ is the change in the resistivity and ρ_0 is the initial electrical resistivity.

Typical strain gauges have G range from 1.6 to 1.7 and M range from 0.3 to 0.5, giving a gauge factor of about 2, which guarantees optimal thermal stability [24].

As stated earlier, the creep sensor measures both axial and lateral resistances, $R_1 = V_1 / I_1$ and $R_2 = V_2 / I_2$, respectively, as shown in Figure 1.6. These measured resistances are normalized to their initial intact values $\eta_{1,2} = R_{1,2} / R_{10,20}$, where R_{10} and R_{20} are the initial intact values of the axial and lateral resistances respectively. The normalized resistance ratio between axial and lateral directions is then calculated as $\xi = \eta_1 / \eta_2$. This normalization helps detect small strain-induced changes in the presence of large reversible resistivity changes which in power plant steels often exceed 100-200% during creep over a typical service temperature range of 500-700°C whereas at early stages of creep, the increase in ξ is only a few percent [23]. Furthermore, ξ is approximated to be

$$\xi \approx 1 + g \varepsilon_e + G \varepsilon_p + m \varepsilon_e + M \varepsilon_p, \quad (1.5)$$

where ε_e and ε_p are the elastic and plastic strains, g and G are the linearized geometrical gauge factors in the elastic and plastic regimes, and m and M are the linearized material gauge factors in the elastic and plastic regimes. The normalized resistance ratio for small changes can be further approximated as $\xi \approx \xi_g \xi_m$, where $\xi_g \approx 1 + g \varepsilon_e + G \varepsilon_p$ is the geometrical factor and $\xi_m \approx 1 + m \varepsilon_e + M \varepsilon_p$ is the material factor of the normalized resistance ratio.

The sensitivity of the creep sensor to geometrical variations were studied theoretically by Madhi and Nagy by deriving the resistance ratios for three different scenarios, i.e., a thin plate, a half-space, and plates of finite thicknesses. It was found that the geometrical gauge factor ranged from 3.8 to 4.4 in the elastic regime and between 4.3 and 5.1 in the plastic regime. This geometrical sensitivity was experimentally studied by spot-welding a 4-mm separation square electrode configuration potential drop probe on a stainless steel 304 specimen. The specimen was exposed to uniaxial loading and the resistance ratio was monitored at a frequency of 3 Hz. Based

on the experimental results, the geometrical gauge factor was determined to be around 4 and 5 in the elastic and plastic regimes respectively. From the geometrical gauge factor study it was observed that the resistance ratio is proportional to the creep strain in the initial stage but increases rapidly at the later stages due to the formation of oriented grain boundary cavities and microcracks in the material.

Figure 1.6 Schematic diagrams of DACPD measurements in the orthogonal x_1 (a) and x_2 (b) directions (from Ref. [23]).

1.2.3 Material gauge factor

The following work was investigated in Ref. [25], i.e., E. Madhi and P. B. Nagy, in *Rev. Progr. Quant. Nondestr. Eval.* Vol. 30, AIP, New York (2011) pp. 1233-40.

The material gauge factor depends on the directionality factor of the probe and piezoresistivity of the material. The directionality factor of the probe determines its sensitivity to anisotropy of the electrical conductivity. Although, in general piezoresistivity refers to stress-dependence of the electric resistivity of the material, here we mean strain-dependence. In the normalized resistance ratio, the material factor can be further approximated as

$$\xi_m \approx 1 + D A, \quad (1.6)$$

where $A = (\rho_1 - \rho_2) / [(\rho_1 + \rho_2) / 2]$ is the induced electric anisotropy that is approximately proportional to the strain in the material, and ρ_1 and ρ_2 are the resistivity values in the x_1 and x_2 principal directions, respectively [23]. $D \approx (\xi_m - 1) / A$ is the directionality factor of the probe [23].

Since the piezoresistivity effect is small, the directionality of the probe was optimized to maximize the material gauge factor. Directionality of an in-line probe was compared to the directionality of a square-probe configuration on an anisotropic thin plate and on an anisotropic half-space. It was determined that the directionality of the square-electrode configuration was between 1.44 and 1.71 whereas that of an in-line probe was between -0.5 and 0. Furthermore, the piezoresistivity coefficients were studied using directional square-electrode configuration. The magnitude of elastic piezoresistivity coefficient which is an intrinsic material parameter ranged from 0.5-1.0 and the magnitude of plastic piezoresistivity coefficient was in the range of 1-2% for typical creep levels which was well below the measured elastic piezoresistivity coefficient

[23]. It was observed that the sensitivity of the square-electrode configuration to material effects was limited in terms of piezoresistivity effects both in the elastic and plastic regimes.

The material gauge factor on stainless steel 304 was measured between 0.1 and 0.12 in comparison to the geometrical gauge factor between 4 and 5, which showed that the creep sensor is more sensitive to geometrical effects than to material effects [21].

When thermo-mechanical deformation occurs, material deterioration increases over time as a function of temperature and mechanical stress, in which carbides transform and precipitate from the initial microstructure and then microcracks begin to form [26]. Electric resistivity increases due to these microcracks which appear along the grain boundaries orthogonal to the applied stress [27-29]. As the microcracks increase in size to a certain threshold, the effect on the normalized ratio is apparent. Figure 1.7 shows this effect when stainless steel 304 was tested for creep monitoring using spot-welded square-electrode configuration [23].

Figure 1.7 Normalized resistance ratio on a stainless steel 304 specimen loaded between 190 MPa and 290 MPa at 600°C (from Ref. [23])

Figure 1.8 shows the graph of resistance ratio versus creep life illustrating the dominance of geometrical effect. The normalized resistance ratio increased sharply towards the later stage of creep life indicating that the material gage factor becomes significant only in the tertiary stage of creep just before failure [23]. This material effect can be analyzed using deployable directional spring-loaded ACPD probes due to the fact that they are not spot-welded to the specimen, eliminating the geometrical effect.

Figure 1.8 Graph of resistance ratio versus creep life.

1.3 ORGANIZATION OF THE REST OF THE THESIS

The above work tried to provide better understanding about creep, weld, potential drop techniques, permanently mounted creep sensor, and its sensitivity, and how they can correlate to each other. The present work comprises of eight additional chapters organized according to the research sequence, i.e., each following chapter is proposed and developed from the problems and observations encountered in or from observations of previous chapters. This thesis discusses the feasibility of low-frequency directional ACPD (DACPD) evaluation of creep damage close to the boundary separating a weld from the base metal of typically significantly higher electric

conductivity. In a previous study, only permanently installed probes were considered that are mainly sensitive to the purely geometrical effects of creep due to plastic strain [23, 30]. In this study we focus on deployable probes that are sensitive to the material effects of creep only. Experimental, analytical and computational results are presented to quantitatively assess the apparent local electric anisotropy produced by the interface between regions of different electric conductivity, which ultimately limits the detection threshold of creep damage in the vicinity of welds.

Chapter II focuses on the application of deployable directional alternating current potential drop (DACPD) probe used to measure electrical anisotropy to detect selectively material changes only. To illustrate this property, measurements were carried out on stainless steel 304 plate and Ti-6Al-4V plate. It also addresses the accuracy and uncertainties encountered during the measurements using DACPD probe.

Chapter III illustrates the application of DACPD probe to detect creep damage location on 0.5%Cr-0.5%Mo-0.25%V low alloy steel pipe segments supplied by RWE npower which includes the three regions, i.e., the base metal, the interface or the fusion zone, and the weld. This chapter evaluates the experimental results for average resistance and anisotropy obtained on the creep samples from the base metal, through the interface, to the weld, and compares it to the creep damage location. The average resistance increases from the base metal, through the interface region, to the weld which corresponds to the change in conductivity between the base metal and the weld. Also the anisotropy peaks at the interface region between the base metal and the weld that corresponds to the creep damage location which is mostly concentrated at the transition region between the HAZ and the base metal, but this anisotropy peak is also caused by

the change in conductivity between the base metal and the weld which creates discrepancy in detecting creep damage in the vicinity of welds.

Chapter IV describes the approach (method of images) used to create the analytical model and derive the equations, to study the effects on average resistance and anisotropy from the base metal, through the interface, to the weld, on specimens similar to that of the 0.5%Cr-0.5%Mo-0.25%V low alloy steel pipe segments supplied by RWE npower that included three regions, i.e., the base metal, the interface or the fusion zone, and the weld. A material property such as change in conductivity between the base metal and weld was taken into consideration. Also the model used in this chapter considers normal boundary at the interface region of the base metal and weld.

Chapter V focuses on the validation of the analytical predictions by comparing the experimental results on 0.5%Cr-0.5%Mo-0.25%V low alloy steel pipe segments supplied by RWE npower from Chapter III with the analytical predictions from Chapter IV and explains the variation in the results obtained. Furthermore this chapter also includes the validation of the analytical predictions from Chapter III by comparing it to the numerical solution obtained using Finite Element tool ANSYS 11.0. It also addresses the reasons for the unevenness in the anisotropy of the analytical predictions and numerical solutions with the experimental results, i.e., the abrupt conductivity transition and boundary inclination between the weld and the base metal.

Chapter VI illustrates the two effects, i.e., the abrupt conductivity transition and boundary inclination between the weld and the base metal, which is thought to lead into the discrepancy in the validation of the analytical predictions and numerical solutions with the experimental results

and the proposed solution on whether these effects could be overcome to eliminate the discrepancy in the results.

Chapter VII focuses on the effect of thermal exposure on the average resistance and intrinsic anisotropy of the weld and base metals separately to examine the behavior in anisotropy as a result of thermal exposure independent of mechanical stress.

Finally, Chapter VIII presents the summary of the present work including conclusions and recommendations for future efforts.

The following work presented in this thesis was performed in the Nondestructive Evaluation Laboratory at the University of Cincinnati, and is extracted from Ref. [31], i.e., S. Prajapati, P. B. Nagy, and P. Cawley, *NDT&E Int.* **47**, 56-65 (2012).

CHAPTER II

2. DEPLOYABLE DACPD PROBE

2.1. INTRODUCTION

A spring-loaded DACPD probe can be used to measure the electrical anisotropy of conductors. Figure 2.1 shows an image of the deployable square-electrode DACPD probe. It consists of four spring-loaded heavy-duty pins with 4-mm separation that are embedded in a Plexiglas fixture. The technique uses low-frequency ACPD to measure two resistances in two orthogonal directions at the same location as shown in Figures 1.6 (a) and (b), respectively. The “axial” transfer resistance $R_1 = V_1/I_1$ is obtained by injecting current I_1 at electrodes A_1 - A_2 and measuring the resulting in-phase voltage drop V_1 between electrodes C_1 - C_2 . The “lateral” transfer resistance $R_2 = V_2/I_2$ is obtained by injecting current I_2 at electrodes A_1 - C_1 and measuring the resulting in-phase voltage drop V_2 between electrodes A_2 - C_2 . Since the electrodes of the probe are spring-loaded, the electrode separation a can be assumed to be constant so the probe is not sensitive to geometrical effects and can be used to detect selectively material changes only.

Let us assume that the nominal electrode separation is $a = 4$ mm. The best commercially available spring loaded electrodes exhibit ± 50 μm targeting accuracy which, unless further

reduced, could cause as much as $\Delta a/a \approx \pm 2\%$ relative electrode separation uncertainty. Since the geometrical gauge factor of the DACPD probe is around $G \approx 5$ [23], such positioning uncertainty causes as much as $\pm 10\%$ random variation in the sought R_1/R_2 resistance ratio, which is unacceptable in most measurements. Therefore, we further reduced the electrode positioning uncertainty by the adding a Plexiglas guiding plate to the front of the probe as shown in Figure 2.1.

Figure 2.1 A picture of a deployable DACPD probe.

2.2. EXPERIMENTAL SETUP

The experimental setup used in this work is the same as the one used by Madhi et al. for the creep sensor shown in Figure 2.2, except a deployable ACPD probe with spring-loaded electrodes is used instead of permanently installed welded electrodes [23]. A user-defined LABVIEW version 7.1 program was created to control the instruments remotely and to obtain the measured data in the desired format [23]. A signal generated by the internal oscillator of SR 830 lock-in amplifier passes through SIM983 differential output amplifier. The differential output amplifier minimizes the common mode signal of the inspection specimen to help make

accurate measurements. The current travels through an LTS 6-NP current sensor to measure its actual value during data processing which could have been affected by the differences in the resistance of the multiplexer's relays, wiring, and the contact resistances. The SR SIM925 multiplexer switches the electrode configuration for resistance measurements in the axial and lateral directions. The measured voltage by the sensing electrodes passed through a 1:8 high-impedance Jensen JT-347 transformer, which then passes through a 100-gain SR552 preamplifier and is finally measured with the SR 830 lock-in amplifier. The experimental setup shown in Figure 2.2 presents excellent reproducibility with high accuracy while operating at low inspection frequency of 3 Hz. This allows for the standard penetration depth to be larger than 4 mm electrode separation. Figure 2.3 shows a picture of the equipments used in the above experimental setup.

Figure 2.2 A schematic diagram of the experimental setup used for monitoring creep (from Ref. [23])

Figure 2.3 A picture of the equipments used in the above experimental setup.

2.3. DIRECTIONAL SENSITIVITY OF DACPD PROBE

In order to illustrate the directional sensitivity of the DACPD probe, electrical anisotropy measurements were conducted on an isotropic stainless steel 304 plate and on an anisotropic textured Ti-6Al-4V plate. The results of these DACPD resistance measurements are shown in Figures 2.4 (a) and 2.4 (b), respectively. One hundred readings were taken at the center of the plates at two orthogonal probe orientations alternating between axial (0°) and lateral (90°) directions. Rotation of the probe was necessary to suppress inevitable small differences between the electrode separations in the two measurement directions. Each resistance data point is the average of the two readings, i.e., the 0° -measurement taken at axial probe orientation was averaged with the 90° -measurement taken at lateral probe orientation. Similarly, the 90° -measurement taken at axial probe orientation was averaged with the 0° -measurement taken at lateral probe orientation. The two averaged values of the electric resistance were then normalized to the combined average resistance.

It is well known that cubic materials do not exhibit crystallographic anisotropy in their electric properties, while those preferentially crystallizing in hexagonal symmetry, like Titanium alloys, do [32]. The normalized resistance results obtained on textured Ti-6Al-4V illustrate that the DACPD probe can easily detect the approximately 4% electric conductivity anisotropy caused by crystallographic texture, while the isotropic stainless steel 304 plate does not exhibit any perceivable anisotropy. The scattering of the data at each orientation is less than 1% and mainly due to the uncertainty in pin positioning that increases the random error of the measurement well above the intrinsic electric noise. Based on these results, this DACPD probe could be used to evaluate creep-induced electric anisotropy above about 1%, so it is expected to be sensitive to material effects of creep in its advanced stages of damage accumulation.

Figure 2.4 Normalized resistance measurements in the axial and lateral directions on an isotropic stainless steel 304 plate (a) and an anisotropic textured Ti-6Al-4V plate (b).

CHAPTER III

3. CREEP ASSESSMENT

3.1. INTRODUCTION

In an exploratory test, electrical anisotropy measurements were performed in three regions (base metal, interface zone, and weld region) on four welded specimens using the deployable DACPD probe shown in Figure 2.1. The specimens were 0.5%Cr-0.5%Mo-0.25%V low-alloy steel pipe segments supplied by RWE npower. All samples were sections of headers with welds, which had been condemned by replica examination after operating for about 150,000 hours at 570 °C and 165 bar. Replica analysis invariably found that more cavities were present in the heat affected zone (HAZ) than in the region farther away from the weld.

3.2. EXPERIMENTAL RESULTS

Table 3.1 lists the widely different cavitation density levels measured by replica inspection in the HAZ of these four specimens. Five sets of DACPD measurements were taken with twenty-five repetitions averaged per location in the three regions as shown in Figure 3.1 (sample #2). As expected, the average resistance monotonically increased from the base metal, through the interface, to the weld region as shown in Figure 3.2 (a). The error bars indicate the

standard deviation of the measurement and include the measurement repeatability as well as random point to point variations parallel to the bondline. The resistivity anisotropy calculated between the normal and tangential directions relative to the weld boundary was low in both the base metal and the weld regions and it peaked in the interface region as shown in Figure 3.2 (b).

The above results indicate that the increase in electric anisotropy correlates well with the creep damage location. At the same time, there is a much higher than expected level of experimental variation in the data that cannot be explained by random electric noise and electrode positioning uncertainties. This added uncertainty occurs mainly in the interface region separating two regimes of different electric conductivity therefore we conducted a combined analytical/computational effort to better understand the nature of this variation.

Table 3.1 Different cavity density levels in the tested four RWE npower creep damage specimens.

Specimen	Cavity Density [mm⁻²]
#1	1200
#2	300
#3	150
#4	45

Figure 3.1 RWE npower creep specimen #2 with five measurement locations (1,2,...5) marked in three regions (base metal, the interface and the weld).

Figure 3.2 The measured average resistance (a) and anisotropy (b) of the four RWE npower creep damage specimens in three regions.

CHAPTER IV

4. APPARENT ANISOTROPY OF NORMAL BOUNDARY

4.1. INTRODUCTION

In order to better understand the overall trends and measurement uncertainties exhibited by the experimental results obtained for the average resistance and anisotropy on four different creep damage specimens over the three distinct regions from the base metal, through the interface, to the weld region as seen in Figures 3.2 (a) and (b) respectively, we first analyzed the simplest case of a normal plane boundary separating two conducting half-spaces. The Method of Images was used to derive the resistance equations at two orthogonal orientations either normal or tangential to the boundary [33].

4.2. METHOD OF IMAGES

Let us assume that two half-spaces of different electric conductivity are separated by a boundary coinciding with the x - z plane of a Cartesian coordinate system as shown in Figure 4.1. A current point source I is located at A on the y axis and a point on the interface is arbitrarily chosen at B on the x axis. Here x -axis is considered tangential and y -axis is considered normal. According to Gauss' law, this current source will produce spherical electric field.

Figure 4.1 Illustration of the Method of Images with the injection and sensing points on the axis on the same side (a) and opposite sides (b) of the plane boundary separating two conducting half-spaces.

In an infinite homogeneous medium the current density $\mathbf{J}$ is given by

$$\mathbf{J} = \frac{I}{4\pi r^2} \mathbf{e}_r, \quad (4.1)$$

where $\mathbf{e}_r$ is the radial unit vector from the source at A to the point on the interface at B. For the same current to travel between the two half-spaces, the interface boundary conditions require the continuity of the normal component of the current density vector

$$J_y(x, 0^+) = J_y(x, 0^-), \quad (4.2)$$

and the continuity of the tangential component of the electric field vector

$$E_x(x, 0^+) = E_x(x, 0^-), \quad (4.3)$$

which can be re-written in terms of the tangential component of the current density vector as follows

$$\frac{J_x(x, 0^+)}{\sigma_1} = \frac{J_x(x, 0^-)}{\sigma_2}. \quad (4.4)$$

Using superposition to re-write the boundary conditions in terms of “reflected” I' and “transmitted” I'' current source images

$$-\frac{I y_s}{4 \pi r^3} + \frac{I' y_s}{4 \pi r^3} = -\frac{I'' y_s}{4 \pi r^3}, \quad (4.5)$$

and

$$+\frac{I x}{4 \pi r^3 \sigma_1} + \frac{I' x}{4 \pi r^3 \sigma_1} = \frac{I'' x}{4 \pi r^3 \sigma_2}. \quad (4.6)$$

These boundary condition equations can be simplified to

$$I' + I'' = I, \quad (4.7)$$

and

$$-I' + I'' \frac{\sigma_1}{\sigma_2} = I, \quad (4.8)$$

and then solved for

$$I' = \frac{\sigma_1 - \sigma_2}{\sigma_1 + \sigma_2} I, \quad (4.9)$$

and

$$I'' = \frac{2 \sigma_2}{\sigma_1 + \sigma_2} I. \quad (4.10)$$

Therefore, Eqs. (4.9) and (4.10) satisfy the boundary conditions for any source point (x_s, y_s) and for any point on the interface (x, y) .

4.3. ANALYTICAL PREDICTIONS

The radial component of the spherical electric field produced by a single current source I on the surface of a homogeneous isotropic half-space can be calculated from Gauss' law as follows

$$E(r) = \frac{J(r)}{\sigma} = \frac{I}{2\pi\sigma r^2}, \quad (4.11)$$

where, σ is the electric conductivity, J is the current density, and r is the distance from the source. The electric potential distribution is

$$V(r) = \int_r^\infty E(r) dr = \frac{I}{2\pi\sigma r} + V_0 = \frac{I}{2\pi\sigma r} + V_0, \quad (4.12)$$

where V_0 is the electric potential very far away from the injection point. In the following, we will neglect V_0 as it will not influence the measured potential differences.

Figure 4.2 (a) shows the geometrical configuration when the source point $A(x_s, y_s)$ and the sensing point $B(x, y)$ are on the same side of the interface separating two quarter-spaces of electric conductivity σ_1 and σ_2 . According to the Method of Images, the electric field on the injection side of the boundary can be calculated by superposition of the direct field produced by the injection current I at $A(x_s, y_s)$ and the reflected image of the injection source I' placed at $A'(x_s, -y_s)$ while assuming that the medium is homogeneous with an electric conductivity equal to σ_1 everywhere. Figure 4.2 (b) shows the corresponding geometrical configuration when the source point $A(x_s, y_s)$ and the sensing point $B(x, y)$ are on the opposite sides of the interface.

Then, the electric field on the sensing side of the boundary can be calculated as the transmitted image of the injection source I'' placed at $A(x_s, y_s)$ while assuming that the medium is homogeneous with an electric conductivity equal to σ_2 everywhere. The physical boundary conditions at the interface, i.e., the continuity of the normal current density and the tangential electric field, can then be satisfied by the appropriate choice of the I'/I reflection and I''/I transmission coefficients as follows [33]

$$\frac{I'}{I} = \frac{\sigma_1 - \sigma_2}{\sigma_1 + \sigma_2} \quad (4.13)$$

and

$$\frac{I''}{I} = \frac{2\sigma_2}{\sigma_1 + \sigma_2}. \quad (4.14)$$

When the injection and sensing points are located on the first side (σ_1) of the plane boundary separating two conducting quarter-spaces, the electric potential can be written as

$$V^{(11)} = \frac{I}{2\pi\sigma_1 r} + \frac{I'}{2\pi\sigma_1 r'} = \frac{I}{2\pi\sigma_1 \sqrt{(x-x_s)^2 + (y-y_s)^2}} + \frac{I'}{2\pi\sigma_1 \sqrt{(x-x_s)^2 + (y+y_s)^2}}. \quad (4.15)$$

On the other hand, when the injection point is on the first side and the sensing point is on the other side of the plane boundary, the electric potential is

$$V^{(12)} = \frac{I''}{2\pi\sigma_2 r''} = \frac{I''}{2\pi\sigma_2 \sqrt{(x-x_s)^2 + (y-y_s)^2}}. \quad (4.16)$$

Substituting I' and I'' from Eqs. (4.13) and (4.14) into Eqs. (4.15) and (4.16), respectively, the potential distribution can be written as

$$V^{(11)} = \frac{I}{2\pi\sigma_1} \left[\frac{1}{\sqrt{(x-x_s)^2 + (y-y_s)^2}} + \frac{\sigma_1 - \sigma_2}{\sigma_1 + \sigma_2} \frac{1}{\sqrt{(x-x_s)^2 + (y+y_s)^2}} \right], \quad (4.17)$$

when the injection and sensing points are on the same side of the plane boundary and

$$V^{(12)} = \frac{I}{\pi(\sigma_1 + \sigma_2)} \frac{1}{\sqrt{(x-x_s)^2 + (y-y_s)^2}}, \quad (4.18)$$

when the injection and sensing points are on the opposite sides of the plane boundary. When the injection and sensing points are both located on the second side (σ_2) of the boundary, the potential $V^{(22)}$ can be obtained from Eq. (4.17) by simply replacing σ_1 with σ_2 and vice versa. Of course, according to the Law of Reciprocity, the injection and sensing electrodes are interchangeable, therefore $V^{(21)} = V^{(12)}$, as one can easily see from the symmetric form of Eq. (4.18).

In the case of parallel and normal potential drop measurements across the boundary, the injection and sensing points might be on either the same side or the opposite sides of the boundary separating the two conducting quarter-spaces as shown in Figure 4.3. Generally, the measured transfer resistance can be calculated from

$$R = \frac{V^{(+)} - V^{(-)}}{I}, \quad (4.19)$$

where $I^{(+)} = -I^{(-)} = I$. Using the combination of potential distribution equations obtained above for $V^{(11)}$, $V^{(22)}$, $V^{(12)}$ and $V^{(21)}$, the parallel and normal resistances can be readily derived.

For a probe entirely on the first side (σ_1) of the boundary as shown in Figures 4.3 (a) and (c), the parallel and normal resistances are

$$R_p^{(11)} = \frac{1}{\pi\sigma_1 a} \left[\left(1 - \frac{1}{\sqrt{2}} \right) + \frac{\sigma_1 - \sigma_2}{\sigma_1 + \sigma_2} \left(\frac{1}{2h/a} - \frac{1}{\sqrt{1+(2h/a)^2}} \right) \right] \quad (4.20)$$

and

$$R_n^{(11)} = \frac{1}{2\pi\sigma_1 a} \left[2\left(1 - \frac{1}{\sqrt{2}}\right) + \frac{\sigma_1 - \sigma_2}{\sigma_1 + \sigma_2} \left(\frac{1}{\sqrt{1+(1+2h/a)^2}} + \frac{1}{\sqrt{1+(1-2h/a)^2}} - \frac{2}{\sqrt{1+(2h/a)^2}} \right) \right], \quad (4.21)$$

where h is the distance from the centerline of the square-electrode probe to the boundary separating the two conducting quarter-spaces. For a probe straddling the boundary as shown in Figures 4.3 (b) and (d), the parallel and normal resistances are

$$R_p^{(12)} = \frac{2}{\pi(\sigma_1 + \sigma_2)a} \left(1 - \frac{1}{\sqrt{2}}\right) \quad (4.22)$$

and

$$R_n^{(12)} = \frac{1}{2\pi(\sigma_1 + \sigma_2)a} \left[4 - \frac{4}{\sqrt{2}} - (\sigma_1 - \sigma_2) \left\{ \frac{1}{\sigma_1} \left(1 - \frac{1}{\sqrt{1+(1+2h/a)^2}}\right) - \frac{1}{\sigma_2} \left(1 - \frac{1}{\sqrt{1+(1-2h/a)^2}}\right) \right\} \right]. \quad (4.23)$$

Finally, when the probe is entirely on the second side (σ_2) of the boundary, the parallel and normal resistances are

$$R_p^{(22)} = \frac{1}{\pi\sigma_2 a} \left[\left(1 - \frac{1}{\sqrt{2}}\right) + \frac{\sigma_2 - \sigma_1}{\sigma_2 + \sigma_1} \left(-\frac{1}{2h/a} - \frac{1}{\sqrt{1+(2h/a)^2}} \right) \right] \quad (4.24)$$

and

$$R_n^{(22)} = \frac{1}{2\pi\sigma_2 a}$$

$$\left[2\left(1 - \frac{1}{\sqrt{2}}\right) + \frac{\sigma_2 - \sigma_1}{\sigma_2 + \sigma_1} \left(\frac{1}{\sqrt{1+(1+2h/a)^2}} + \frac{1}{\sqrt{1+(1-2h/a)^2}} - \frac{2}{\sqrt{1+(2h/a)^2}} \right) \right], \quad (4.25)$$

where besides replacing σ_1 with σ_2 and vice versa the sign of the probe offset h relative to the interface also had to be inverted, which affects Eq. (4.24) but not (4.25).

Using the parallel and normal resistances derived in Eqs. (4.20) through (4.25), the normalized average resistance

$$R_a = \frac{R_p + R_n}{2 R_r} \quad (4.26)$$

and normalized “apparent” anisotropy

$$A_a = \frac{R_p - R_n}{2 R_r} \quad (4.27)$$

were obtained. These quantities are presented in a dimensionless form normalized with respect to the reference resistance that the same square-electrode probe would measure on a hypothetical homogeneous and isotropic half-space with an electric conductivity equal to the average of the conductivities of the two contacting quarter-spaces. For an ideal square-electrode probe, this reference resistance can be calculated as follows

$$R_r = \frac{2}{\pi(\sigma_1 + \sigma_2)a} \left(1 - \frac{1}{\sqrt{2}}\right) \quad (4.28)$$

This simplified model can be applied to a weld/base metal interface by assuming that σ_1 is the isotropic conductivity of the base metal, σ_2 is the lower conductivity of the also isotropic weld, and the plane boundary between the quarter-spaces represents the idealized fusion surface between them. Figure 4.4 (a) shows how the normalized average resistance R_a gradually increases from the base metal, through the interface (fusion line), to the weld region for different

σ_1/σ_2 conductivity ratios over a transition region that extends roughly one probe dimension on both sides of the bondline. The normalized “apparent” anisotropy A_a exhibits significant values and sharp variations only over the transition region as shown in Figure 4.4 (b) and higher conductivity ratios produce higher apparent anisotropy in the vicinity of the interface. This anisotropy is called “apparent” because the intrinsic electric conductivity is isotropic everywhere and the observed macroscopic anisotropy is caused entirely by the presence of the interface. For 50% difference between the more conducting base metal and the less conductive weld, the apparent anisotropy could reach as much as $\pm 20\%$ in the worst case scenario which occurs when the probe offset (h) relative to the bondline is such that two pins are directly over the bondline ($h/a = \pm 0.5$). This apparent anisotropy changes so rapidly with offset from the bondline that small positioning errors will inevitably lead to large measurement uncertainties in terms of anisotropy, which explains the large experimental uncertainties observed in our measurements when the probe was repeatedly re-positioned over the bond line.

Figure 4.2 Illustration of the Method of Images with the injection and sensing points on the same side (a) and opposite sides (b) of the plane boundary separating two conducting quarter-spaces.

Figure 4.3 Parallel (a, b) and normal (c, d) potential drop measurements with square-electrode configuration on the same side of the boundary (a, c) and across it (b, d).

Figure 4.4 The normalized average resistance (a) and anisotropy (b) across the three regions (base metal, interface and the weld) as a function of the normalized probe position.

CHAPTER V

5. VALIDATION OF THE ANALYTICAL PREDICTIONS

5.1. INTRODUCTION

The analytical predictions derived in the previous chapter showed a gradual increase in the average resistance across the interface from the base metal to the weld region as well as a spurious apparent anisotropy near the bondline (see Figure 4.4). Although the predicted apparent anisotropy essentially vanishes directly over the bondline, a small offset in either direction could cause a significant variation in the measured anisotropy. This high sensitivity to probe position explains the inherently high measurement uncertainty observed in our experiments in the vicinity of the weld/base metal boundary. A more quantitative validation of the predicted apparent anisotropy required comparison to the results of additional measurements at various distances from the bondline. Such measurements were done on each specimen and, with the exception of the most damaged one (specimen #1), revealed anisotropy profiles $A(h)$ similar to those predicted by the simplified analytical model which assumed a perfect normal interface between two connected quarter-spaces of different electric conductivities (the profile of specimen #1 exhibited a large irregular positive peak in the vicinity of the boundary).

5.2. COMPARISON WITH EXPERIMENTAL RESULTS

As an example, Figure 5.1 shows a comparison between the analytical predictions and the experimental results for the least damaged specimen (#4) we had access to. This specimen had negligible creep damage (45 cavities/mm²) but very large conductivity contrast ($\sigma_1/\sigma_2 = 1.67$). The actual offset from the perceived bondline could have been off by as much as $\Delta h = \pm 0.5$ mm while the electrode separation was only $a = 4$ mm, therefore the normalized offset uncertainty could be as high as $\Delta h/a = \pm 12.5\%$ which is enough to cause $\pm 8\%$ uncertainty in the measured apparent anisotropy. Considering the inevitable uncertainty in probe positioning caused by the irregularity of the weld boundary, the experimental results agreed with the analytical predictions quite well for both the normalized average resistance (a) and normalized anisotropy (b). On the other hand, even with careful probe positioning the measured anisotropy tends to be lower than expected because the interface between the weld and the base metal is not necessarily normal to the surface and the transition is not necessarily as sharp as assumed in the analytical model, two secondary effects that will be addressed later.

5.3. COMPARISON WITH NUMERICAL SOLUTION

5.3.1. Numerical approach using finite element analysis (ANSYS)

The two effects that (i) the interface between the weld and the base metal is not necessarily normal to the surface and (ii) the transition is not necessarily as sharp as assumed in the analytical model can be much more easily studied by numerical simulation than by more complex analytical models, therefore, in preparation for such numerical simulations, we used

finite element analysis (ANSYS 11.0) to numerically validate the analytical predictions for the one case where analytical results were already available, i.e., in the case of two quarter-spaces in perfect electric contact.

For the numerical analysis, finite element tool ANSYS 11.0 was used. Within ANSYS 11.0, ANSYS Academic Teaching Advanced Utility Menu was selected. Electromagnetic model was selected to run the analysis for the geometry with base metal, interface, and weld. To create the geometry, SOLID69 element type was selected which is a three-dimensional coupled thermal-electric solid [34]. This element has eight nodes. At each node, it has two degrees of freedom, i.e., temperature and voltage [34]. A constant resistivity value in ohm-m was entered for the electrical material property. The resistivity value for a desired electrical conductivity was calculated. The conductivity ratio of $\sigma_1/\sigma_2 = 1.67$ was created fitting that of our least damaged specimen #4. σ_1 is the electrical conductivity of the base metal and σ_2 is of the weld. The three-dimensional geometry (60 mm by 60 mm by 20 mm) was created with the base metal, interface, and welded section. The geometry was meshed by defining number of element divisions per line for length and breadth as 60 respectively and 30 for the thickness. The meshed geometry yielded 115351 nodes. Convergence was achieved by manipulating the number of element divisions per line and on the limitation of the number of nodes in ANSYS 11.0. The maximum number of nodes supported by this version of ANSYS is 256000 nodes. The electric load of 0.1 A was applied alternating between the axial and lateral directions between each simulation for the 4 mm square electrode configuration. In a four-point probe, current is injected into the inspected specimen through one pair of electrodes and the resulting voltage difference between two other points on the surface is measured using a second pair of electrodes. Simulation was executed for the desired setup and the electric potential was obtained for both desired directions, i.e., axial and

lateral. The obtained values of electric potentials were then further manipulated in Microsoft Excel spreadsheet to obtain the plots for anisotropy and normalized average resistance versus the normalized probe position that are shown in Figure 5.2.

The potential drop values obtained from these numerical simulations were used to calculate the normalized average resistance and anisotropy according to Eqs. (4.26) through (4.28). Figures 5.2 (a) and (b) show a comparison of the analytical and numerical results for the normalized average resistance and normalized anisotropy, respectively. The finite element results agreed with the analytical predictions within a small numerical error related to the meshing density used. In the approximation of our idealized simple model of the weld/base metal interface, the average resistance gradually increases from the base metal, through the interface to the weld and the apparent anisotropy becomes significant only in the close vicinity of the bondline between the weld and the base metal.

5.4. CONCLUSION

From the comparison between the analytical, experimental and numerical results it can be concluded that the apparent anisotropy near the bondline is caused by the sudden change in conductivity between the base metal and the weld region and has nothing to do with the potential presence of creep damage in the same area. This apparent anisotropy that is observed only in the vicinity of the weld/base metal boundary makes the detection of any creep damage near this boundary more difficult to detect. The exact match between the analytical and numerical simulations for the normalized apparent anisotropy is due to the consideration of a perfectly normal (90°) interface in the numerical simulation in agreement with the analytical model. In contrast, the experimental results, though they exhibit similar anisotropic behavior, do not

overlap with the analytical predictions. This discrepancy could be due to the inclination of the interface since the boundary between the weld and the base metal is usually not exactly normal to the surface. Another possible source of discrepancy between the theoretical model and the experimental test piece is the gradual rather than abrupt conductivity transition between the weld and the base metal. These two secondary effects are difficult to study analytically, but could easily be investigated by numerical means.

Figure 5.1 Comparisons between analytical (normal interface) and experimental results from specimen #4 of negligible creep damage for the normalized average resistance (a) and anisotropy (b) as a function of probe position (specimen #4).

Figure 5.2 Comparison between the analytical and numerical results for the normalized average resistance (a) and anisotropy (b) as a function of probe position for a normal interface.

CHAPTER VI

6. BOUNDARY INCLINATION AND GRADUAL TRANSITION

6.1. INTRODUCTION

The previously presented comparisons between the analytical and numerical predictions and experimental observations seem to indicate that the observed underestimation in the measured anisotropy could be due to the random offset uncertainty caused by probe positioning as it is placed over the bondline between the weld and the base metal. Even with perfect probe positioning relative to the bondline on the surface, significant additional uncertainty could be caused by the unknown inclination of the interface relative to the surface and/or by the gradual transition between the weld and the base metal. Figure 6.1 shows schematic diagrams of imperfect weld/base metal models for finite element simulation of boundary inclination (a) and gradual transition (b).

6.2. EFFECT OF BOUNDARY INCLINATION

In order to study the effect of boundary inclination relative to the surface, numerical simulations were conducted at seven different boundary inclination angles. The simulation was performed the same way as before except that the boundary was not necessarily normal to the

surface. Figure 6.2 (a) shows the normalized average resistance as a function of the normalized probe position at seven different boundary inclination angles. Changing the boundary inclination angle caused only a minor shift in the normalized average resistance curves. This could be expected since the penetration depth of the measurement is roughly equal to half of the electrode separation therefore the effective position of the boundary is shifted relative to the visible bondline on the surface depending on the inclination angle. Figure 6.2 (b) shows the apparent anisotropy as a function of the normalized probe position at the same seven boundary inclination angles. Changing the inclination angle somewhat reduced the apparent anisotropy on both sides of the weld/base metal boundary depending on the inclination angle but did not eliminate it. This effect could be also expected since the inclined interface appears as a slightly shifted gradual transition and the location of the actual bondline is a function of depth therefore the probe averages over its effective penetration depth.

6.3. EFFECT OF GRADUAL TRANSITION

Gradual transition in the electric conductivity near the interface region was also studied using finite element analysis. In order to minimize the number of adjustable parameters in the finite element simulation, the transition layer was assumed to be of constant width $t = a/2$ equal to half of the electrode separation, i.e., $t = 2$ mm in this case. Linear interpolation was used to distribute the conductivity difference from weld to base metal and the number n of homogeneous sub-layers was varied from 0 to 4 to represent transition profiles of different smoothness. Figure 6.3 (a) shows the normalized average resistance for the four different transition layers as a function of the normalized probe position. The average resistance did not show any perceivable sensitivity to the smoothness of transition and the gradual increase from the base metal through

the interface to the weld region was essentially the same for each of the four cases studied. In contrast, the 4-step smoothened transition layer significantly reduced the apparent anisotropy as shown in Fig. 6.3 (b), though did not completely eliminate it.

6.4. CONCLUSIONS

These results suggest that gradual transition of the electric conductivity throughout the fusion boundary layer between the weld and the base metal is more likely to explain the experimentally observed behavior than the non-normal inclination of the interface relative to the surface.

Figure 6.1 Imperfect weld/base metal models for finite element simulation of boundary inclination (a) and gradual transition (b).

Figure 6.2 Finite element predictions for the normalized average resistance (a) and anisotropy (b) for seven different boundary inclination angles.

Figure 6.3 Finite element predictions for the normalized average resistance (a) and anisotropy (b) for four different boundary models.

CHAPTER VII

7. EFFECTS OF HEAT TREATMENT

Further tests were conducted to study the effect of thermal exposure on the average resistance and intrinsic anisotropy of the weld and base metals separately. The importance of these tests lies in the role of changing anisotropy during creep. In creep monitoring with either permanently installed or deployable DACPD probes, the measured resistance ratio is normalized to the initial resistance ratio recorded in the intact state of the material to eliminate systematic errors caused by imperfect electrode positioning and pre-existing texture. Any further change in the resistance ratio is then interpreted as either creep-induced geometrical effect due to plastic strain (only in the case of permanently installed electrodes) or material effect in the form of creep damage induced electric anisotropy. An obvious concern in this respect is that small changes in the apparent or real material anisotropy might occur as a result of thermally-activated microstructural evolution even in the absence of stress, i.e., actual creep.

In more or less homogeneous materials, i.e., far away from weld/base metal interfaces, the only issue is whether the intrinsic electric anisotropy of the material might change due to thermal exposure. The intrinsic electric anisotropy of creep resistant steels is limited to morphological texture and is usually very weak, less than 1%, therefore this effect is not expected to be significant. However, the apparent anisotropy measured in the vicinity of

weld/base metal boundaries could exhibit much more significant changes because the resistivity of the weld and base metals could change at very different rates during thermal exposure, which influences their resistance ratio and therefore the level of apparent anisotropy for a given probe position, especially if the probe straddles the boundary, but is not centered directly over it.

This effect was studied separately in specimens cut from the weld and base metal parts of a 0.5%Cr-0.5%Mo-0.25%V low-alloy steel pipe. Before thermal exposure, the resistivity of the weld was found to be as much as 75% higher than that of the base metal. Thermal exposure tests were conducted subsequently at three different temperatures, namely at 650°C, 700°C and 750°C. At each temperature, the base metal and weld were heated to the test temperature in five subsequent cycles of 3 hours, 7 hours, 20 hours, 70 hours and 200 hours. Between the cycles, i.e., after 3, 10, 30, 100, and 300 combined thermal exposure, the electric resistances were measured in two orthogonal directions with the previously described deployable DACPD probe. The relative anisotropy and average resistance were calculated from these measured resistance pairs. After the full five-step heat treatment cycle was completed at one temperature, the temperature was increased to the next level and the cycle was repeated. Overall, the two samples were heat treated for a total of 900 hours with 300 hours at each temperature.

During the whole series of thermal exposure tests, the intrinsic anisotropy of both the weld metal and the base metal remained essentially constant at $\Delta \approx 1\%$, which was barely detectable in the presence of comparable measurement uncertainties. The normalized average resistances measured in every heat treatment step on the two samples are plotted in Figure 7.1. The symbols represent measurement data and the solid, dashed and dotted lines are linear regressions (they look exponential because of the logarithmic time scale). The averaged values of the electric resistances were normalized to those of the corresponding intact (initial state)

samples for easier comparison. The average resistance for both the base metal and weld exhibited some increase during thermal exposure. However, in the weld metal the changes were barely noticeable at 650°C and 700°C and even at 750°C remained below 0.6% total increase. Much larger changes occurred in the base metal. Initially, the resistance slightly decreased at 650°C but then started to increase at 700°C and reached as much as 6.6% higher at the end of the 300-hour exposure at 750°C. This means that high-temperature thermal exposure will reduce the resistivity difference between the weld and the base metals, at least at peak temperatures. It should be mentioned that the average operational temperature for this kind of steel is expected to be around 600°C or less, therefore the thermal exposure used in this example was relatively short, but still quite excessive because of the 750°C peak temperature.

According to our earlier results, in the worst case scenario, i.e., when the offset of the probe from the weld/base metal boundary is the least favorable, 6.6% change in the resistivity contrast will cause approximately 2.6% spurious change in the measured resistance ratio. This change (reduction) in the apparent anisotropy measured by the DACPD probe exceeds all other experimental uncertainties, therefore will adversely affect the sensitivity of creep monitoring by this technique. However, in the case of permanently mounted probes, the combined geometrical and material gauge factors produce changes on the order of 200-300% in the resistance ratio before rupture [23], therefore the role of apparent anisotropy remains limited even in the vicinity of weld/base metal boundaries.

In the case of deployable DACPD probes the geometrical effect of creep strain is eliminated therefore material changes, including both real anisotropy changes due to creep damage accumulation and apparent anisotropy changes in the vicinity of the weld due to unrelated thermally-induced microstructural evolution, become dominant. The spurious apparent

anisotropy can be minimized by aligning the probe with the weld/base metal boundary as suggested by the zero crossings in Figure 5.1 (b) and subsequent results. In practice, deployable probes are not expected to reach much better than $\pm 1\%$ repeatability on perfectly isotropic materials even with precision-guided electrode pins. Compared to this inherent targeting uncertainty, the influence of apparent anisotropy changes due to thermal evolution only can be all but eliminated by careful positioning of the probe over the weld/base metal boundary. One possibility is to measure the average resistances of the base metal and the weld far away from the boundary and then optimize the position of the probe so that the average of the two resistivity values measured normal and parallel to the bond line matches the algebraic means of the weld and base metal resistances.

Figure 7.1 Normalized average resistance of the base metal and weld throughout three different heat treatment cycles. The solid, dashed and dotted lines are linear regressions.

CHAPTER VIII

8. CONCLUSIONS

Deployable DACPD probes are not sensitive to geometrical effects and so can be used to detect selectively material changes only. Initial experiments showed that severe creep damage ($>1,000$ cavities/mm²) can be readily detected by deployable probes even in the vicinity of weld/base metal interfaces. However, at lower damage levels the intrinsic electric anisotropy caused by the presence of oriented clusters of grain boundary cavities could be overwhelmed by a spurious apparent anisotropy caused by the vicinity of the directional weld/base metal boundary that separates domains of significantly different electric conductivity. Although this apparent anisotropy is negligible directly over the boundary, even a small offset normal to the boundary might result in a large change in the measured anisotropy and thereby might lead to false indication below damage levels of approximately 300-600 cavities/mm² depending on the conductivity difference between the weld and the base metal.

Inclined boundaries and gradual transitions reduce the apparent anisotropy, but do not completely eliminate it. In the presence of typical 30-50% conductivity difference between the weld and the base metal, random positioning errors might cause significant apparent anisotropy in the vicinity of the boundary and will limit the detection threshold to about 3-5% anisotropy

irrespective of the electric noise and pin positioning error. It was also found that the apparent anisotropy will likely decrease during service as a result of thermal exposure independent of mechanical stresses causing creep.

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