AD-A088 409

TECHNICAL: LIBRARY

AD

AD-E400 457

SPECIAL PUBLICATION ARSCD-SP-80001

A COMPARISON OF RESIDUAL STRESS MEASURING TECHNIQUES: THEIR STRENGTHS AND WEAKNESSES

FRED M. WITT FEE M. LEE WALTER M. RIDER

AUGUST 1980



US ARMY ARMAMENT RESEARCH AND DEVELOPMENT COMMAND FIRE CONTROL AND SMALL CALIBER WEAPON SYSTEMS LABORATORY DOVER, NEW JERSEY

APPROVED FOR PUBLIC RELEASE; DISTRIBUTION UNLIMITED.

The views, opinions, and/or findings contained in this report are those of the author(s) and should not be construed as an official Department of the Army position, policy or decision, unless so designated by other documentation.

Destroy this report when no longer needed. Do not return it to the originator.

The citation in this report of the names of commercial firms or commercially available products or services does not constitute official endorsement or approval of such commercial firms, products, or services by the United States Government.

| UNCLASSIFIED | Entered) | | | |
|---|--|--|--|--|
| REPORT DOCUMENTATION | PAGE | READ INSTRUCTIONS | | |
| 1. REPORT NUMBER | 2. GOVT ACCESSION NO. | 3. RECIPIENT'S CATALOG NUMBER | | |
| Special Publication ARLCD-SP-80001 | | | | |
| 4. TITLE (and Subtitle) | | S. TYPE OF REPORT & PERIOD COVERED | | |
| A Comparison of Residual Stress N | Measuring | | | |
| Techniques: Their Strengths and | Weaknesses | | | |
| | | S. PERIORANIAO GRO. REI GRI NOMBER | | |
| 7. AUTHOR(s) | | 8. CONTRACT OR GRANT NUMBER(s) | | |
| Fred Will Fee M. Lee | | | | |
| Walter M. Rider | | | | |
| 9. PERFORMING ORGANIZATION NAME AND ADDRESS | | 10. PROGRAM ELEMENT, PROJECT, TASK | | |
| ARRADCOM, FC&SCWSL | | | | |
| MGMI DIV. (DKDAR-SCM-P) Dover NI 07801 | | | | |
| 11 CONTROLLING OFFICE NAME AND ADDRESS | | DA Project ILI61101A91A | | |
| ARRADCOM, TSD | | Aug 1980 | | |
| STINFO Div. (DRDAR-TSS) | | 13. NUMBER OF PAGES | | |
| Dover, NJ U/801 | at from Controlling Office) | 31 IS SECURITY CLASS (of this report) | | |
| NA MONTONING AGENCI NAME & ADDRESSIN BINNE | | | | |
| | | UNCERSS IF IED | | |
| | | 150. DECLASSIFICATION/DOWNGRADING SCHEDULE | | |
| 15. DISTRIBUTION STATEMENT (of this Report) | | | | |
| 17. DISTRIBUTION STATEMENT (of the ebstrect entered | fn Block 20, ff different fro | m Report) | | |
| 18. SUPPLEMENTARY NOTES | , , , , , , , , , , , , , , , , , | | | |
| | | | | |
| | | | | |
| 19. KEY WORDS (Continue on reverse side if necessary as | nd identify by block number) | | | |
| Residual stressElectricStrain gage methodsAbrasiveX-ray methodsStress cRotating cuttersPoint ga | discharge machir jet machining oncentration fact ge | ning Line shift Subgrain interior cor | | |
| 20. ABSTRACT (Continue on reverse side H necessary and Ongoing in-house work on the mea by both X-ray and strain gage me compared. The X-ray portion inc of stresses in highly textured s effects produced by various mech fields sensed by the active gage errors introduced by considering as a point gage instead of one w | d Identify by block number) surement of resid chanical relaxati ludes problems as amples while the anical methods t elements. Compa the active eleme ith finite length | dual stress in Army Materiel on methods is presented and ssociated with the calculation strain gage portion describes to partially relieve the strain arisons are also made of the ent in the strain gage rosette | | |

DD 1 JAN 73 1473 EDITION OF TNOV 65 IS OBSOLETE

UNCLASSIFIED

SECURITY CLASSIFICATION OF THIS PAGE (When Data Entered)

SECURITY CLASSIFICATION OF THIS PAGE(When Date Entered)

TABLE OF CONTENTS

| | | Page N | .C | | | |
|-----|---|--------|----|--|--|--|
| Int | roduction | 1 | | | | |
| Cir | cumferential Stress by Longitudinal Slitting | 1 | | | | |
| Bli | nd Hole Drilling Methods | 3 | | | | |
| | Hole Forming Methods | 4 | | | | |
| | Abrasive Jet Method | 7 | | | | |
| | Indeterminacy of Beta | 9 | | | | |
| Eff | ect of the Central Hole on the Measured Stress | 9 | | | | |
| | X-ray Measurement of Stress | 11 | | | | |
| Sum | mary | 14 | | | | |
| Ref | erences | 15 | | | | |
| Dis | tribution List | 19 | | | | |
| | FIGURES | | | | | |
| 1 | Crampton's method for computing the circumferential stress in thin wall tubes | 2 | | | | |
| 2 | 2 Relationship between the linear strain gage elements and the principal directions | | | | | |
| 3 | 3 Strain effects caused by various hole producing methods | | | | | |
| 4 | Experimental setup for abrasive jet machining | 7 | | | | |
| 5 | Effect of the stress concentration factor on the state of stress in a uniaxially stressed plate | 10 | | | | |
| 6 | Interplanar spacing plot for different residual stress states | 12 | | | | |

Page No.

7 Lattice and total stress-strain curve 13

TABLE

Effect of using point gage versus finite length gage 8 equations in the computation of residual stress

INTRODUCTION

Residual stresses are those stresses which reside in the bulk of a material when the external forces acting on it are removed. One customarily excludes external forces such as gravity or thermal gradients. Since the body under consideration is in equilibrium, the resultant force equals zero. This report describes methods used to circumvent some pitfalls encountered when standard X-ray and strain gage methods are indiscriminately used to measure residual stress.

When the residual stresses are long range in nature, that is, are reasonably constant in magnitude, sign, and direction, and extend over distances comparable to many grain diameters, they are termed macrostresses. As such they are easily measured by mechanical methods employing dissection, layer removal, and careful hole-drilling procedures. Stresses of this type produce an X-ray line shift when the sample is rotated in an X-ray beam. On the other hand, when the residual stresses are short range in nature, and vary appreciably over distances comparable to the grain diameter, they are called microstresses. These cannot be detected by mechanical dissection methods and may or may not produce an X-ray line shift, depending on the distances over which the microstresses exert their influence. For example, when the stress acting distance is approximately a micron, X-ray line broadening and line shifts are observed. As this distance shrinks to approximately 1000 Å or less, X-ray line broadening persists, but line shifts are not observed.

Furthermore, when the actual deformation mode produces extensive movement of material in a given direction, complications arise which prohibit the use of X-ray methods and computational formulas. In other words, the standard X-ray analysis can produce erroneous results for cases where the specimens have been extensively rolled, drawn, stretched, compressed, or bent. The standard X-ray methods do, however, produce reliable answers when the measurement is made on surfaces which have been machined, ground, or peened (ref 1). Here the mass movement is not predominantly unidirectional. A relatively unknown, but straightforward way, does exist, however, to correct for the deficiencies of the standard X-ray method when extensive amounts of uniaxial plastic metal flow has occurred. The method, reported by Marion and Cohen (ref 2), will be described in a later section. Some discussion will now be given concerning several problems the authors have encountered, using some of the "handbook" approaches to determine the stress state of various specimens.

CIRCUMFERENTIAL STRESS BY LONGITUDINAL SLITTING

The stress condition of thin wall tubes is sometimes assessed by



Figure 1. Crampton's method for computing the circumferential stress in thin wall tubes.

Crampton's method (figure 1). Here one slits a tube its entire length along one element and makes use of the beam formula to compute the maximum circumferential stress. The method assumes that a condition of plane strain exists. In other words, the strain and curvature change in the longitudinal direction are zero. According to Crampton, the circumferential stress on the outer fibers is given by,

$$\sigma_{c} = \frac{Et}{(1-\nu^2)} \left[\frac{1}{D_0} - \frac{1}{D_1} \right]$$
(1)

Where E is Young's modulus, t is the wall thickness, v is Poisson's ratio, D_0 is the mean diameter before slitting, and D_1 is the mean diameter after slitting. It can be seen from equation 1 that a tensile stress on the outside surface of the tube causes D_1 to be larger than D_0 ; while a compressive stress causes D_1 to be smaller than D_0 .

It is possible to obtain some indication of the reasonableness of the computed Crampton result by noticing that the length of the thin wall tube does not enter into the formula. Consequently, after axial slicing, and the D_1 relaxation is noted, one can perform another cut, perpendicular to the cylinder axis, so two slit cylinders, each of length $\ell/2$ can be obtained. These individual cylinders, can be examined concerning their new D_1 values. If the new values differ considerably from the value produced by the first axial cut, then one can conclude that constant axial stress does not exist. The requirement that the stress varies linearly from inner wall to outer wall is illustrated in figure 1(b). Here, the shaded area represents the stress distribution according to the beam requirement. The sinusoidal type curve depicted therein might, however, be closer to the actual distribution. It is concluded that the use of slitting techniques provide, at best, a crude indication of the actual stress state in a given specimen. More accurate methods are described below.

BLIND HOLE DRILLING METHODS

It should be noted that residual stresses cannot be measured directly. One must first determine the magnitude and alegebraic sign of the strain that exists in a given sample under examination and then compute the residual stresses from the strain information. To obtain the strain information, the state of constraint of the material just beneath the strain gage is altered by removing a small amount of material adjacent to the gage. This is achieved by drilling a small hole in the center of a rosette, consisting of three linear gages (figure 2) bonded to the specimen and, connected to a digital strain indicator. The drilling operation relaxes the material at the edge of the hole, causes a local redistribution of the stress, which in-turn produces the strain change detected by the strain gages in the rosette.

It has been experimentally observed that the relaxed strains depend on the depth of the drilled hole. When the hole depth approximates the diameter, further drilling does not significantly change the strain gage readings; hence one normally takes as equilibrium surface strain values those obtained at a depth equal to the diameter of the drilled hole. Unless special precautions are taken when the hole is drilled, the strain gage readings will not only reflect the residual strains/stresses in the sample but will also reflect the strains produced by the hole drilling operation.

For a given state of stress, the strain gages sense relaxation signals which are proportional to the diameter of the drilled hole. It is desirable to obtain a strong signal by drilling a large hole. Care must be exercised, however, in selecting the drill size so that the cutting surface area is not too close to the gages and unduly perturb the true strain relaxation signal from the drilling operation. Holes of 0.159-cm (1/16-inch) diameter are normally used in practice, because



Figure 2. Relationship between the linear strain gage elements and the principal directions,

they provide relatively strong and unperturbed relaxation signals.

The radial distance from the center of the hole to the three linear elements in the rosette is critical because the strains are functions of the inverse second and inverse fourth powers of this radius. Therefore, one should use commercially available precision strain-gage rosettes and should not attempt to construct a homemade rosette by gluing down three linear gages because the formulas used to calculate the stress state assume that all three linear gages lie precisely on the same circle of radius R, and are symmetrically oriented with respect to each other; as shown in figure 2.

Hole Forming Methods

Various techniques have been described in the literature to produce the center hole in the strain gage rosette. These include the use of conventional drill bits, specially ground milling cutters, electric discharge machining, and abrasive jet machining.

Bush and Kromer (ref 3) have investigated some problems associated with the use of various methods to produce the center hole. Their results for annealed steel are shown in figure 3. It can be seen, for conventional drill bits, that unpredictable errors, ranging from 60 $\mu\epsilon$ to -70 $\mu\epsilon$ can result. When these strain values are converted into stress, they amount to an error of + 124.11 MPa (+ 18 ksi). The rotating cutters cause an error ranging from -30 $\mu\epsilon$ to -60 $\mu\epsilon$.



Figure 3. Strain effects caused by various hole producing methods.

The electric discharge machining can cause errors ranging from 30 $\mu\epsilon$ to -80 $\mu\epsilon$, depending on the hole diameter. The superiority of the abrasive jet machining method is obvious from the extremely low strain values of 2 or 5 $\mu\epsilon$ shown in figure 3. This method will be described in more detail later. For now, consideration will be given to the equations normally used to convert the measured strains into computed stresses.

The equations describing the principal stresses and their orientation with respect to the number three gage can be written in two different ways for the Blind Hole Method. This choice depends on whether or not one considers the linear gage element as a point gage (i.e., of infinitesimal length), or as one with a finite length, centered on the specimen, at the same location as the point gage. For the radially oriented point gage case, the principal stresses and their directions are given by (ref 4):

$$\sigma_{p} = \frac{\varepsilon_{3}(A + B \cos 2\beta) - \varepsilon_{1}(A - B \cos 2\beta)}{4AB \cos 2\beta}$$

$$\sigma_{q} = \frac{\varepsilon_{1}(A + B \cos 2\beta) - \varepsilon_{3}(A - B \cos 2\beta)}{4AB \cos 2\beta}$$

$$\beta = \frac{1}{2} \tan^{-1} \frac{(\varepsilon_{3} - 2\varepsilon_{2} + \varepsilon_{1})}{(\varepsilon_{3} - \varepsilon_{1})}$$

where

$$A = \frac{-(1 + \nu)}{2Er^{2}}$$

$$B = -\frac{(1 + \nu)}{2E} \left[\left(\frac{4}{1 + \nu} \right) - \frac{1}{r^{2}} - \frac{3}{r^{4}} \right]$$
(2)

 ϵ_1 , ϵ_2 , and ϵ_3 are the measured strains, E and ν are the elastic modulus and Poisson's ratio, respectively, and r is the ratio of the radius of the rosette to the radius of the drilled hole, R/R_o . The quantities σ_p and σ_q are the principal residual stress, while the angle β is measured to the number three gage element (fig. 2). When the equations listed above are used, it should be realized that the strain gages are assumed to act as point gages, i.e., of zero gage length. For the finite length gage, the equations for the principal stresses become (ref 5)

$$\sigma_{p,q} = E \left[\frac{\varepsilon_1 + \varepsilon_3}{2S_1} + \frac{\varepsilon_1 - \varepsilon_3}{2S_2 \cos 2\beta_1} \right]$$

where

$$S_1 = (v-1) + R_1 \text{ and } S_2 = -(v+1) + R_2$$
 (3)

here

$$R_{1} = \frac{1}{(r_{2} - r_{1})} \int_{r_{1}}^{r^{2}} (1 - \frac{a}{r^{2}}) - v (1 + \frac{a^{2}}{r^{2}}) dr$$

$$R_{2} = \frac{1}{(r_{2} - r_{1})} \int_{r_{1}}^{r_{2}} ((1 - \frac{3a^{4}}{r^{4}} - \frac{4a^{2}}{r^{2}}) + v(1 + \frac{3a^{4}}{r^{4}})) dr,$$

where r_1 and r_2 are the inner and outer edge dimensions of the active gage element, respectively, and a is the radius of the drilled hole.

An attempt was made to compare the difference in the computed stress value when both equation 2 and equation 3 were used with the same input strain data. The results are given in the table where it can be seen that the point gage equations (i.e., equations 2) produce numerical values for the residual stresses which are 10 to 20 percent higher than those produced by the finite length equations (i.e., equations 3).

Abrasive Jet Method

In this method, see figure 4, a carefully controlled stream of gas, containing 50 μ aluminum oxide powder, is directed against the workpiece in a manner which chips away microscopic particles of the material to drill the nominal 0.159-cm (1/16-inch) diameter hole.



Figure 4. Experimental setup for abrasive jet machining.

Table. The effect of using point gage versus finite length gage equations in the computation of the residual stress

| | | | | _ | | | | |
|----------------------------|----------------------|---------------|----------------|---------------|---------------------------|---------------|---------------|---------------|
| | B Degree | | 38 | 56 | . ര | 33 | 28 | 38 |
| | INCTH GAGE MATION | MPa (KSI) | -226.2(-32.8) | -163.4(-23.7) | -174.4(-25.3) | -183.4(-26.6) | -164.8(-23.9) | -187.5(-27.2) |
| S USING FINITE LE | MPa (KSI) | -206.9(-30.0) | -196.5(-28.5) | -199.3(-28.9) | -224.8(-32.6) | -128.2(-18.6) | -248.2(-36.0) | |
| SSIDUAL STRES | AGE ATION | MPa (KSI) | -273.7(-39.7) | -184.8(-26.8) | -193.1 (-28.0) | -204.8(-29.7) | -180.0426.1) | -209.66-30.4) |
| I | COMPUTA COMPUTA | TPa (KEI) | -252.4(36.6) | -218.6(-31.7) | -219.3 (31.8) | -246.8(35.8) | -143.4(-20.8) | -239.6439.3) |
| STRAIN | | | 244 | 436 | 498 | 462 | 250 | 518 |
| | 51 | пс | 505 | 313 | 423 | 323 | 80 | 219 |
| | | | 472 | 327 | 357 | 378 | 346 | 434 |
| ROSETTE HOLE RADIUS | | mm (inch) | 0.81(0.032) | 0.81(0.032) | 0.86(0.034) | 0.81(0.032) | 0.81(0.032) | 0.81(c.032) |
| | | | A ¹ | B.1 | Å 1 | в 1. | A. 1. | c) Pů |
| SAMPLE | | | Ъ | | CN | | m | |

1. Hole was drilled by undercut end mill.

2. Hole produced by abrasive machining.

As seen in figure 3, the abrasive jet machining method appears to be the most gentle one to produce the central hole. Special precautions must be taken however to insure that the walls are relatively square with the surface on which the rosette' is bonded. It is also desirable to insure that the hole is accurately positioned at the center of the rosette. For the cases where the hole is off-center, the iterative method proposed by Sandifer and Bowie (ref 6) can be used to obtain corrected values for the stress. When the hole is centered to within a few thousandths of an inch, the iterative method is not needed. Under certain circumstances, however, the formula used to compute the direction of the principal stress can be subject to large errors when small errors exist in the measured strain readings.

Indeterminacy of Beta

The quantities ε_1 , ε_2 , and ε_3 are equal in an equal biaxial stress field. When these values are inserted into the expression for β in equation 2 they produce a value for β of 0 degrees. When $\varepsilon_1 = \varepsilon_3 > \varepsilon_2$ then β approaches +45 degrees. When $\varepsilon_1 = \varepsilon_3 > \varepsilon_2$, then β approaches -45 degrees. In other words, slight errors in the measurement of ε_2 can produce large errors in the computed value for β . The two points to be emphasized are: (1) in an equal biaxial stress field the angle β , describing the principal stress directions, can fluctuate wildly; and (2) the algebraic sign and magnitude of the computed stress are not affected by this uncertainty in beta.

EFFECT OF THE CENTRAL HOLE ON THE MEASURED STRESS

It is well known from strain measurements and photoelastic experiments that geometrical discontinuities such as holes or notches act as stress raisers in plate material under tension. This multiplication of stress can be expressed in terms of a theoretical stress concentration factor K, given by the ratio of the maximum stress to the nominal stress on the net section. In other words,

 $K_{+} = \sigma \max / \sigma \operatorname{nominal}$

Both longitudinal and radial stresses are produced around a circular hole in material subjected to a load. When a uniaxial stress is present, and the hole is many diameters from the edge of the specimen, the radial, theta, and the shear components of stress are given by the Kirsch (ref 7) equations, namely:

$$\sigma_{rr} = \frac{\sigma}{2} \left(1 - \frac{a^2}{r^2}\right) + \frac{\sigma}{2} \left(1 + \frac{3a^4}{r^4} - \frac{4a^2}{r^2}\right) \cos 2\theta \qquad (4)$$

$$\sigma_{\Theta\Theta} = \frac{\sigma}{2} \left(1 + \frac{a^2}{r^2} \right) - \frac{\sigma}{2} \left(1 + \frac{3a^4}{r^4} \right) \cos 2\Theta$$
$$\tau_{\mathbf{r}\Theta} = -\frac{\sigma}{2} \left(1 - \frac{3a^4}{r^4} + \frac{2a^2}{r^2} \right) \sin 2\Theta$$

Here, σ is the uniform applied tensile stress far removed from the hole. The quantity a is the hole radius, while r is the location where the r, Θ components of stress are desired. From the above equations, and inspection of figure 5, it can be seen that the maximum stress of 3 σ occurs at points C and A. Here, r = a, and theta = 0° and 180°. In other words, $\sigma_{\Theta\Theta} = 3\sigma = \sigma$ max. Hence, K_t = 3. It can also be seen that $\sigma_{\Theta\Theta} = -\sigma$ at points D and B when r = a and Θ = 90° and 270°, respectively. Hence, a tensile stress, far removed from the hole, produces at points B and D, a compressive stress of equal magnitude.



Figure 5. Effect of the stress concentration factor on the state of stress in a uniaxially stressed plate.

The net effect of the stress concentration factor is to produce plasticity at the edge of the drilled hole when the measured stress exceeds one-third of the yield stress for cases where the gages are positioned in a uniaxial field. When the gage is positioned in an equal biaxial field, plasticity occurs when the measured stress exceeds one-half of the yield stress. Beaney and Procter (ref 8) have shown, for the uniaxial case, that at stress values less than half yield, the errors are negligible while at full yield, the errors can rise to 10 percent.

X-ray Measurement of Stress

In brief, the X-ray method measures the shift of the diffracted beam when the sample surface is rotated a known amount in the incident beam. From the measured peak shift one computes the change in the lattice spacing and attributes the shift to residual macrostresses. In other words, the interplanar spacing change is used as a strain gage indicator to compute the biaxial stress residing on the outer surface of the sample. The mathematics describing the X-ray method is adequately described in section 2 of an SAE booklet (ref 9). Equation 31 of Section 2 in (ref 9) shows that the stress is given by

$$\sigma = \frac{E}{(1+\nu)} - \frac{1}{\sin^2\psi} - \frac{\cot\theta}{2} - \frac{\pi}{180} \left(2\theta - 2\theta\psi\right) (5)$$

where

E is the modulus of elasticity

v is Poisson's ratio

- $\boldsymbol{\psi}$ is the angle the sample normal was rotated in the X-ray beam
- θ is the angle of incidence of the X-ray beam with the atomic planes
- 20 is the angular position of the diffracted X-ray beam when the sample bisects the incident and diffracted X-ray beams
- $2\theta\psi$ is the position of the diffracted X-ray beam when the sample is rotated an additional ψ degrees about a vertical axis lying in the plane of the sample and perpendicular to the plane of the incident and diffracted X-ray beams

There is some controversy in the literature concerning the appropriate values to use for Young's modulus and Poisson's ratio; however, the SAE paper (ref 9) gives a technique in Section 6.4 for experimentally

determining these quantities.



SING (SPECIMEN THIT ANGLE)



Equation 5 implies that the interplanar spacing, d, is linearly dependent on $\sin^2\psi$. From figure 6 it can be seen that a positive slope is produced by a tensile stress, a negative slope by a compressive stress, and a slope of zero by a residual stress of zero. For highly textured samples, the interplanar spacing values oscillate above a least square line drawn through the experimental data points. It is found, experimentally, that the curve is above the mean straight line for those orientations where the reflecting pole density is higher than the random level and below it where it is lower than the random level. It is believed that the larger-than-expected values for the interplanar spacing results from stress relaxation by dynamic recovery.

To understand these findings, the shape of the modified stressstrain curve shown in figure 7 and proposed by Cullity (refs 10 and 11), and Smith and Wood (ref 12) should be evaluated. It should be noted that the positive abscissa describes the contraction of the (hk1) spacing when the stress direction is at right angles to the normal of the diffracting grains. Curve A-B-C, the total strain curve, consists of an elastic portion, A-B, and an elastic-plastic portion, B-C. In the elastic region, the strain measured by X-rays, called the lattice strain, and that measured by mechanical methods (i.e., total strain) are both proportional to the applied stress, and have the same slope. In other words, the lattice strain curve and the mechanically measured total strain curve are superimposed on each other along the path A-B. In addition, both curves return to zero when the applied stress is removed if the material has not been taken into the yield region. When the applied stresses exceed the elastic limit, the X-ray method presupposes that the lattice strain follows the line B-D. In practice, however, it deviates significantly from the line B-D and follows the curve B-E. As the applied stress is released the lattice strain curve follows the path E-F, and has the same slope as the path A-B.



Figure 7. Lattice and total stress-strain curve.

Thus, this result will produce a net mean compressive residual lattice strain which is represented by F-A, with large fluctuations from the mean being exhibited by individual diffracting crystallities. This net residual lattice strain produces an observed X-ray line shift which is interpreted by the X-ray stress formula as an indication of the presence of compressive macrostrain in the specimen. The picture becomes clearer when one considers the findings of other researchers (refs 13, 14, and 15) and also remembers that the X-ray diffraction signal arises predominantly from coherently diffracting domains which are centered in the subgrain interior regions.

Cullity proposes a rather new explanation for the anomalous computed residual stress values for uniaxially plastically elongated materials. He cites the work of Keh and Weissman (ref 16), Neurath and Waite (ref 17), and Morrill (ref 18) which indicate that, in uniaxially elongated metals, the subgrain walls remain in tension. Because the

X-ray beam predominately samples the subgrain interior, it can readily be seen that equation 5 is preconditioned to predict overall compressive stresses in the sample. The reverse prediction would occur if the subgrains were put into tension, by uniaxial compression.

Marion and Cohen (ref 2) have extended the work of Weidemann (ref 9) and have put forth the formula given below to describe the behavior of the interplanar spacing $d\phi,\psi$ on $\sin^2\psi$. Here the nonlinear dependence of d on $\sin^2\psi$ is attributed to the relief of microstrains in subgrain interiors. The degree of relief depends on the orientation of the grain to the stressing system and to the texture developed during the plastic deformation process. The Marion-Cohen approach separates the nonlinear and linear components of d by means of the expression

$$d\phi,\psi = (d_{\max} - d_B) f(\alpha,\beta) + d_{\perp} \frac{(1 + \mathbf{v})}{E} \sigma_{\phi} \sin^2 \psi + d_B$$
(6)

The term d_{max} is the interplanar spacing in a region where the strain is fully relieved, while d_B corresponds to a region which has not been relieved. This means that the quantity $(d_{max} - d_B)$ describes the range of interplanar spacings present in the sample. The distribution function $f(\alpha,\beta)$, describes the orientation dependence of d and is obtained from the observed integrated intensity of the diffraction peak at each ψ inclination. The distribution function is normalized to unity. Equation 6 is solved by measuring the interplanar spacing, $d\phi, \psi$, and the normalized distribution function, $f(\alpha,3)$, as a function of $\sin^2\psi$ for six orientations of the sample. This overdetermined system of equations is solved by means of a least squares computation to yield the stress, $\sigma\phi$. The necessity of using equation 6 for a given sample is readily determined by plotting interplanar spacing measurements as a function of $\sin^2 \psi$ for 5 or 6 orientations of the sample. If the plot is linear, then the standard formula shown in equation 5 can be used to compute the stress. When the plot oscillates, the more sophisticated Marion-Cohen expression must be used.

SUMMARY

It can be shown that the indiscriminate use of dissection strain gage and X-ray methods to determine the stress state of specimens can introduce large errors in the computed result.

Note that for thin-walled tubes that the Crampton dissection method required a condition of plain strain in addition to a linear variation of the stress from the inside wall surface to the outside wall surface. Since the formula to compute the maximum circumferential stress was length independent, the condition of plain strain could be readily verified by cutting the slit tube, of length ℓ into two tubes of length $\ell/2$ whereby the new diameters could be compared with the diameter obtained from the slit tube of length ℓ .

A comparison of various blind-hole drilling methods in annealed steel indicated that abrasive jet machining causes a negligible error in contrast to other hole drilling methods which introduce errors range from +60 to -70 $\mu\epsilon$. It was pointed out that the standard equations to convert strain readings to computed stresses assume that the strain gage is in reality a point gage (i.e., one of zero length). When the actual gage length is taken into account, the computed stresses can vary significantly. For cases where the strain gage is placed in an equal biaxial stress field, it was shown that small errors in the measurement of ϵ_2 can produce large fluctuations in the computed value for β .

The standard X-ray stress equations were reviewed, and it was noted that a nonlinear plot of interplanar spacing versus $\sin^2 \psi$ identified those samples where the standard X-ray equations could not be used. The oscillating dependence of d on $\sin^2 \psi$ was attributed to the relief of microstrains during extensive plastic deformation and the production of a strong texture. The curve tended to lie above a mean straight line drawn through the data points for diffracting plane orientations where the pole density was higher than the random level and below it where it was lower than the random level. When measurements of the interplanar spacing were taken for six ψ tilts, the non-linear dependence of d could be separated from the linear component by using the Marion-Cohen formula.

REFERENCES

- B. D. Cullity, "Some Problems in X-ray Stress Measurements," <u>Advances in X-ray Analysis</u>, Vol 20, Plenum Press, New York, (1977), pp 259-271.
- R. H. Marion and J. B. Cohen, "Anomalies in Measurement of Residual Stress by X-ray Diffraction," <u>Advances in X-ray Analysis</u> Vol. 18, Plenum Press, New York, (1974), pp. 466-501.
- A. J. Bush and F. J. Kromer, "Simplification of the Hole-Drilling Method of Residual Stress Measurement," <u>ISA Transactions</u>, Vol 12, No. 3, (1973), pp 249-259.
- 4. "Measurements of Residual Stresses by the Blind Hole Drilling Method," <u>Technical Data Bulletin T-403</u>, (1977), Photolastic Inc.

- 5. K. P. Milbradt, "Ring-Method Determination of Residual Stresses," Proc. Soc. Exptl. Stress Anal., Vol. 9, No. 1, (1951), pp 63-74.
- J. P. Sandifer and G. E. Bowie, "Residual Stress by Blind-Hole Method with Off-Center Hole," <u>Exp. Mech., 18</u>, No. 5, (May 1978), pp 173-179.
- G. Kirsch, "Die Theorie der Elastizitat und die Bedurfnisse der Festigkeitslehre," <u>Zeitschrift des Vereines Deutscher Ingenievre</u>, Vol. 42, (July 16, 1898), pp 797-807.
- E. M. Beaney and E. Procter, "A Critical Evaluation of the Centre Hole Technique for the Measurement of Residual Stresses," <u>Strain</u>, (Jan. 1974), pp 7-14, 52.
- 9. M. E. Hilley, J. A. Larson, C. F. Jatczak, and R. E. Ricklefs, eds. "Residual Stress Measurements by X-ray Diffraction," <u>SAE</u> <u>Report J 784a</u> (1971), Available from SAE, Inc., 400 Commonwealth Drive, Warrendale, PA 15096.
- B. D. Cullity, "Residual Stress After Plastic Elongation and Magnetic Losses in Silicon Steel," <u>Trans. Met. Soc.</u>, Vol. 227, (1963), pp 356-358.
- B. D. Cullity, "Sources of Error in X-ray Measurements of Residual Stress," JAP35, (1964), pp 1915-1917.
- 12. S. I. Smith and W. A. Wood, "Internal Stress Created by Plastic Flow in Mild Steel, and Stress-Strain Curves for the Atomic Lattice of Higher Carbon Steels," <u>Proc. Royal Society</u>, Series A 182, (1944) pp 404-415.
- 13. G. B. Greenough, "Residual Lattice Strains in Plattically Deformed Metals," Nature 160, (1947), p 258.
- G. B. Greenough, "Residual Lattice Strains in Plastically Deformed Polycrystalline Metal Aggregates," <u>Proc. Royal Society</u>, Series A, 197, (1949), pp 556-567.
- M. J. Donachie and J. T. Norton, "Lattice Strains and X-ray Stress Measurement," <u>Trans Met. Society</u>, AIME 221, (1961), pp 962-967.
- 16. L. S. Darken, "Some Observations on Atoms and Imperfections," <u>Trans. Quarterly Amer. Society Metals</u>, 54, (1961), pp 559-642, (fig. 30 by Keh and Weissman).

- P. W. Neurath and R. E. Waite, "Elastic and Plastic Strains and Watt Losses in Grain-Oriented Three PCT Si-Fe," <u>Trans. AIME 203</u>, (1955), p 480.
- W. Morrill, "Silicon Irons up to Date," <u>Metals Progress 78</u>, (1960), pp 84-87.
- 19. W. Wiedemann Ph. D., Thesis, "Technische Hochschule," Aachen, Germany, (1966).

Ĺ

DISTRIBUTION LIST

Director Defense Research and Engineering Office ATTN: DDRE (R&AT) Mr. J. Persh Washington, DC 20310

Director Defense Advanced Research Projects Agency ATTN: Dr. E. Van Redth 1400 Wilson Blvd Arlington, VA 22209

Defense Technical Information ATTN: Accessions Divisions (DDC-TC) (12) Cameron Station Alexandria, VA 22314

Assistant Secretary of the Army (R&D) ATTN: Deputy for Science and Technology Washington, DC 20310

Deputy Chief of Staff for Research, Development and Acquisition Department of the Army ATTN: DAMA-ARZ-D Washington, DC 20310

Commander U.S. Army Materiel Development and Readiness Command ATTN: DRCMT, L. Croan DRCDM-D DRCDM-DT DRCDM-R DRCDE-DE DRCDE-E, Mr. E. Gardner DRCDE-I DRCDE-I DRCDE-W Technical Information Division 5001 Eisenhower Avenue Alexandria, VA 22333

Commander U.S. Army Armament Research and Development Command DRDAR-CG, MG A. H. Light Jr. ATTN: DRDAR-TD, Dr. R.E. Weigle DRDAR-TDR, Dr. R. J. Eichelberger DRDAR-SC, Col. M. G. Swindler DRDAR-SC, Dr. D. Gyorog DRDAR-SCM, Mr. J. D. Corrie DRDAR-SCM-P, Dr. E. Bloore DRDAR-SCM-M, Mr. W. Dittrich DRDAR-SCM-P, Dr. J. Waldman Mr. J. Beetle Mr. J. Mulherin Mr. I. G. Betz Mr. F. Witt (10) Mr. F. M. Lee Mr. W. Rider Mr. P. Miller Mr. J. Rinnovatore DRDAR-LC, Col. D. P. Whalen DRDAR-LCU, Mr. B. Bushey Mr. B. Gustad Mr. W. Sharp Mr. D. Robertson DRDAR-LCU-SS, Mr. R. Botticelli Mr. W. Field DRDAR-TS, Col. D. E. Wright DRDAR-TSS (5) DRDAR-QA, Mr. D.G. Adams DRDAR-QAR-I, Mr. M. Weinberg DRDAR-QAR-Q, Mr. R. Blakeslee Mr. F. Cohen DRDAR-SCP DRCPM-CAWS, Col. R. R. Phillipp DRCPM-SA, Col. C. Jones, Jr. DRCPM-ADG, Col. L. Marrella DRCPM-PBM, Col. H. V. Dutchyshyn DRCPM-NUC DRCPM-AAH (30 mm) DRCPM-TMA DRDAR-LCA-G, Mr. L. Rosendorf Mr. G. Bubb Dr. S. Yim Dover, NJ 07801 Commander U. S. Army Research Office ATTN: Director, Metallurgy & Materials Science Division PO Box 12211

Research Triangle Park, NC 27709

Commander U. S. Army Materials and Mechanics Research Center ATTN: DRXMR, Dr. E. Wright DRXMR-PT AMXMR-EQ, Dr. C. Gazzara DRXMR-MQ, Mr. W. Roy Technical Information Division Watertown, MA 02172

Commander Naval Air Development Center, Johnsville Warminster, PA 18974

Aero Materials Department ATTN: Mr. F. Williams, Man Dr. W. S. Scott, Man Warminster, PA 18974

Commander U. S. Naval Engineering Experimental Station ATTN: Materials Lab. WCTRL-2 Annapolis, MD 21401

Director

£

U. S. Army Mobility Equipment Research and Development Command ATTN: DRDME-MMM, E. York Fort Belvoir, VA 22060 Commander U.S. Army Research and Standardization Group (Europe) ATTN: DRXSN-E-RM, Dr. R. Quattrone PO Box 65 FPO, New York 04510 Commander U.S. Naval Surface Weapons Center ATTN: W. Mannschreck, Code G54 Dahlgren, VA 22448 Commander Naval Surface Weapons Center White Oak Laboratory ATTN: A. P. Divecha, M/S R-32 Silver Spring, MD 20910 Commander U.S. Army Aviation Materiel Command ATTN: Technical Information Division P.O. Box 209, Main Office St. Louis, MO 63166

Commander U.S. Army Communications and Electronics Materiel Readiness Command ATTN: Technical Information Division Fort Monmouth, NJ 077C3

Commander Rocky Mountain Arsenal ATTN: Technical Information Division Denver, CO 80240

Commander U.S. Army Research Office ATTN: Metallurgy & Materials Science Divisior, G. Mayer, Director PO Box 12211 Research Triangle Park, NC 27709

22

Commander Redstone Scientific Information Center ATTN: DRDMI-T-B-D, Bldg 4484 Redstone Arsenal, AL 35809

Commander U.S. Army Foreign Science & Technology Center ATTN: Mr. W. F. Marley 220 Seventh Street, N.E. Charlottesville, VA 22901

Director, Ames Directorate U.S. Army Air Mobility R&D Lab ATTN: SAVDL-AM Ames Research Center Moffett Field, CA 94035

Director, Langley Directorate U.S. Army Air Mobility R&D Lab ATTN: SAVDL-LA, M/S 266 B. Lisagor, M/S 188B Hampton, VA 23365

Director, Lewis Directorate U.S. Army Air Mobility R&D Lab ATTN: SAVDL-LE 21000 Brook Park Road Cleveland, OH 44135

Director, ARRADCOM USA Ballistic Research Laboratories ATTN: Dr. A. M. Dietrich, DRDAR-BLT Dr. S. Golaski, DRDAR-BLT Mrs. P. W. Kingman, DRDAR-BLT Mr. J. Simon, DRDAR-BLT Technical Library, DRDAR-TSB-S Technical Library, DRDAR-CLJ-L Aberdeen Proving Ground Aberdeen, MD 21005

Benet Weapons Laboratory Technical Library ATTN: DRDAR-LCB-TL Watervliet, NY 12189

Commander U.S. Army Armament Materiel Readiness Command ATTN: DRSAR-LEP-L Rock Island, IL 61299

Director U.S. Army TRADOC Systems Analysis Activity ATTN: ATAA-SL (Technical Library) White Sands Missile Rarge, NM 88002

U.S. Army Materiel Systems Analysis Activity ATTN: DRXSY-MP Aberdeen Proving Ground, MD 21005

Librarian National Bureau of Standards Washington, DC 20025

National Academy of Science Materials Advisory Board ATTN: Dr. J. R. Lane 2101 Constitution Ave., N.W. Washington, DC 20418

Metals & Ceramics Information Center Battelle Memorial Institute 505 King Avenue Columbus, OH 43201

U.S. Atomic Energy Commission Document Library Germantown, MD 21403 Commander Aeronautical Systems Division ATTN: Technical Library Director, Air Force Materials Laboratory Wright Patterson AFB Dayton, OH 45433

National Aeronautics & Space Administration Federal Bldg #10 ATTN: Code RRM Washington, DC 20456

Drexel University Materials Engineering Dept. ATTN: Prof. A. Lawley Philadelphia, PA 19104

Bethlehem Steel Corp. Homer Research Labs ATTN: Dr. E. Kottecamp Bethlehem, PA 18018

Dr. Paul Flynn 215 Tuscany Drive Baltimore, MD 21210

2

Weapon System Concept Team/CSL ATTN: DRDAR-ACW Aberdeen Proving Ground, MD 21010

Technical Library ATTN: DRDAR-CLJ-L Aberdeen Proving Ground, MD 21010

Director U.S. Army Ballistic Research Laboratory ARRADCOM ATTN: DRDAR-TSB-S (STINFO) Aberdeen Proving Ground, MD 21005 Commander U.S. Army Armament Materiel Readiness Command ATTN: DRSAR-LEP-L Rock Island, IL 61299 *