Sensitivity analysis of a directional potential drop sensor for creep monitoring

Elhoucine Madhi, Peter B. Nagy*

School of Aerospace Systems, University of Cincinnati, Cincinnati, Ohio 45221-0070, USA

Abstract

Recent research efforts indicated that directional potential drop (PD) measurements could be exploited for in-situ creep monitoring. The present work investigates the sensitivity of such measurements with a square-electrode configuration to geometrical and material variations caused by creep. This measurement technique is based on a modification of the conventional PD technique as it measures simultaneously two resistance values in orthogonal directions. Under uniaxial stress condition, the ratio of the lateral and axial resistances is roughly proportional to the applied strain. Experimental tests showed that small anisotropic changes in the resistance ratio caused by directional effects of creep can be distinguished from potentially far larger isotropic changes caused by non-directional reversible and irreversible thermal effects. The sensitivity of the square-electrode PD sensor to geometrical and material variations was analyzed separately and the analytical predictions were validated by experimental tests in both cases. The directional PD technique was found to exhibit high sensitivity that allows the detection of elastic and plastic strains as low as 0.05%. Additional experimental results from an accelerated 400-hour creep test are presented to demonstrate the feasibility of this technique to monitor creep degradation in 304 stainless steel at 600°C.

© 2011 Elsevier Ltd. All rights reserved.

1. Introduction

During creep deformation, material damage accumulates with time as a function of temperature and mechanical stress. The specific form of degradation is material dependent, but in creep-resistant steels it might follow two main types of path [1]. First, there is transformation and precipitation of carbides from the initial microstructure. These precipitations might be in the form of tempered martensite and bainite or pearlite and ferrite, depending on the heat treatment used. Second, at a later stage of the degradation process, the presence of voids and microcracks becomes more evident. For creep-resistant materials used at high temperatures, creep rupture caused by cumulative damage is a common failure mode. The process starts by the nucleation and then growth of cavities at grain boundaries. The cavities tend to gather preferentially on grain boundaries approximately perpendicular to the applied stress [2–4]. Eventually these cavities connect together to form microcracks that propagate and join together in a later stage of degradation that leads to ultimate failure. This process leads to anisotropic texture in electric resistivity, which can be exploited for creep monitoring. In effect, the resulting preference in damage orientation introduces small but perceivable path-length differences for electrical current. This induced anisotropy is detectable by directionally sensitive sensors.

Most damage detection techniques have sufficient level of sensitivity for the purposes of damage monitoring, but the crucial issue is their selectivity, or the lack of it, to a specific material degradation mechanism. There are numerous variables to which a given sensor can be sensitive including microstructural evolution, carbide precipitation, hardening, plastic strain and elastic strain. Effective creep monitoring requires that the sensor sufficiently suppress changes that are primarily associated with thermally-activated microstructural evolution. Even prior to mechanical loading, the material contains various microdefects, such as microcracks, voids, inclusions, second-phase particles and other inhomogeneities [5]. Also, most steel components used in power plants accumulate an initial amount of cold work during their manufacturing. The presence of cold work induced texture and directional defects is expected to result in an initial material anisotropy that is detectable by sensitive probes as recovery and relaxation takes place during thermal exposure.

Because of the above described complexities, creep monitoring in general and detection of void precursors in early stages of creep in particular continue to be great challenges and also great opportunities for the nondestructive evaluation community.
The most popular techniques for creep detection include replica metallography, various forms of surface strain measurements, ultrasonic velocity, attenuation, backscatter, birefringence measurements, magnetic methods including Barkhausen and magneto-acoustic emission, hardness measurements, positron annihilation, small-angle neutron scattering, eddy current and acousto-electric emission, hardness measurements, positron metallography, various forms of surface strain measurements, and replicas. From the point of view of creep amplification, the most popular techniques for creep detection include replicas, which offer many advantages over the conventional non-directional inline electrode configuration [16]. A permanently installed sensor consists of the material to be tested and four thermocouple wires spot-welded to it as shown in Fig. 2. Let us assume that a uniaxial tensile load is applied in the x1 direction. First, the “axial” transfer resistance $R_1=V_1/I_1$ is obtained by injecting current $I_1$ at electrodes A–B and measuring the resulting in-phase voltage drop $V_1$ between electrodes D–C. Then, the “lateral” transfer resistance $R_2=V_2/I_2$ is obtained by injecting current $I_2$ at electrodes A–D and measuring the resulting in-phase voltage drop $V_2$ between electrodes B–C. In this fashion, creep can be monitored through the variations observed in the resistance ratio $R_1/R_2$. It should be mentioned that, based on the Reciprocity Theorem, the injection and sensing electrodes can be exchanged without affecting the measured transfer resistances, which might be exploited to improve the measurement accuracy.

Generally, the measured electric resistances $R_{1,2}$ are complicated functions of both reversible and irreversible thermal, mechanical and thermo-mechanical (creep) effects in the material. For our current purposes, these effects can be modeled as follows:

$$R_{1,2}(T, e) \approx R_{0,20}B_{1,2}(T)K_{1,2}(e),$$

where $B_{10}$ and $B_{20}$ are the initial “intact” values of $R_1$ and $R_2$, respectively, $B_1 \approx B_2$ represents the very similar temperature dependence of the resistance in the two principal directions, $T$ is the instantaneous temperature of the specimen, $C_{1,2}$ are functions that represent the strain dependence of the resistances in the principal directions and $e$ is the total axial strain that includes both elastic $e_0$ and plastic $e_p$ strain components. Since thermal expansion is negligible in comparison with the much higher temperature dependence of the electric resistivity, $B_{1,2}(T)$ can be approximated as a purely material effect:

$$B_{1,2}(T) \approx 1 + \beta (T-T_0)^m + \beta_{1,2},$$

where $\beta$ and $m$ are the essentially isotropic coefficient and power of reversible temperature variation, respectively, $T_0$ is an arbitrary
reference temperature and $\beta_{1,2}$ represent the irreversible changes caused by thermally-activated microstructural evolution. These irreversible thermal effects are essentially identical, but might exhibit slightly different values in the $x_{1,2}$ directions partly because of preexisting texture and residual stress in the "intact" material and partly because of possible directional thermally-activated microstructural changes in the presence of unidirectional state of stress.

In order to suppress inevitable variations in electrode positioning and other inherent variations unrelated to subsequent creep, such as preexisting inhomogeneity and texture that together could result in as much as 10–20% difference between $R_{10}$ and $R_{20}$, the measured resistances are first normalized to their initial intact values:

$$\eta_{1,2} = \frac{R_{1,2}(T, e)}{R_{10,20}}.$$  

(3)

where the normalized resistances $\eta_{1,2}$ are functions of temperature and strain. In the second step of data evaluation the normalized resistance ratio is calculated between the axial and lateral directions as follows:

$$\xi = \frac{\eta_1(T, e)}{\eta_2(T, e)}.$$  

(4)

This second normalization is necessary because during creep the reversible resistivity changes over a typical service temperature range of 500–700°C as power plant steels often exceed 100–200% while the expected overall increase of $\xi$ in the early stages of creep is only a few percent. Fortunately, this strong reversible temperature effect is mainly isotropic since the increase in lattice vibrations at high temperature causes the resistivity to increase by equal amounts in every direction, which was exploited in Eq. (2).

In contrast to the reversible temperature effects, irreversible resistivity changes $\beta_{1,2}$ caused by thermal exposure are usually not entirely isotropic. As we mentioned earlier, any electric anisotropy caused by preexisting texture in the microstructure or stress relaxation can cause irreversible directional resistivity changes due to thermal exposure. Thermally-activated microstructural evolution starts at significantly lower temperature and progresses at significantly higher rate in the presence of microstructural damage [17]. For instance, in cold-rolled materials resistivity changes with thermal exposure can be notably different in two principal directions. This difference in resistivity variation with thermal exposure introduces apparent directionality to the measurement since the orthogonal resistances are normalized to their respective initial values, which include the effects of preexisting anisotropic texture.

The fact that the temperature effect $\beta_{1,2}(T)$ is mostly isotropic while the strain effect $C_{1,2}(e)$ is highly anisotropic can be exploited for the separation of these two competing effects:

$$\xi = \frac{\xi_g \xi_m}{\eta_{s} \eta_m} \approx \frac{C_1(e)}{C_2(e)}.$$  

(5)

where $\xi_g$ is the geometrical factor and $\xi_m$ is the material factor of the overall normalized resistance ratio $\xi$. The strain effect $C_{1,2}(e)$ can be further approximated as follows:

$$C_{1,2}(e) \approx 1 + g_{1,2}e_e + G_{1,2}e_p + m_{1,2}e_e + M_{1,2}e_p.$$  

(6)

Here, $g_{1,2}$ and $G_{1,2}$ are linearized gage factors that account for purely geometrical changes caused by elastic and plastic strains, respectively. In contrast, $m_{1,2}$ and $M_{1,2}$ are linearized gage factors that account for purely material changes, namely reversible and irreversible anisotropic resistivity changes, respectively, in the principal directions. Then, for small values of elastic and plastic strains, $\xi$ can be also approximated as follows:

$$\xi \approx 1 + g_{1,2}e_e + G_{1,2}e_p + m_{1,2}e_e + M_{1,2}e_p.$$  

(7)

where $g = g_1 - g_2$ and $G = G_1 - G_2$ are differences between the respective geometrical gage factors in the axial and lateral directions, respectively, and $m = m_1 - m_2$ and $M = M_1 - M_2$ denote the relative anisotropic texture of resistivity caused by elastic and plastic strain, respectively. In this way, the geometrical $\xi_g$ and material $\xi_m$ factors of the normalized resistance ratio can be expanded into

$$\xi_g \approx 1 + g_e e_e + G_p e_p$$  

(8)

and

$$\xi_m \approx 1 + m_e e_e + M_p e_p \approx 1 + D A.$$  

(9)

Here, $A$ is a measure of the induced electric anisotropy defined as

$$A = \frac{\rho_1 - \rho_2}{(\rho_1 + \rho_2)/2},$$  

(10)

where $\rho_1$ and $\rho_2$ are the principal resistivity values in the $x_1$ and $x_2$ directions, respectively, and $D = (\xi_m - 1)/A$ is the directional factor of the sensor. The sign of $A$ was chosen so that its expectation value is positive for uniaxial stress along the $x_1$ axis.

3. Gage factors

Let us consider a simple strain gage whose resistivity $R(e) \approx R_0 (1 + F_e)$, where $R_0$ is the gage’s unstrained resistance and $F$ is the so-called gage factor that is defined as its relative sensitivity to the applied strain:

$$F = \frac{\Delta R}{R_0}.$$  

(11)

where $\Delta R = R - R_0$ is the strain-induced resistance change. The sensitivity of the strain gage is partly due to geometrical effects and partly due to material effects:

$$F = F_g + F_m.$$  

(12)

The geometrical part of the gage factor is

$$F_g = 1 + 2v,$$  

(13)

where $v$ is Poisson’s ratio. The material part of the gage factor is

$$F_m = \frac{\Delta \rho}{\rho_0}.$$  

(14)

where $\rho_0$ is the resistivity of the unstrained material. For most conventional strain gauges, $F_g \approx 1.6 \ (v \approx 0.3)$ and $F_m \approx 0.4–0.6$ for an overall gage factor $F \approx 2.0–2.2$. In the following sections, we are going to analyze the relative sensitivity of the square-electrode PD sensor to geometrical and material effects following this concept using asymptotic analytical solutions for very thick and very thin specimens [18,19].

3.1. Geometrical gage factor

In an isotropic half-space the spread of electric current from a point source is spherical, therefore

$$E(r) = \rho j(r) = \frac{1}{2 \pi r^2}.$$  

(15)

where $E$ is the radial electrical field, $\rho$ is the resistivity of the material, $J$ is the current density, $I$ is the applied current and $r$ is the distance from the source. The electric potential can be evaluated as

$$\phi(r) = \int_r^{\infty} E(r') dr' = \frac{1}{2 \pi} \int_r^{\infty} \frac{dr}{r} = \frac{1}{2 \pi} r + \phi_0.$$  

(16)
where \( \varphi_0 \) is an arbitrary integration constant that corresponds to the assumed electric potential very far away from the injection point. The transfer resistance can be determined by superposition, which yields

\[
\frac{\Delta V}{I} = \frac{\rho}{2\pi} \left( \frac{1}{r_{11}} - \frac{1}{r_{12}} - \frac{r_{12}}{r_{21}} + \frac{1}{r_{22}} \right),
\]

(17)

where \( r_{ij} (i,j=1 \text{ or } 2) \) is the distance between the \( i \text{th} \) injection electrode and the \( j \text{th} \) sensing electrode. For the sake of simplicity, let us consider only an ideal square-electrode sensor with an initial characteristic dimension \( a \) as shown in Fig. 2.

After deformation, the characteristic dimensions in the \( x_1 \) and \( x_2 \) directions will be \( a_1 \) and \( a_2 \), respectively. For the measurement in the axial direction, \( r_{11} = r_{22} = a_2 \) and \( r_{12} = r_{21} = \sqrt{a_1^2 + a_2^2} \). Let the current \( I^+ \) be injected at \( A \) and \( I^- \) at \( B \), and the voltage \( V^+ \) be measured at \( D \) and \( V^- \) at \( C \). Then, the transfer resistances can be determined from the measured voltage differences as follows:

\[
R_{1,2} = \frac{\rho}{\pi} \left( \frac{1}{a_{1,2}} - \frac{1}{\sqrt{a_1^2 + a_2^2}} \right),
\]

(18)

After some algebra, the sought resistance ratio \( \xi = R_1/R_2 \) is

\[
\xi = \sqrt{\frac{\gamma^2 + 1}{\gamma^2 + 1 + n(1+\gamma^2)}} - 1,
\]

(19)

where \( \gamma = a_1/a_2 \) is the geometrical aspect ratio of the deformed probe. In the case of uniaxial tension in the \( x_1 \) direction, \( a_1 = a_1(1+\varepsilon) \) and \( a_2 = a_1(1-\varepsilon) \). Assuming small strains \( \varepsilon \ll 1 \) only,

\[
\gamma = 1 + (1+\varepsilon)\varepsilon \quad \text{and the geometrical factor of the resistance ratio } \xi_g \text{ can be approximated as follows:}
\]

\[
\xi_g = 1 + n(1+\varepsilon)\varepsilon,
\]

(20)

where \( n = \sqrt{2}/(\sqrt{2} - 1) \approx 3.41 \). It should be mentioned that although elastic strains are inherently small, plastic strains that occur in later stages of creep could be as high as 50\% depending on the material and the creep conditions, therefore the above described simple linearization breaks down as rupture approaches.

Similar calculations can be conducted for a thin plate as well. In an isotropic thin plate the spread of electric current from a point source is cylindrical, therefore

\[
E(r) = \rho j(r) = \frac{I_0}{2\pi rt},
\]

(21)

where \( t \) is the thickness of the plate. The electric potential distribution can be evaluated as

\[
\varphi(r) = \int_r^\infty E(r) dr = \frac{I_0}{2\pi} \int_r^\infty \frac{dr}{t} = -\frac{I_0}{2\pi t} \ln r + \varphi_0
\]

(22)

and the transfer resistance can be again determined by superposition, which yields

\[
\frac{\Delta V}{I} = -\frac{\rho}{2\pi t} [\ln r_{11} - \ln r_{12} - \ln r_{21} + \ln r_{22}].
\]

(23)

After calculating the voltage difference in a similar fashion as in the case of a half-space, the transfer resistances in the axial \( x_1 \) and lateral \( x_2 \) directions can be obtained as follows:

\[
R_{1,2} = \frac{\rho}{2\pi t} \ln \frac{a_1^2 + a_2^2}{a_{1,2}^2},
\]

(24)

Thus, the resistance ratio is

\[
\xi = \frac{\ln(1+\gamma^2)}{\ln(1+\gamma^2)},
\]

(25)

Fig. 3. Linear and power approximations of the gage function for (a) thick and (b) thin plates.
rupture so that the peak value of $\gamma \approx 1 + (1 + v) \varepsilon \approx 1.5$ while the normalized resistance ratio might reach $\xi \approx 3 - 4$. In this range of practical interest, the linear approximation might cause more than 20% underestimation for the normalized resistance ratio while the power approximation is accurate within $\approx 0.5\%$.

3.2. Material gage factor

PD measurements with square-electrode configuration are also sensitive to the electric anisotropy of the material, which makes the sensor suitable for detecting variations due to creep-induced texture. In an anisotropic half-space with its free surface lying in the $(x_1, x_2)$ principal plane, the potential distribution of a point current source is [20,21]

$$
\varphi = \frac{1}{2\pi \rho_1 \rho_2 \rho_3} \left[ \frac{1}{\sqrt{\rho_1 (x_1-x_{15})^2 + \rho_2 (x_2-x_{25})^2 + \rho_3 (x_3-x_{35})^2}} \right] \sqrt{\rho_1^2 + \rho_2^2 + \rho_3^2} + \varphi_0.
$$

(26)

where $x_1$, $x_2$ and $x_3$ are the spatial coordinates of the field point; $x_{15}, x_{25}$ and $x_{35}=0$ are the coordinates of the source point on the surface; $\rho_1$, $\rho_2$ and $\rho_3$ denote the electric resistivity in the principal directions. Of course, for sensing points on the surface $x_3=0$.

Following the previously used procedure for a square-electrode PD sensor,

$$
R_{1,2} = \frac{\sqrt{\rho_1 \rho_2 \rho_3}}{\pi a} \left( \frac{1}{\sqrt{\rho_{11}}} - \frac{1}{\sqrt{\rho_{12}}} \right),
$$

(27)

and the resistance ratio is

$$
\xi = \frac{1 + \rho_{12} / \rho_{11} - 1}{1 + \rho_{12} / \rho_{11} - 1}.
$$

(28)

For the sake of simplicity, let us assume that the initial anisotropy is negligible ($\rho_{10} \approx \rho_{20}$). Then, for small values of induced anisotropy $A$ from Eq. (10) the material factor of the normalized resistance ratio $\xi_m$ can be approximated as follows:

$$
\xi_m \approx \frac{2}{2\varepsilon} = \frac{1}{2\varepsilon} = \frac{1}{\varepsilon} + \frac{2 - \sqrt{\varepsilon}}{6 - 4\sqrt{\varepsilon}}
$$

(29)

and the directionality factor of the square-electrode sensor defined in Eq. (9) is $D \approx 1.71$.

Similarly, in a thin anisotropic plate and assuming that $x_1$, $x_2$ and $x_3$ are aligned with the principal axes of conductivity, the potential function has been derived by Tatarinov [22] as

$$
\varphi = -\frac{1}{2\pi t} \ln \sqrt{\rho_1 x_1^2 + \rho_2 x_2^2 + \rho_3 x_3^2 + \varphi_0}.
$$

(30)

The potential differences and the resulting transfer resistances can be derived like before. After some algebra,

$$
R_{1,2} = \frac{\sqrt{\rho_1 \rho_2 \rho_3}}{2\pi t} \ln \left[ 1 + \frac{\rho_{12}}{\rho_{11}} \right]
$$

(31)

and the resistance ratio becomes

$$
\xi = \frac{\ln(1 + (\rho_{12} / \rho_{11}))}{\ln(1 + (\rho_{12} / \rho_{11}))}.
$$

(32)

Assuming again that the initial anisotropy is negligible and that the induced anisotropy is small,

$$
\xi_m = \frac{\ln(4/(2-A))}{\ln(4/(2+A))} \approx 1 + \frac{A}{\ln(2)},
$$

(33)

therefore the directionality factor of the square-electrode sensor is $D \approx 1.44$.

In summary, the square-electrode PD sensor’s sensitivity to anisotropy $D$ ranges between 1.44 and 1.71 depending on the thickness of the specimen relative to the electrode separation. It is interesting to mention that the directionality factor of a conventional in-line PD sensor on a half-space is $D = -0.5$, i.e., negative. It is also somewhat counterintuitive that for a thin plate, the directionality factor of a conventional in-line PD sensor is not only small, but exactly zero [22].

The weak creep-induced electric anisotropy $A \approx A_e + A_p$ can be crudely approximated as a sum of elastic $A_e$ and plastic $A_p$ contributions produced by reversible and irreversible piezoresistivity, respectively. Both effects are relatively weak, therefore, for the sake of simplicity, an essentially linear relationship between induced anisotropy and strain can be assumed, so that $A_e \approx \rho_{1e}$ and $A_p \approx \rho_{1p}$, where $p$ and $P$ are linearized coefficients of elastic and plastic piezoresistivity, respectively. However, in some cases the nonlinearity of the plastic contribution is so pronounced that it is better to refer to $A_p(\varepsilon_p)$ in terms of a general function.

Piezoresistivity is defined as the stress dependence of the electric resistivity in the material. In the presence of elastic stress $\tau$, the electric resistivity $\rho$ and conductivity $\sigma$ tensors of an initially isotropic conductor become slightly anisotropic. In a normalized form, the linearized elastic piezoelectric relationship can be written as

$$
\begin{align*}
\Delta \rho_{11}/\rho_0 &\approx \frac{p_{11}}{\rho_{11}} + \frac{p_{12}}{\rho_{11}} + \frac{p_{13}}{\rho_{11}} = \frac{\tau_1}{E} \\
\Delta \rho_{12}/\rho_0 &\approx \frac{p_{12}}{\rho_{12}} + \frac{p_{11}}{\rho_{12}} + \frac{p_{13}}{\rho_{12}} = \frac{\tau_2}{E} \\
\Delta \rho_{13}/\rho_0 &\approx \frac{p_{13}}{\rho_{13}} + \frac{p_{11}}{\rho_{13}} + \frac{p_{12}}{\rho_{13}} = \frac{\tau_3}{E}
\end{align*}
$$

(34)

where $E$ is Young’s modulus, $\Delta \rho_i = \rho_i - \rho_0 (i=1.2.3)$ and $\rho_0$ represents the electric resistivity in the absence of stress. In terms of the corresponding un-normalized piezoresistivity tensor $\pi$ of the material, $\rho_{11} = E\pi_{11}$ and $\rho_{12} = E\pi_{12}$ are the unitless axial and lateral piezoresistivity coefficients, respectively [23]. In general, $p$ is an intrinsic material parameter that has a relatively modest magnitude in the range of 0.5–1.0.

The relationship between electric resistivity and plastic strain is usually more nonlinear than the corresponding elastic relationship and strongly depends on the microstructure of the material. For weak effects, the linearized plastic piezoresistivity coefficient $P$ can be defined in a conceptually similar manner as the elastic parameter $p$:

$$
\begin{align*}
\Delta \rho_{11}/\rho_0 &\approx \frac{p_{11}}{\rho_{11}} + \frac{p_{12}}{\rho_{11}} + \frac{p_{13}}{\rho_{11}} = \frac{\varepsilon_{p1}}{E} \\
\Delta \rho_{12}/\rho_0 &\approx \frac{p_{12}}{\rho_{12}} + \frac{p_{11}}{\rho_{12}} + \frac{p_{13}}{\rho_{12}} = \frac{\varepsilon_{p2}}{E} \\
\Delta \rho_{13}/\rho_0 &\approx \frac{p_{13}}{\rho_{13}} + \frac{p_{11}}{\rho_{13}} + \frac{p_{12}}{\rho_{13}} = \frac{\varepsilon_{p3}}{E}
\end{align*}
$$

(35)

The magnitude of $P$ is also relatively modest; it is often in the range of 1–2% for typical creep levels, i.e., well below its linear counterpart $p$. Therefore, overall, the proposed creep monitoring technique is more sensitive to creep-induced dimensional variations than to creep-induced material changes that are sometimes non-monotonic functions of the damage accumulation.

4. Experimental results and discussions

In this section we will present the results of tests in 304 austenitic stainless steel aimed at (i) experimentally validating the main predictions of our former analysis and (ii) illustrating the performance of the in-situ monitoring system based on a square-electrode PD sensor through an example of accelerated creep test at 600 °C.

4.1. Measurement system

The block diagram of the experimental setup used in these tests is shown in Fig. 4. This measurement system offers the level of sensitivity and thermal- and long-term stability necessary in creep monitoring applications. A low-frequency harmonic signal generated by the internal oscillator of the lock-in amplifier drives
a differential output amplifier. The purpose of the latter is to ensure that the spurious common mode electric potential \((V^+ + V^-)/2\) of the sensing electrodes is as small as possible and the finite common mode rejection ratio (CMRR \(\approx 120\) dB) of the following differential amplifier is sufficient to accurately measure the true differential potential \(V^+ - V^-\). The serial output resistance of the differential amplifier was 2 \(\times 50 = 100\) \(\Omega\). In light of inevitable contact resistance variations on the order of 0.1\(-1\) \(\Omega\), this relatively high driving impedance keeps the injection current more or less constant around a chosen nominal value but not stable enough to completely neglect its variation. Therefore, the injected current passes through a magnetic sensor that measures its actual value in order to compensate for any change due to resistance variations in the multiplexer relays, connecting wires, or contact resistances between the electrodes and the specimen.

The multiplexer’s main role is to switch between electrode configurations in order to obtain the two sought resistance values in the axial and lateral directions. In addition, the multiplexer allows us to monitor creep evolution essentially simultaneously at more than one location if necessary. The voltage difference measured between the sensing electrodes is fed through a 1:8 impedance matching step-up transformer into a 100-gain low-noise preamplifier, and then it is measured with a phase-sensitive lock-in amplifier. The measurement system is automated through a LABVIEW program that remotely controls the electronic devices and acquires the measurement data.

The quantity measured in potential drop tests is the voltage difference between the sensing electrodes and the results are presented in the form of a transfer resistance. The latter is taken as the ratio between the real (in-phase) part of the voltage difference \(V^+ - V^-\) between the sensing electrodes and the measured value of the injected current. It should be mentioned that it was essential to optimize the sensor not only to maximize the signal-to-noise ratio but also to accommodate the wide range of magnetic permeability values inherent to power plant steels. These materials range from paramagnetic 304 stainless steel to ferromagnetic low-alloy chromium/molybdenum steels, which require low inspection frequency to assure essentially quasi-static operation. Possible thermoelectric voltages generated in the

sensing circuit due to temperature differences between the contact points are eliminated by the low-frequency ac mode of operation of the monitoring system.

The above described low-frequency ACPD system is capable of accurate measurements down to 1 Hz, which is less than what is necessary even for ferritic steels for small electrode separation of \(a \approx 4-8\) mm. In all the experiments reported below, the injection current, inspection frequency, and electrode separation were kept constant at 140 mA, 3 Hz, and 4 mm, respectively. The measurements were conducted at room temperature unless indicated otherwise. An important parameter to take into consideration when using low-frequency ACPD measurements for creep monitoring is the initial geometrical aspect ratio \(a_{10}/a_{20}\) of the electrode configuration. In practice, it is very difficult to assure that the spot welded electrodes are uniformly spaced in an ideal square-configuration. Fortunately, the earlier described initial normalization effectively suppresses spurious resistance variations caused by mounting errors. However, large initial aspect ratios \(a_{10}/a_{20}\) present a problem in accurate resistivity ratio measurements. It was found that a \(+/-5\) relative positioning error between the electrodes, i.e., only \(+/-0.4\) for 4 mm nominal electrode separation, might cause as much as 25% difference in the initial values of \(R_{10}\) and \(R_{20}\), which causes practical difficulties in maintaining the measurement accuracy necessary for creep monitoring. Therefore, it is of great importance to keep the uncertainty in electrode positioning to a strict minimum.

The sensor consists of the material under test and four Chromel thermocouple wires spot-welded to it as shown in Fig. 5. Up to about 5-10% relative error is acceptable if the measured resistances are normalized to their respective initial values. In order to achieve such a demanding positioning accuracy, a ceramic template was used to guide the wires during spot welding. Another issue of importance is magnetic induction interference within the sensing loop. In order to limit this adverse effect, the sensing loop had to be kept at a strict minimum. For this purpose, eight wires were necessary for two orthogonal resistance measurements and they had to be twisted in pairs and welded at the four electrode spots, two for injection and two for sensing. In this manner one set of four wires is used for the axial measurement and the other set of four wires is utilized for the lateral measurement. Tungsten Inert Gas (TIG) welding was used to create beads for each pair of wires to increase the durability of the contact and these beads were subsequently spot welded to the specimen as shown in Fig. 5.

All 304 stainless steel specimens used in this study were of the same size \((177.80 \times 25.40 \times 6.35\) mm\) and uniform rectangular
shape. The specimens were milled but not polished. The effective penetration depth of the low-frequency ACPD measurement is roughly equal to half of the electrode separation therefore surface roughness caused either by machining or subsequent corrosion exerts a negligible influence on the measurement. It should be also mentioned that creep tests are far less sensitive to surface conditions than fatigue tests.

4.2. Suppression of thermal effects

In order to increase the measurement’s selectivity to directional variations due to creep, the sensor must exhibit low sensitivity to non-directional reversible thermal effects. To investigate this issue, the sensor was tested on 304 stainless steel by exposing it to a cumulative 252-hour heating regime at a temperature of 500 °C. Fig. 6a shows the average normalized resistance \( R = (R_1 + R_2)/2 \) and temperature \( T \) as functions of test time during a reversible six-cycle thermal exposure of 304 stainless steel at 500 °C peak temperature and Fig. 6b shows the resulting variation in the normalized resistance ratio \( \xi \). Due to the high average thermal coefficient of electric resistivity (720 ppm/°C) in 304 stainless steel, the “common” normalized resistance exhibits an increase of about 45% while the accompanying change in \( \xi \) is less than \( \pm 0.17\% \). The relatively small changes observed in resistance ratio are likely to be due to initial cold work in the material that led to subsequent directional microstructural changes during thermal exposure. Overall, the observed variations in \( \xi \) can be considered mostly reversible since there was practically no remnant change at room temperature. Let us introduce the thermal common mode rejection ratio TCMRR = \( \Delta R/\Delta \xi \) of the sensor to characterize its ability to suppress changes common to both axial and lateral resistance measurements due to thermal effects. For the reversible thermal effects on 304 stainless steel, the TCMRR typically reaches values higher than 250. This high level of TCMRR indicates that the creep sensor can efficiently suppress very large reversible resistance variations due to wide temperature changes expected during in-situ creep monitoring.

Overall, the irreversible TCMRR seems to be a little lower and strongly material dependent, but a value of \( \approx 10 \) is usually achieved even in specimens that are strongly textured in their initial state. However, the irreversible thermal changes in \( R_{1,2} \) are not expected to exceed 10%, therefore the corresponding effect on \( \xi \) is around 1%, similar to the much larger, but better suppressed reversible thermal effects. Fig. 7a shows the average normalized resistance ratio \( \eta \) and temperature \( T \) as functions of test time during an irreversible four-cycle thermal exposure of 304 stainless steel bar stock at 650 °C and Fig. 7b shows the resulting variation of the normalized resistance ratio \( \xi \). At this elevated temperature, the normalized resistance ratio drifted as much as 0.6% and might exhibit an additional 0.3% reversible oscillation between room temperature and 650 °C. This small yet perceivable irreversible thermal effect seems to be an intrinsic feature of the sensor on specimens exhibiting strong initial manufacturing texture. However, it represents only a small impact on the overall performance of the sensor; thanks to the high sensitivity of \( \xi \) to creep strain as was shown earlier. In effect, 1% plastic strain causes \( \approx 5\% \) change in \( \xi \); therefore 0.6% irreversible thermal change in the normalized resistance ratio represents a very low \( \approx 0.1\% \) detection threshold in terms of creep strain.

4.3. Gage factor measurements

The creep monitoring sensor has been tested on 304 stainless steel under elastic and plastic strain conditions at room temperature. Fig. 8 shows smoothed 3-Hz ACPD resistance measurements with a square-electrode sensor in the (a) elastic and (b) plastic regimes of 304 stainless steel at room temperature.

![Fig. 6. Normalized average resistance (a) and normalized resistance ratio (b) variations during reversible thermal exposure of 304 stainless steel at 500 °C.](image)

![Fig. 7. Normalized average resistance (a) and normalized resistance ratio (b) variations during irreversible thermal exposure of 304 stainless steel at 650 °C.](image)
In the elastic regime alternating axial load was applied to the specimen at a cyclic frequency of 0.01 Hz. The maximum tensile load resulting in almost 0.15% elastic strain was chosen to be about twice as high as the maximum compressive load in order to minimize the possibility of buckling in the slender rectangular bars. The results yield yield values of $f_1 \approx 2.95$ and $f_2 \approx -0.86$ for an overall elastic geometrical gage factor of $f \approx 3.81$, which is within reasonable agreement with the theoretical bounds (3.75–4.44) calculated earlier.

For the plastic regime, specimens were subjected to gradually increasing uniaxial tensile stress up to failure, which occurs at approximately 10% strain in 304 stainless steel at room temperature. Fig. 8b shows an example of normalized resistance change versus plastic strain. The measured values were $F_1 \approx 2.74$ and $F_2 \approx -1.96$ for an overall plastic geometrical gage factor of $F \approx 4.70$, which is also in agreement with the theoretical bounds (4.33–5.12) calculated earlier. In summary, Fig. 9 illustrates the acceptable agreement between the experimentally determined values of the elastic and plastic geometrical gage factors for several specimens and their respective theoretical bounds.

Next, we used non-contacting eddy current conductivity measurements to determine the material gage factor independently from the usually much stronger geometrical effect. Fig. 10 shows the results of elastic piezoresistivity measurement on 304 stainless steel. The reversible conductivity variation was measured using a Nortec 2000S eddy current instrument with a racetrack-shaped eddy current probe coil of aspect ratio 4 at 300 kHz using calibration blocks of 1.48% IACS and 2.53% IACS. By rotating this directional probe coil around its own axis, the apparent eddy current conductivity (AECC) was measured separately along the axial and lateral directions in the surface of the specimen [23]. The axial and lateral piezoelectric coefficients were found to be $p_{11} \approx 1.35$ and $p_{12} \approx 1.95$, respectively (negative conductivity changes correspond to positive resistivity changes). Therefore, the differential piezoelectric coefficient is $p = p_{11} - p_{12} \approx -0.6$. For the sake of simplicity, this measurement was only done at room temperature since the elastic piezoresistivity is not expected to change much with temperature.

The irreversible electric anisotropy caused by plastic strain was measured using the same eddy current system at a frequency of 2 MHz. Fig. 11 shows the average AECC results for one intact specimen and six others that were plastically strained to 15% at room temperature. At room temperature, the AECC difference between the axial and lateral directions is approximately $A \approx 1.7\%$. However, this difference gradually diminishes as the specimens are deformed at increasing temperatures and almost completely vanishes at 250 °C where the deformed specimen exhibits anisotropy $\approx 0.7\%$ very similar to that of the intact material. The largest electric anisotropy $A \approx -1.7\%$ caused by plastic strain $x_p \approx 15\%$ at room temperature corresponds to a linearized plastic piezoresistivity coefficient of only $P = p_{11} - p_{12} \approx -0.1$. Since the directionality factor of a square-electrode PD probe is $D \approx 1.4 - 1.7$, even this level of deformation-induced anisotropy would cause only less than 0.2% change in the material factor $\zeta_m \approx 1 + DA$ of the normalized resistance ratio, not to mention that the effect becomes weaker at elevated temperatures. Generally, material effects remain negligible until creep causes severe microscopic damage, such as
reached a value of around 410 h. Just before rupture, the resistance ratio had risen sharply at an accelerating rate until failure occurred at 290 MPa, the rate of increase of the normalized resistance ratio creep rate progressively higher. At the final tensile stress level of 290 MPa at 600 °C, the loading of 190 MPa was applied, which made the resistance ratio remained practically constant. After 27 h, initial mechanical purely thermal exposure or soaking, therefore the resistance ratio of the axial and lateral resistances, the sensor can distinguish

aligned clusters of cavities and preferentially oriented micro-cracks, in the material.

4.4. Creep monitoring

The main advantage of the proposed directional ACPD sensor is that it is capable of detecting creep-induced damage sufficiently early in time during the degradation process to be of use for life management purposes. The high thermal and temporal stability of the proposed monitoring system as well as its high sensitivity to creep-induced geometrical changes and, to a lesser degree, anisotropic texture formation makes it uniquely suitable for this purpose. The directional ACPD sensor is sensitive to deformation in both elastic and plastic regimes. It is also sensitive to the initiation and growth of microcracks due to their particular directionality in the case of uniaxial loading. With a predicted strain threshold of better than 0.1%, the sensor can detect low levels of plastic deformation during creep.

For demonstration purposes, the system was tested on 304 stainless steel specimens under accelerated creep conditions. Fig. 12 shows an example of the creep stress (a) and normalized resistance ratio (b) history of a specimen loaded between 190 and 290 MPa at 600 °C. First, the specimen was only subjected to purely thermal exposure or soaking, therefore the resistance ratio remained practically constant. After 27 h, initial mechanical loading of 190 MPa was applied, which made the resistance ratio increase at a very slow rate. Between 90 and 360 h, the loading was increased in six steps by a total of 100 MPa, which made the creep rate progressively higher. At the final tensile stress level of 290 MPa, the rate of increase of the normalized resistance ratio rose sharply at an accelerating rate until failure occurred at around 410 h. Just before rupture, the resistance ratio had reached a value of ≈ 3.6 while the recorded creep strain was ≈ 25.6% (rupture occurred very close to, but outside the square-electrode probe). This observation supports the expectation that the material gage factor increases in later stages of creep when microcracks coalescence into bigger cracks and cause an apparent jump in electrical anisotropy.

5. Conclusions

This work analyzed the feasibility of a directional ACPD probe with square-electrode configuration for in-situ monitoring of creep in high-temperature environments. By detecting variations in the ratio of the axial and lateral resistances, the sensor can distinguish directional creep damage from mostly non-directional thermally-activated microstructural evolution. The main advantage of directional electric resistance measurements over non-directional ones is the capability of detecting creep at its early stages, which is still a challenge to most existing NDE methods. Both analytical predictions and experimental tests conducted on 304 stainless steel showed promising results for in-situ monitoring of creep. It was found that under uniaxial stress square-electrode PD sensors exhibit geometrical gage factors of about 4 and 5 in the elastic and plastic regimes, respectively, i.e., more than twice those of conventional strain gauges. Experimental results obtained on stainless steel 304 using a square-electrode PD creep sensor agreed well with these theoretical predictions.

The sensitivity of the PD creep sensor to material effects was also investigated. In order to optimize the sensitivity of the probe to material damage, we used a square-electrode configuration, which is at least three times more directional than the common in-line configuration. However, the probe’s sensitivity to creep-induced electric anisotropy is still relatively low, especially in materials with cubic symmetry that do not exhibit crystallographic electric anisotropy due to preferred grain orientation. In this way, the creep sensor is mostly sensitive to geometrical effects up to the initiation of preferentially oriented microcracks and the monitored resistance ratio is initially proportional to the creep strain. Finally, an accelerated creep test in a 304 stainless steel specimen at 600 °C was used to illustrate the attractive features of directional ACPD measurements for in-situ creep monitoring.

Due to its high geometrical gage factor, a permanently installed directional ACPD probe acts essentially as a high-temperature strain gage throughout most of the creep life of the monitored component. In most cases, the influence of electric resistivity anisotropy caused by preferentially oriented material defects remains negligible until the last phase of secondary creep and becomes significant only in the tertiary stage of creep just
before rupture. Deployable directional ACPD probes with spring-loaded contact pins can be used to selectively assess the otherwise negligible material effects of creep. Because of the inevitably larger positioning error of spring-loaded electrodes, the measurement uncertainty of such deployable probes is approximately one order of magnitude higher than their permanently mounted counterparts. At the same time, in the absence of geometrical effects of deformation, the sensitivity to creep is also much lower. Still, such deployable probes might find application in nondestructive creep assessment because they can be used to scan larger areas of interest, including welds and the crucial boundary region between welds and the neighboring heat-affected zone of the base metal.

Acknowledgment

This work was supported by the Nuclear Energy University Program (NEUP) of the US Department of Energy subcontract 00101721 issued under prime contract DE-AC07-05ID14517 to Battelle Energy Alliance, LLC. The authors would like to acknowledge valuable scientific collaboration with Giuseppe Sposito, Catrin M. Davies and Peter Cawley of the UK Research Centre in NDE at Imperial College London.

References