Potential drop detection of creep damage in the vicinity of welds

Seeran Prajapati a, Peter B. Nagy a,b,*, Peter Cawley b

a School of Aerospace Systems, University of Cincinnati, Cincinnati, Ohio 45221, USA
b UK Research Centre in NDE, Imperial College, London SW7 2AZ, UK

1. Introduction

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. 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 [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. From numerous NDE methods, potential drop measurement emerged as one of the most promising options [1, 8]. Alternating current potential drop (ACPD) measurement is an electromagnetic technique used mainly for monitoring crack growth [8, 9], estimating the depth of surface breaking defects [10, 11] and evaluating material properties such as conductivity or permeability [12, 13]. 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 \( \delta \) beneath the surface, which can be controlled by the inspection frequency as follows

\[
\delta = \frac{1}{\sqrt{\pi f \mu \sigma}}.
\]

where \( f \) is the frequency of the injection current, \( \mu \) is the magnetic permeability of the material and \( \sigma \) 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 [14]. 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 [15,16].

For creep monitoring in ferritic steels, the inspection frequency is chosen so low, typically between 1 and 10 Hz, that the electromagnetic skin depth is larger than the electrode separation therefore quasi-static approximations can be used to determine the current distribution in the specimen. Still, these inspection frequencies are high enough to exploit some of the technical advantages of ACPD inspection over conventional DCPD methods, namely the elimination of thermoelectric effects, enhanced common mode rejection ratio, impedance matching and ground loop elimination by transformer coupling, and higher signal to noise ratio even at very moderate injection current levels (~0.1 A). All the electric conductivity measurements reported in this paper were conducted with a square ACPD probe of 4 mm electrode separation at 3 Hz so that the influence of magnetic property variations could be neglected even in strongly ferromagnetic steels.

This paper 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 [17,18]. 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.

2. Deployable DACPD probe

A spring-loaded DACPD probe can be used to measure the electrical anisotropy of conductors. Fig. 1(a) 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 Fig. 1(b) and (c), 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 \( A_1-A_2 \) and measuring the resulting in-phase voltage drop \( V_2 \) between electrodes \( A_2-C_2 \). Since the electrodes are sprung-loaded, the average electrode separation \( a \) remains constant independent of the plastic deformation the material might have undergone between measurements, though slight random variation are inevitable due to the limited targeting accuracy of deployable electrodes. Because of this, the probe is not sensitive to geometrical effects of creep strain and can be used to detect selective material changes only.

Let us assume that the nominal electrode separation is \( a = 4 \text{ mm} \). The best commercially available spring loaded electrodes exhibit \( \pm 50 \mu \text{m} \) targeting accuracy which, unless further reduced, could cause as much as \( \Delta a/a \approx \pm 2\% \) relative electrode separation uncertainty. Since the geometrical gage factor of the DACPD probe is around \( G \approx 5 \) [18], 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 Fig. 1(a).

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 Fig. 2(a) and (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 [19]. 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 304 stainless steel 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.

3. Creep assessment

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 above described deployable DACPD probe. 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 tested by replica examination after operating for about 150,000 h 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. Table 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 Fig. 3 (sample #2). As expected, the average resistance monotonically increased from the base metal, through the interface, to the weld region as shown in Fig. 4(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 Fig. 4(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.

4. Apparent anisotropy of normal boundary

In order to better understand the overall trends and measurement uncertainties exhibited by the above 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,
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 [20].

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

\[
E(r) = \frac{I(r)}{\sigma} = \frac{I}{2\pi\sigma r^2},
\]

where, \( \sigma \) is the electric conductivity, \( I \) is the current density and \( r \) is the distance from the source. The electric potential distribution is

\[
V(r) = \int_0^r E(r) dr = \frac{I}{2\pi\sigma r} + V_0,
\]

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.

Fig. 5(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 \( \sigma_1 \) and \( \sigma_2 \). 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 \( \sigma_2 \) 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 [20]

\[
\frac{I'}{I} = \frac{\sigma_1 - \sigma_2}{\sigma_1 + \sigma_2},
\]

and

\[
\frac{I''}{I} = \frac{2\sigma_2}{\sigma_1 + \sigma_2}.
\]

when the injection and sensing points are located on the first side (\( \sigma_1 \)) of the plane boundary separating two conducting quarter-spaces, the electric potential can be written as

\[
V^{(11)} = \frac{I'}{2\pi\sigma_1 r} + \frac{I''}{2\pi\sigma_2 r} = \frac{I}{2\pi\sigma_1 \sqrt{(x-x_s)^2 + (y+y_s)^2}}
\]

\[
+ \frac{I'}{2\pi\sigma_1 \sqrt{(x-x_s)^2 + (y+y_s)^2}^{(y+y_s)}},
\]

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

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

Substituting \( I' \) and \( I'' \) from Eqs. (4) and (5) into Eqs. (6) and (7), respectively, the potential distribution can be written as

\[
V^{(11)} = \frac{I}{2\pi\sigma_1 \sqrt{(x-x_s)^2 + (y+y_s)^2} + \sigma_1 - \sigma_2} \frac{1}{\sigma_1 + \sigma_2 \sqrt{(x-x_s)^2 + (y+y_s)^2}^{(y+y_s)}}.
\]

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

\[
V^{(12)} = \frac{I}{2\pi(\sigma_1 + \sigma_2)} \frac{1}{\sqrt{(x-x_s)^2 + (y+y_s)^2}^{(y+y_s)}}.
\]
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 (σ₂) of the boundary, the potential \( V^{(22)} \) can be obtained from Eq. (8) by simply replacing \( \sigma_1 \) with \( \sigma_2 \) 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. (9).

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 Fig. 6. Generally, the measured transfer resistance can be calculated from

\[
R = \frac{V^{(+)} - V^{(-)}}{I},
\]

where \( f^{(+)} = f^{(-)} = I \). 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 (σ₁) of the boundary as shown in Fig. 6(a) and (c), the parallel and normal resistances are

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

and

\[
R_{n}^{(11)} = \frac{1}{2\pi \sigma_1 a} \left[ \left( \frac{1}{\sqrt{1 + (1 + 2h/a)^2}} - \frac{1}{\sqrt{1 + (1 - 2h/a)^2}} - \frac{2}{\sqrt{1 + (2h/a)^2}} \right) \right].
\]

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 Fig. 6(b) and (d), the parallel and normal resistances are

\[
R_{p}^{(12)} = \frac{2}{\pi (\sigma_1 + \sigma_2) a} \left( 1 - \frac{1}{\sqrt{2}} \right)
\]

\[
R_{n}^{(12)} = \frac{1}{2\pi (\sigma_1 + \sigma_2) a} \left[ \frac{4 - 4}{\sqrt{2}} - \frac{1}{\sqrt{1 + (1 + (2h/a)^2)}} \right]
\]

Finally, when the probe is entirely on the second side (σ₂) of the boundary, the parallel and normal resistances are

\[
R_{p}^{(22)} = \frac{1}{\pi \sigma_2 a} \left[ \left( 1 - \frac{1}{\sqrt{2}} \right) + \frac{\sigma_1 - \sigma_2}{\sigma_1 + \sigma_2} \left( -\frac{1}{2h/a} - \frac{1}{\sqrt{1 + (2h/a)^2}} \right) \right].
\]

and

\[
R_{n}^{(22)} = \frac{1}{2\pi \sigma_2 a} \left[ 2 \left( 1 - \frac{1}{\sqrt{2}} \right) + \frac{\sigma_1 - \sigma_2}{\sigma_1 + \sigma_2} \left( \frac{1}{\sqrt{1 + (1 + 2h/a)^2}} + \frac{1}{\sqrt{1 + (1 - 2h/a)^2}} - \frac{2}{\sqrt{1 + (2h/a)^2}} \right) \right].
\]

where besides replacing \( \sigma_1 \) with \( \sigma_2 \) and vice versa the sign of the probe offset \( h \) relative to the interface also had to be inverted, which affects Eq. (15) but not (16).

Using the parallel and normal resistances derived in Eqs. (11)–(16), the normalized average resistance

\[
R_n = \frac{R_p + R_n}{2R_n},
\]

and normalized anisotropy

\[
A = \frac{R_p - R_n}{2R_n},
\]

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:

\[
R_0 = \frac{2}{\pi (\sigma_1 + \sigma_2) a} \left( 1 - \frac{1}{\sqrt{2}} \right).
\]

This simplified model can be applied to a weld/base metal interface by assuming that \( \sigma_1 \) is the isotropic conductivity of the base metal, \( \sigma_3 \) is the lower conductivity of the also isotropic weld, and the plane boundary between the quarter-spaces represents the idealized fusion surface between them. Fig. 7(a) shows how the normalized average resistance \( R_n \) gradually increases from the base metal, through the interface (fusion line), to the weld region for different \( \sigma_1/\sigma_2 \) conductivity ratios over a transition region that extends roughly one probe dimension on both sides of the bondline. The normalized anisotropy \( A \) exhibits significant values and sharp variations only over the transition region as shown in Fig. 7(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 \( +/-20\% \) 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

![Fig. 6. 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).](image-url)
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.

5. Validation of the analytical predictions

The analytical predictions derived in the previous section 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 Fig. 7). 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).

As an example, Fig. 8 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.

These effects 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) 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. A three-dimensional geometry was defined that included three regions, the base metal, the interface and the weld, with a conductivity ratio of \(\sigma_1/\sigma_2 = 1.67\) fitting that of our least damaged specimen #4. The electric transfer resistance of a
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.

### 6. Boundary inclination and gradual transition

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. Fig. 10 shows schematic diagrams of imperfect weld/base metal models for finite element simulation of boundary inclination (a) and gradual transition (b).

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. Fig. 11(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 only half of the electrode separation therefore the effective position of the boundary is shifted by a small amount relative to the visible bondline on the surface depending on the inclination angle. Fig. 11(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 also be expected since the inclined interface appears as a slightly shifted gradual transition and the
of constant width finite element simulation, the transition layer was assumed to be order to minimize the number of adjustable parameters in the face region was also studied using finite element analysis. In location of the actual bondline is a function of depth therefore the probe averages over its effective penetration depth.

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. Fig. 12(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. 12(b), though it did not completely eliminate it. 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.

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 h, 7 h, 20 h, 70 h and 200 h. 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 h with 300 h 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 Δ ≈ 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 Fig. 13. 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 h 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 gage factors produce changes on the order of 200–300% in the resistance ratio before rupture, 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 Fig. 8b and subsequent results. In practice, deployable probes are not expected to reach much better than ± 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.

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 (≈ 1000 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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