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Prediction of Adiabatic Shear in 4340 Steel, Remco and Armco Irons

by

Charles R. Mason
B.A.Sc. (Mechanical Engineering)

A thesis submitted to
the Faculty of Graduate Studies and Research
in partial fulfilment of the requirements
for the degree of

Master of Engineering
in Mechanical Engineering

Department of Mechanical and Aerospace Engineering
Ottawa-Carleton Institute
for Mechanical and Aerospace Engineering

Carleton University
Ottawa, Ontario
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**Prediction of Adiabatic Shear
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submitted by

Charles R. Mason, B.A.Sc.

in partial fulfilment of the requirements for
the degree of Master of Engineering

A handwritten signature in black ink, consisting of several loops and a long horizontal stroke, positioned above a solid horizontal line.

Thesis Supervisor

A handwritten signature in black ink, appearing to read 'Robert Bell', positioned above a solid horizontal line.
Chair, Department of Mechanical and Aerospace Engineering

Carleton University

May 1997

I would like to dedicate this thesis to my parents, Bob and Freda, and thank them for their love and support, without which this thesis would not have been possible.

Abstract

Adiabatic shear localization is a catastrophic failure mechanism which can occur in ductile metals under high strain loading. This mechanism is driven by a thermal instability process in which rapid temperature rise due to plastic work couples with thermal softening to cause uniform deformation to collapse into narrow bands of intense shear within which material ductility is exhausted.

To study adiabatic shear localization, a compressive split Hopkinson bar facility was commissioned as part of this research for testing materials at high strain rates in either compression or shear. Punching-shear experiments were performed at room and elevated temperatures in several ferrous materials. Optical and scanning electron microscopy was performed on the deformed shear specimens to determine the extent of shear localization and mode of failure. Experimental evidence showed that the tempered 4340 steels were susceptible to localization through adiabatic shear banding, however, annealed Armco, as-received and shock-hardened Remco irons are not.

Finite element simulations of the experiments were performed utilizing a user material subroutine developed as part of this research. This constitutive routine incorporates three adiabatic shear failure criteria, namely (i) maximum shear stress, (ii) maximum shear stress with a minimum critical strain rate, and (iii) flow localization. These criteria proved to be capable of predicting the onset of an instability, however, the deformation which follows the instability was not predicted well.

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Appendix E - Velocity Calculation from Incident Pulse

Nomenclature

A	Initial yield strength (Johnson-Cook)
A_b	Cross-sectional area of incident and transmitter bar
A_s	Cross-sectional area of specimen
A_{unif}	Cross-sectional area of uniform shear region
A_{def}	Cross-sectional area of defect shear region
A_o	Difference in cross-sectional area between the uniform and defect regions
α	Flow localization parameter
B	Hardening coefficient (Johnson-Cook)
β	Fraction of plastic work converted into heat
C	Strain rate coefficient (Johnson-Cook)
C_o	Speed of sound
C_p	Specific heat
C_1 - C_5	Material parameters for Zerilli-Armstrong constitutive equation
δ	Radial clearance between the punch and die plate
E	Young's modulus
E_o	Excitation voltage of load cell
ΔE_a	Change in Wheatstone bridge voltage
ε	Effective strain
ε_{true}	True strain

ϵ_{eng}	Engineering strain
ϵ_i	Elastic strain of incident pulse
ϵ_r	Elastic strain of reflected pulse
ϵ_t	Elastic strain of transmitted pulse
ϵ_1	Elastic strain at the interface between the incident bar and specimen
ϵ_2	Elastic strain at the interface between the transmitter bar and specimen
ϵ_s	Average strain in the specimen
$\dot{\epsilon}$	Effective strain rate
$\dot{\epsilon}_0$	Reference strain rate (Johnson-Cook)
$\dot{\epsilon}_r$	Incremental radial component of the strain rate tensor
$\dot{\epsilon}_z$	Incremental axial component of the strain rate tensor
$\dot{\epsilon}_{rz}$	Incremental shear component of the strain rate tensor
G	Load cell calibration factor
GF	Gauge factor
γ	Shear strain
γ_c	Critical shear strain
γ_{def}	Shear strain in the defect shear region
γ_{unif}	Shear strain in the uniform shear region
$\bar{\gamma}$	Arithmetic average between γ_{def} and γ_{unif}
$\dot{\gamma}$	Shear strain rate
$\dot{\gamma}_{def}$	Shear strain rate in the defect shear region
$\dot{\gamma}_{unif}$	Shear strain rate in the uniform shear region

$\dot{\gamma}_p$	Principal shear strain rate
$\dot{\gamma}_{avg}$	Running average of the principal shear strain rate
$\dot{\gamma}_{crit}$	Critical value of $\dot{\gamma}_{avg}$ to cause adiabatic shear localization
k	Microstructural stress intensity factor
K_{unif}	Strength coefficient of uniform shear region
K_{def}	Strength coefficient of defect shear region
K	Difference in strength coefficient between the uniform and defect shear regions
l	Average grain diameter
L	Length of striker bar
L_s	Length of specimen
m	Strain rate hardening exponent
m_s	Mass of the striker
μ_T	Shear modulus at temperature T
μ_{293}	Shear modulus at 293 K
n	Strain hardening exponent
P	Load acting on the load cell during calibration
P_i	Force acting on incident bar during impact
P_o	Maximum load of load cell
P_1	Force at the interface between the incident bar and specimen
P_2	Force at the interface between the transmitter bar and specimen
q_m	Thermal softening coefficient (Johnson-Cook)

R_i	Initial resistance of a single strain gauge
R_f	Fixed resistance of Wheatstone bridge
R_{g1}	Resistance of gauge 1 in Wheatstone bridge
ΔR	Change in strain gauge resistance for a given strain
ρ	Density
σ	Effective stress
σ_{true}	True stress
σ_{eng}	Engineering stress
σ_i	Stress of the incident pulse
σ_T	Twinning stress
$\Delta\sigma_G$	Influence of solute and original dislocation density on flow stress
$\Delta(\Delta\sigma)$	Increase in flow stress due to the formation of twins
t	Time
T	Temperature
T^*	Homologous temperature
T_{melt}	Melting temperature
T_{ref}	Reference temperature
τ	Shear stress
U_1	Displacement at the end of the incident bar
U_2	Displacement of the end of the transmitter bar
V_c	Calculated velocity
V_e	Excitation voltage

V_s Velocity of striker bar

ΔV Closure velocity between the ends of the incident and transmitter bars

ΔV_c Change in voltage of the load cell

w_p Plastic work

Chapter 1

Introduction

In metals remaining ductile under high strain rate conditions, failure occurs through either ductile fracture or adiabatic shear localization. The former mechanism occurs through the nucleation, growth, and coalescence of microvoids to form a crack [1-4]. Adiabatic shear localization [5,6,7] is a thermal instability process in which rapid temperature rise due to plastic work couples with thermal softening to cause uniform deformation to collapse into narrow bands of intense shear within which material ductility is exhausted. The present research is concerned with the study of adiabatic shear localization with a long term goal being the development and implementation of constitutive models incorporating adiabatic shear localization within finite element codes.

The balance of this chapter provides a review of the three main areas of concern for this thesis. An overview of adiabatic shear localization will be given which will describe the phenomenon, controlling factors, and different type of shear bands that form. Next, two types of failure criteria for predicting adiabatic shear localization will be described along with constitutive equations commonly used in finite element codes used

in high strain rate applications. Finally, a review of the various testing methods employed in the study of adiabatic shear banding will be given.

1.1 Adiabatic Shear Bands

Shear bands are narrow bands of intense plastic shear strain that may form during dynamic loading. The term adiabatic shear and the mechanism through which they form is generally attributed to Zener and Hollomon [8]. During plastic deformation, heat is generated which will cause thermal softening. If the thermal softening reaches a point where it overcomes the rate of strain hardening, homogeneous deformation will give way to deformation that localizes into narrow bands. Such bands are termed adiabatic shear bands. These bands are not truly adiabatic, but since under high strain rate conditions, much more heat is generated than lost, the process is referred to as adiabatic. For adiabatic shear banding to occur, sufficient localized plastic work must accumulate in a short enough period of time that the heat generated does not have time to conduct away. Adiabatic shear bands are easily detected in steels since they will appear as white etched bands of martensite which form due to the rapid heating and cooling (quenching) in the shear band.

The study of adiabatic shear bands is important because these bands can cause premature failure of components subjected to impulsive high rate loading. In fact Bai and Johnson [9] showed that it takes less impact energy to fail a plate in plugging than through bulging. Conventional material data, such as strength, hardness, fracture

toughness, and fracture strain, for example, give very little indication as to whether a material is susceptible to adiabatic shear failure. Therefore it is important to understand the variables controlling the formation of adiabatic shear bands. Adiabatic shear banding is seen in many processes such as armour penetration, dynamic blanking and cropping, high speed machining and forming, failure of aeroengine blades during foreign object impact, and in the formation of explosively formed projectiles. A greater understanding of the adiabatic shear banding phenomenon will aid in the optimization of aircraft engines to withstand impact and protective structures for peacekeeping.

Adiabatic shear bands have been traditionally divided into two distinct groups known as deformed and transformed shear bands. Deformed shear bands are characterized by very narrow zones of intense shear without any evidence of a crystallographic phase change. In transformed shear bands the temperature rise is great enough to cause a phase change in the material. It should be noted that deformed and transformed bands may form in the same material, but with different initial conditions and degrees of deformation.

1.1.1 Deformed Shear Bands

After strain localization very steep strain, strain rate, and temperature gradients are formed, however no phase transformations occur in the materials displaying deformed shear bands. Such shear bands are a characteristic feature of aluminum and aluminum alloys that have been loaded dynamically and have also been observed in iron, steels, titanium alloys, and uranium alloys. Wingrove [10] demonstrated that if an aluminum

alloy is impacted with a blunt projectile, the sharp edges of the projectile will act as initiation sites for shear bands. Wingrove and Wulf [11] noted that the thickness of the shear band decreased with increasing initial hardness of the material. Rogers and Shastry [12] demonstrated that hardness traverses across the shear band show an increasing hardness towards the centre of the band up to a level similar to that of a highly work-hardened material.

1.1.2 Transformed Shear Bands

Hollomon and Zener [8] noted that a shear strain of 5 would produce a temperature rise of 1000°C in steel which would cause a phase transformation from body centred cubic (BCC) ferrite to face centred cubic (FCC) austenite in steels. If a steel is rapidly quenched by the surrounding material from austenite it will form martensite. Martensite will appear as white etched bands in steel.

Rogers and Shastry [12] performed microhardness transverses across shear bands in several steels and found a linear increase in hardness of the shear band with carbon content as seen in Figure 1.1. Figure 1.2 shows the results from a microhardness transverse of a 1040 quenched and tempered steel. The figure shows a peak-valley-hill distribution where the regions adjacent to the transformed band have undergone a slight softening due to the high temperature reached in the transformation band and are termed heat affected zones (HAZ). Following this there is a further increase in hardness within surrounding work hardened zones considered to be unaffected by the increase in temperature in the transformation band.

In a study on chromium armour steel, Derep [13] discovered that the centre of the shear band consisted of equiaxed grains of δ -ferrite and laths of martensite with martensite on either side of this centre region. The presence of this high temperature form of ferrite indicates that a very high temperature, near melting, was reached in this alloy. Giovanola [14,15] studied adiabatic shear banding in steel and observed the appearance of ductile voids in the shear band, which indicate that the final failure occurred within the shear band but by ductile fracture. Timothy [5] noted that a transformed shear band often consists of two zones, a central zone with a significant amount of microstructural modification and an outer zone with a decrease in shear deformation as the undeformed or less deformed material is approached.

1.2 Criteria for the Prediction of Adiabatic Shear Localization

The onset of adiabatic shear bands is often predicted using one of two separate models, based on (i) maximum shear stress or (ii) flow localization. In the maximum shear stress model, the peak in the shear stress versus shear strain curve is used to predict failure via adiabatic shear banding. The flow localization approach treats localization as a process which begins to develop at a geometric or strength defect at the beginning of deformation.

1.2.1 Maximum Shear Stress Criterion

The onset of adiabatic shear banding is said to occur when thermal softening overcomes work hardening. This will occur when the slope of the stress versus strain curve becomes negative.

The flow stress can be written as a function of strain (ϵ), strain rate ($\dot{\epsilon}$), and temperature (T) as,

$$\sigma = \sigma(\epsilon, \dot{\epsilon}, T) \quad (1.1)$$

In incremental form,

$$d\sigma = \frac{\partial \sigma}{\partial \epsilon} d\epsilon + \frac{\partial \sigma}{\partial \dot{\epsilon}} d\dot{\epsilon} + \frac{\partial \sigma}{\partial T} dT \quad (1.2)$$

If it is assumed that the strain rate remains constant during deformation ($d\dot{\epsilon}=0$), then equation (1.2) becomes,

$$d\sigma|_{d\dot{\epsilon}=0} = \frac{\partial \sigma}{\partial \epsilon} d\epsilon + \frac{\partial \sigma}{\partial T} dT \quad (1.3)$$

If it is assumed that the temperature evolution is due to plastic work, W_p , and that β is the fraction of plastic work converted to thermal energy, then the temperature rise is,

$$dT = dW_p \frac{1}{\rho C_p} = \sigma d\varepsilon \frac{\beta}{\rho C_p} \quad (1.4)$$

where ρ is the material density and C_p is the specific heat. Substituting equation 1.4 into 1.3 yields,

$$\left. \frac{d\sigma}{d\varepsilon} \right|_{dT=0} = \frac{\partial \sigma}{\partial \varepsilon} + \sigma \frac{\partial \sigma}{\partial T} \frac{\beta}{\rho C_p} \quad (1.5)$$

The first term on the right hand side is the rate of work hardening and the second term is the rate of thermal softening. When $d\sigma/d\varepsilon|_{dT=0} > 0$ the deformation is said to be stable; however, if $d\sigma/d\varepsilon|_{dT=0} < 0$ adiabatic shear is able to occur.

1.2.2 Flow Localization Criterion

While the attainment of a maximum shear stress and thermal softening is a necessary criterion for adiabatic shear localization it is not a sufficient criterion in itself and therefore a new approach is required. The flow localization model that will be utilized for the current research is the model proposed by Semiatin et al. [16] for the case

of simple shear. In this model, the specimen is assumed to consist of two different regions (i) a uniform or bulk region and (ii) a defect region. The shear load that is supported by these two regions is assumed to be equal. The results of this analysis will be similar for equivalent sizes of an initial geometric defect (or cross-sectional area change) and a strength defect. A variation in cross-sectional area can be expressed as,

$$d\ln A_{,,} = \ln A_{unif} - \ln A_{def} = \ln \left[\frac{A_{unif}}{A_{def}} \right] \quad (1.6)$$

where A_{unif} and A_{def} are the cross sectional areas of the uniform and defect regions, respectively. Here, the operator d refers to the difference between quantities inside and outside of the defect. A strength defect can be expressed as,

$$d\ln K = \ln K_{unif} - \ln K_{def} = \ln \left[\frac{K_{unif}}{K_{def}} \right] \quad (1.7)$$

Here, the review will focus on a strength defect only.

Equilibrium requires that,

$$d(A\tau) = A d\tau + \tau dA = 0 \quad (1.8)$$

but for simple shear $dA=0$ therefore (1.8) becomes,

$$\frac{d\tau}{\tau} = 0 = d\ln \tau \quad (1.9)$$

The shear stress can be written as a function of shear strain (γ), shear strain rate ($\dot{\gamma}$), temperature (T), and K as,

$$\tau = \gamma^n \dot{\gamma}^m h(T) K \quad (1.10)$$

where K, n, and m are material constants and h(T) is the function that describes the temperature dependence of the flow stress.

In incremental form and using equation (1.10), the right hand side of equation (1.9) becomes,

$$d \ln \tau = n d \ln \gamma + m d \ln \dot{\gamma} + \left(\frac{\partial \ln \tau}{\partial T} \right) dT - d \ln K \quad (1.11)$$

The temperature rise, dT, can be calculated from the fraction of the heat that is generated by plastic work by,

$$d\tau = \tau d\gamma \frac{\beta}{\rho C_p} \quad (1.12)$$

where ρ is the material density, C_p is the specific heat, and β is the fraction of plastic work converted into heat.

Using equation 1.12 the temperature term in equation 1.11 can be expressed as,

$$\left(\frac{\partial \ln \tau}{\partial T} \right) \delta T = \left(\frac{\partial \tau}{\partial T} \right) \frac{\beta \delta \gamma}{\rho C_p} \quad (1.13)$$

The critical shear strain value, γ_c , which is defined as the value of shear strain where the shear stress is a maximum, can be shown to be

$$\gamma_c = -\frac{\rho C_p n}{\beta \left(\frac{\partial \tau}{\partial T} \right)} \quad (1.14)$$

If the term $(\partial \tau / \partial T)$ is assumed to be constant and equation (1.14) is used, equation (1.13) reduces to,

$$\left(\frac{\partial \ln \tau}{\partial T} \right) dT = -\frac{n}{\gamma_c} d\gamma \quad (1.15)$$

Combining equations (1.9), (1.11), and (1.15) yields,

$$d \ln \tau = \left[\frac{n}{\gamma} - \frac{n}{\gamma_c} \right] d\gamma + m d \ln \dot{\gamma} - d \ln K = 0 \quad (1.16)$$

This can be rearranged to obtain a definition of $\delta \ln \dot{\gamma}$ as,

$$d \ln \dot{\gamma} = \left[\left(\frac{n}{\gamma_c} - \frac{n}{\gamma} \right) d\gamma - d \ln K \right] \frac{1}{m} \quad (1.17)$$

or

$$\dot{\gamma}_{def} = \dot{\gamma}_{unif} \left(\frac{K_{unif}}{K_{def}} \right)^{\frac{1}{m}} \exp \left[\left(\frac{n}{\gamma_c} - \frac{n}{\gamma} \right) \left(\frac{\gamma_{def} - \gamma_{unif}}{m} \right) \right] \quad (1.18)$$

Where $\bar{\gamma}$ is an average of γ_{def} and γ_{unif} which ensures equilibrium, a simple arithmetic mean usually provides sufficiently accurate results.

In typical calculations n , m , γ_c , and $f_o=(K_{\text{def}}/K_{\text{unif}})$ are known and equation (1.18) can be applied incrementally for a given constant $\dot{\gamma}_{\text{unif}}$, to obtain $\dot{\gamma}_{\text{def}}$ over time.

Equations (1.17) and (1.18) show that as long as (n/γ) exceeds (n/γ_c) , the growth rate of the strain rate gradient is slow, however, once γ exceeds γ_c , rapid localization will occur.

When $\gamma > \gamma_c$ the rate of localization can be estimated by equation (1.17) and neglecting $\ln K$, since it will be small compared to $\delta\gamma$, equation (1.17) becomes.

$$\frac{d\ln\dot{\gamma}}{d\gamma} = \left(\frac{n}{\gamma_c} - \frac{n}{\gamma} \right) \frac{1}{m} \quad (1.19)$$

The term $d\ln\dot{\gamma}/d\gamma$ is defined as the flow localization parameter α . Using the effective strain $\epsilon=\gamma/\sqrt{3}$ and effective strain rate $\dot{\epsilon}=\dot{\gamma}/\sqrt{3}$, equation (1.19) becomes,

$$\alpha = \frac{\delta\ln\dot{\epsilon}}{\delta\epsilon} = \frac{\sqrt{3}}{m} \left(\frac{n}{\gamma_c} - \frac{n}{\gamma} \right) \quad (1.20)$$

Using the differential forms of equations (1.11) and (1.15) it can be shown that,

$$\left(\frac{n}{\gamma_c} - \frac{n}{\gamma} \right) = -\frac{1}{\tau} \left(\frac{d\tau}{d\gamma} \right) \Big|_{\dot{\gamma}} \quad (1.21)$$

at constant strain rate. This implies that α or the rate of flow localization is proportional

to the flow softening rate ($d\tau/d\gamma$) and inversely proportional to the strain rate sensitivity (m).

Rearranging equation (1.20) so that shear strain can be expressed for different levels of α yields,

$$\gamma(\alpha) = \gamma_c \left(1 - \frac{\alpha m \gamma_c}{\sqrt{3} n} \right)^{-1} \quad (1.22)$$

To verify the usefulness of the flow localization model Semiatin et al. [16] used the experimental results from Staker's [6] explosively expanded cylinder test on quenched and tempered 4340 steel. Equation (1.18) was used to study flow localization under conditions of adiabatic simple shear. The material constants needed for equation (1.18) are n , m , f_0 , and γ_c . The values of n are given in Staker [6] for various hardness levels. A value of $m=0.04$ was given by Nicholas [17] for 4340 steel at high rates of deformation. A defect strength of 1.5% which results in a value for f_0 of 0.985 was assumed. The values of γ_c for different hardness levels were determined by direct measurement of the flow stress maxima on dynamic stress-strain curves by Wulf [18].

It was assumed that the values for m and f_0 values are independent of the hardness level and therefore only n and γ_c need to be varied in the simulation. The results of these simulations are presented in Figure 1.3 in terms of γ_{def} versus γ_{unif} for four different hardness levels. The figure shows that localization begins slowly but eventually accelerates rapidly until catastrophic localization occurs. The results of Staker's exploded cylinder tests are also plotted on Figure 1.3. It can be seen that the shear banding only occurs where the predicted defect strain localization is large compared to the uniform strain as reflected by the steeper slopes in the figure.

The relation between the localization parameter, α , and the occurrence of adiabatic shear banding can be seen in Figure 1.4, which plots all of Staker's experimental data. The broken lines in this figure are the bulk strains resulting from different values of α as calculated from equation (1.22). The figure shows that a value of α between 3 and 4 is needed to cause catastrophic shear failure. The value is slightly below that of 5 which is associated with noticeable localization in torsion, tension, and compression [19-21]. Semiatin et al. proposed that this change is a result of the path change from pure to simple shear that takes place in the explosively expanded cylinder test during localization.

Ning [22] used a similar criterion to model adiabatic shear in tantalum. In order to use the above criterion, an expression for α must be determined for the specific constitutive equation that is being used. If a constitutive relation of the form $\sigma = \sigma(\epsilon, \dot{\epsilon}, T)$ is adopted, the stress variation due to a disturbance will be,

$$d\sigma = \frac{\partial \sigma}{\partial \epsilon} d\epsilon + \frac{\partial \sigma}{\partial \dot{\epsilon}} d\dot{\epsilon} + \frac{\partial \sigma}{\partial T} dT \quad (1.23)$$

Substituting,

$$\frac{d \ln \dot{\epsilon}}{d \dot{\epsilon}} = \frac{1}{\dot{\epsilon}} \quad \text{or} \quad d\dot{\epsilon} = \dot{\epsilon} d \ln \dot{\epsilon} \quad (1.24)$$

and rearranging equation (1.23) gives,

$$\frac{d \ln \dot{\epsilon}}{d \epsilon} = \left(-\frac{\partial \sigma / \partial \epsilon}{\partial \sigma / \partial \dot{\epsilon}} - \frac{\partial \sigma / \partial T}{\partial \sigma / \partial \dot{\epsilon}} \frac{dT}{d\epsilon} \right) \frac{1}{\dot{\epsilon}} \quad (1.25)$$

This criteria can be used to predict adiabatic shear failure in computer simulations.

1.2.3 High Strain Rate Constitutive Models

Two of the most widely used semi-empirical constitutive models are the Johnson-Cook and Zerilli-Armstrong constitutive relations. These semi-empirical models are attractive because they only require a few material parameters [17] yet incorporate terms for strain hardening, strain rate hardening, and thermal softening. These are called semi-empirical since they are partially derived from curve fitting and are not solely based on metal physics theories. For these reasons they were adopted for the current research and are described below.

The Johnson-Cook Constitutive Equation

The constitutive model proposed by Johnson and Cook [23] is as follows.

$$\sigma = (A + B \varepsilon^n)(1 + C \ln \dot{\varepsilon}^*)(1 - T^{*m}) \quad (1.26)$$

where ε is the equivalent plastic strain, $\dot{\varepsilon}^* = \dot{\varepsilon} / \dot{\varepsilon}_0$ is a dimensionless plastic strain rate for which $\dot{\varepsilon}_0 = 1.0 \text{ s}^{-1}$ and $T^* = (T - T_{\text{ref}}) / (T_{\text{melt}} - T_{\text{ref}})$ is a form of homologous temperature where T_{ref} is the reference temperature usually taken as room temperature and T_{melt} is the melting temperature. The terms A and B are material constants with the same units as stress and C , n , and m are dimensionless constants.

The first term in brackets of equation (1.26) expresses stress as a function of strain in a power law formulation for $\dot{\varepsilon} = 1.0 \text{ s}^{-1}$ and at room temperature or when $T^* = 0$. The second term in brackets incorporates a logarithmic dependency of flow stress on strain rate. The final term is the effect of temperature and thermal softening on flow stress.

Johnson and Cook [24] compared computer simulations with experimental data obtained from cylinder impact (Taylor) tests. The Taylor tests were simulated using the

Epic-2 computer code. The results showed good agreement for the Armco iron and 4340 steel.

Inserting the Johnson-Cook constitutive relation, equation (1.26), into equation (1.25) and assuming the adiabatic condition gives,

$$\frac{d \ln \dot{\epsilon}}{d \epsilon} = -\frac{(nB \epsilon^{n-1})(1 + C \ln \dot{\epsilon})}{C(A + B \epsilon^n)} + \frac{\sigma(1 + C \ln \dot{\epsilon})(q_m T^{*m-1})}{\rho C_p C(T_{melt} - T_{room})(1 - T^{*m})} \quad (1.27)$$

The Zerilli-Armstrong Constitutive Equation

Zerilli and Armstrong [25] developed a dislocation mechanics-based constitutive model using the same tensile data considered by Johnson and Cook [24] to develop their model. Most models, including the Johnson-Cook model, are numerical fits to data and therefore are not considered reliable outside the range of the test data. Furthermore these models do not take into account the effect of grain size or differences in crystal structure. The Zerilli-Armstrong model attempts to account for these difference.

Different crystal structures will have different dislocation characteristics and therefore each crystal structure will have a similar but unique constitutive equation. For a face centred cubic (FCC) structure the constitutive equation is written as,

$$\sigma = \Delta \sigma_G - C_2 \epsilon^{-1/2} \exp(-C_3 T + C_4 T \ln \dot{\epsilon}) + k l^{-1/2} \quad (1.28)$$

and for a body centred cubic (BCC) structure,

$$\sigma = \Delta \sigma_G - C_1 \exp(-C_3 T - C_4 T \ln \dot{\epsilon}) - C_5 \epsilon^n + k l^{-1/2} \quad (1.29)$$

where ϵ is the equivalent plastic strain, $\dot{\epsilon}$ is the equivalent plastic strain rate, T is the absolute temperature, and $\Delta\sigma'_G$, C_1 , C_2 , C_3 , C_4 , C_5 , n , and k are material constants and l is the grain size.

The most obvious difference between equations (1.28) and (1.29) is the uncoupling of the strain hardening factor from the thermal softening and strain rate effects. The reason for this difference is due to the stronger dependence of the yield stress on temperature and strain rate in BCC materials as compared to FCC materials.

The term $\Delta\sigma'_G$ in both of these equations is the influence of solute and original dislocation density on the flow stress. The term $kl^{-1/2}$ is the increase in the flow stress at relatively low temperatures for the transmission of stress between polycrystal grains due to the slip band-stress concentrations at the grain boundaries [26], commonly known as the Hall-Petch relationship. Here, k is the microstructural stress intensity factor.

Equations (1.28) and (1.29) were used to simulate the Taylor tests with the EPIC-2 computer code. The results were then compared with the experimental data and the simulations done by Johnson and Cook. The comparison showed a significant improvement over the Johnson and Cook simulations for the dislocation-mechanics-based constitutive relation for the FCC model, as seen in Figure 1.5. However, the results for the BCC model did not show a significant improvement over the Johnson-Cook model, as seen in Figure 1.6.

The failure of the BCC model to accurately predict the results of the Taylor cylinder experiments was investigated by Zerilli and Armstrong in a later paper [27]. They proposed that deformation twinning was occurring in the iron under impact loading and this was the source of the discrepancy between the simulated and experimental results. To correct for twinning, Zerilli and Armstrong suggested that there would be an increase in flow stress, $\Delta(\Delta\sigma)$, due to the formation of twins. When the flow stress from equation (1.29) becomes greater than the stress necessary to form twins, σ_T , $\Delta(\Delta\sigma)$ is

added to the flow stress for the remainder of the simulation. Zerilli and Armstrong compared the results of the simulation using this twinning model with experimental data and found a significant improvement. They found that $\Delta(\Delta\sigma)=65$ MPa and $\sigma_T=600$ MPa gave the best results, as seen in Figure 1.7.

For the Zerilli-Armstrong equation (1.29) using the BCC material description, equation (1.25) becomes,

$$\frac{d\ln\dot{\epsilon}}{d\epsilon} = -\frac{n C_5 \epsilon^{n-1}}{C_1 C_4 T \exp(-C_3 T + C_4 T \ln\dot{\epsilon})} + \frac{\sigma(C_3 - C_4 \ln\dot{\epsilon})}{\rho C_p C_4 T} \quad (1.30)$$

Goldthorpe [28] in a study on annealed and shock hardened Remco iron developed a constitutive equation based on the Zerilli-Armstrong equation (1.29), with a slight modification for temperature dependence. Zerilli and Armstrong [25] had developed their model on the basis that strain hardening is independent of both strain rate and temperature. Goldthorpe confirmed this trend for strains below 1.0, but reported a slight temperature dependence of strain hardening that is associated with the shear modulus. Although shock hardening has a large effect on the material properties, only a simple modification to the model is required. Goldthorpe discovered that shock hardening raises the initial flow stress and reduces the work hardening rate. Goldthorpe treated the effect of shock hardening as an additional rate independent constant that does not change the strain rate or temperature dependence. Goldthorpe's modified form of the Zerilli-Armstrong constitutive equation becomes,

$$\sigma = C_1 + C_2 \exp(-C_3 T + C_4 T \ln\dot{\epsilon}) - (C_5 \epsilon^n + C_6) \frac{\mu_T}{\mu_{293}} \quad (1.31)$$

where,

$$\frac{\mu_T}{\mu_{293}} = 1.13 - 0.000445 T \quad (1.32)$$

where μ_T is the shear modulus at temperature T (Kelvin).

1.3 Testing Methods

Adiabatic shear bands are difficult to study as a number of different phenomena occur simultaneously and become impossible to differentiate. For example, in explosively formed projectiles (EFP's) adiabatic shear banding occurs along with spallation and ductile fracture and making it difficult to isolate the effects of adiabatic shear banding. For this reason, it is necessary to develop specific testing methods to study adiabatic shear banding directly. Before a testing method can be chosen, an understanding of the specific requirements must be gained.

The strain rate generated must be above the level at which adiabatic shear bands form and the stress state in the gauge length should be well defined and relatively simple, like simple shear or uniaxial compression [29]. The increase in temperature is primarily plastic shear deformation and therefore a large and homogeneous plastic shear deformation within the gauge length is needed.

1.3.1 Torsion

When testing materials in shear, torsional testing machines have several advantages over other testing methods. Torsion will produce a simple shear deformation with no macroscopic volume change but rigid body rotation does occur and this must be taken into consideration when analyzing the data. Microscopic dilation due to microvoid

nucleation resulting from local non-shear stress components at inhomogeneities is not precluded by the constant volume requirement. In a thin-walled tubular specimen, an approximate homogeneous state of shear deformation along a gauge length can be obtained. The flanges that attach the thin-walled specimen to the testing apparatus need to be designed and machined carefully to ensure that a homogeneous state of shear in the gauge length occurs. An absence of any change in cross-section means that large strains can occur. Geometric instabilities such as necking in tension and barrelling in compression are avoided due to the simple shear deformation.

Hydraulic Torsional Machines

A schematic of a typical hydraulic torsional machine can be found in Figure 1.8. The apparatus must have a high torsional stiffness and the moving parts need to have low inertia. A maximum torsional stiffness is achieved by enclosing the loading system within a simple cylindrical housing. Loading is achieved by applying a hydraulic pressure between the rotary and stationary vanes, producing a torque on the specimen. A fast opening solenoid valve is placed between the accumulator and actuator to provide high strain rates. Using this type of testing facility, strain rates up to 300 s^{-1} have been achieved [30]. Since the actuator, specimen, and load transducer are initially attached, no impact loading occurs, however the specimen does experience a rapid acceleration which results in 'ringing' in the load transducer.

Torsional Impact

A schematic of a typical torsional impact machine is illustrated in Figure 1.9. This device uses a commercial lathe to perform testing. The specimen is initially uncoupled from the drive unit as seen in the figure. The specimen is brought up to the

desired rotational speed and the trigger rod is released. This causes the drive unit to become engaged with the specimen thereby loading it in torsion.

In this type of device it is important to maintain alignment and coaxiality of the specimen and drive shaft. The load train must have low inertia and high stiffness and there must be enough energy in the lathe chuck to maintain a constant strain rate during deformation. The load history is recorded by the strain gauges mounted on the load train. Strain rate up to 10^3 s^{-1} have been reached using this type of testing method [31].

Torsional Split Hopkinson Bar

The torsional split Hopkinson bar or Kolsky bar is illustrated schematically in Figure 1.10. This type of testing apparatus was first developed by Baker and Yew [32] in 1966. It has a similar set up to the Kolsky axial bar which will be discussed in detail in a later section. The bar is clamped and a torque is applied to the end of the incident bar using a hydraulic pulley system. When the clamp is released, an elastic torsional wave travels the length of the bar to the specimen. The specimen is of lower strength and cross-sectional area than the bar and is deformed plastically. As the specimen deforms, some of the torsional wave will be transmitted into the transmitter bar by the specimen and the rest of the wave will be reflected back into the incident bar. Using elementary wave propagation theory, the stress-strain histories of the specimen can be obtained by the analysis of these waves which are measured by strain gauges mounted directly to the bars as indicated.

The proper design of a quick release clamp is crucial to obtaining good results. The clamp should be designed such that it will hold the required torque without scoring the bars as well as being able to release the bar quickly enough to produce a nearly square loading wave. Two such quick release clamps can be seen schematically in

Figures 1.11 (a) and (b). The clamp design in Figure 1.11 (a) clamps the bar between two hinged arms which have half moon shaped holes cut into them to accommodate the bar. As the notched bolt is tightened the arms press against the bar and hold it in place while the torque is being applied. Once the required torque is achieved, the notched bolt is tightened until it fractures releasing the torque. Figure 1.11 (b) is a similar design but the bar is held between two flat arms and a hydraulic piston is used to fracture the notched pin.

Test specimens used in the torsional bar are usually thin-walled tubes. Two such specimen designs are illustrated schematically in Figure 1.12 (a) and (b). A cylindrical flange design shown in Figure 1.12 (a) is cemented directly to the bar and can be used for testing materials with low flow stress. When testing materials with high flow stress, adhesives are not strong enough to hold the specimen rigidly in place and an alternate design as shown in Figure 1.12 (b) is used for these higher strength materials. The flanges are hexagonal shaped and matching sockets are machined into the ends of the Kolsky bars. A set screw is used to ensure that no rotation occurs between the bar and specimen.

To produce a loading pulse with a shorter rise time, explosive charges can be used to initiate the torsional pulse as illustrated in Figure 1.13. To ensure only a pure torsional pulse is generated, the explosions must be of equal magnitude and detonation must occur simultaneously. The pulse generated by this means has a short rise time but contains axial and bending components and is neither smooth nor square. Mechanical filters can be used to reduce these components and are illustrated in Figure 1.14. The filter on top is used to reduce the axial and bending stresses since these components will not propagate through the thin section. The torsional pulse resulting from the explosion will have a varying amplitude which will result in an undesirable varying strain rate in the specimen which is undesirable. Elastic torsional waves will propagate along the length of the bar

without any geometrical dispersions and therefore a second filter must be used to smooth the pulse. This filter, shown as the bottom filter in Figure 1.14, will deform plastically and thereby reduce the high frequency components and thereby smooth the pulse.

The torsional split Hopkinson bar technique has a few advantages over other torsion tests. The specimen flanges are connected directly to the bars and therefore there will be no friction on the interface between the specimen and bar. The strain rate and average strain can be controlled accurately which is not always possible with other tests. Unfortunately, these test are also more costly since the specimens require larger amounts of material and their machining is more complicated. The torsional Hopkinson bar is more expensive to construct than its axial counterpart which also leads to higher costs. Furthermore, typical rise times of between 20 and 40 μs will tend to limit strain rates and mechanical connections between the specimens and bars can cause undesirable wave reflections.

1.3.2 Contained Cylinder Tests

The contained cylinder test is not a conventional material test because it does not provide a well defined stress state or uniform deformation within the specimen gauge length [29]. However ultra-high strain rates, 10^4 - 10^6 s^{-1} , and large plastic strains are attained, both of which are necessary for the formation of adiabatic shear bands. Figure 1.15 shows a schematic of the experimental set up which consists of an explosively packed cylindrical specimen surrounded by plexiglass, steel, and lead shells. Upon detonation of the explosive, a compressive wave causes the specimen cylinder to expand rapidly. The plexiglass and steel shells act as buffers to reduce the reverberating stress waves.

This type of deformation does not produce a homogeneous stress state throughout the specimen cylinder. The inner surface of the cylinder will experience a large compression which produces a large plastic strain. The expanding cylinder will experience a hoop tension as well. These two circumstances will produce shear bands at 45° to the circumferential direction. This type of test has been used to study the relation between adiabatic shear instability strain and material properties [33] as well as the kinetics of this type of microdamage [34].

1.3.3 Compression Testing

Several advantages exist when using compression testing to study adiabatic shear bands. The compression test is straightforward and the results can be linked directly to specific applications and therefore are more readily accepted by engineers in industry. These tests will impose compression under plane strain conditions which will promote a pure shear deformation without rotation [29], making metallurgical examination of morphological changes during deformation easier. Compression testing does have a few disadvantages as well. The effect of friction must be taken into account and barrelling must be kept to a minimum to provide a homogeneous deformation. Effects of friction can be reduced with proper lubrication of the interfaces between the bar and specimen.

Several different testing techniques are used to test materials in dynamic compression and will be discussed in the following sections.

Cam Plastometer

The first of the cam plastometer machines was developed by Orowan in the 1950's [35] and was used to test materials at medium rates of strain. A photograph of a typical cam plastometer drive train can be found in Figure 1.16. The cam plastometer uses a

flywheel to transfer the axial load to the specimen via a logarithmic cam and therefore will provide a constant true strain rate if the flywheel speed is kept constant during deformation [36]. To keep the flywheel speed constant, large amounts of kinetic energy must be stored in the flywheel and the rest of the transmission. The amount of stored kinetic energy must greatly exceed the energy required to deform the specimen. In a logarithmic cam, the radius of the rising (or lowering) part of the cam increases (or decreases) to satisfy the following equation,

$$L_f = L_o \exp(\dot{\epsilon} t) \quad (1.33)$$

where L_f and L_o are the final and original heights of the specimen, t is the time, and $\dot{\epsilon}$ is the strain rate.

Drop Tower

A typical drop tower compression machine is shown in Figure 1.17 and the accompanying dynamic compressive fixture in Figure 1.18. This test uses a falling weight to provide a compressive load to a specimen at a medium rate of strain and high load [37]. The dynamic compressive fixture transfers the load from the falling crosshead to the specimen through a shaft. As with all dynamic testing, alignment is critical and is maintained through the use of linear ball bearings in the fixture. The amount of displacement is controlled by the use of stop blocks which arrest the falling crosshead at the desired amount of displacement. The amount of strain in the specimen is measured using linear voltage differential transducers (LVDT's) mounted within the dynamic compressive fixture. A load cell is located at the base of the dynamic compressive fixture to record the load history of the specimen. A disadvantage of this type of testing

apparatus is that the test is neither a constant strain rate nor constant stress rate test.

The Compressive Split Hopkinson Bar (CSHB)

In 1914, Bertram Hopkinson used a long elastic bar to study pressures produced by projectile impact or detonation of an explosive [38]. Hopkinson found that as long as the bar remained elastic, the displacements in the bar could be directly related to the stresses and the length of the stress pulse could be related to the duration of the impact. Kolsky [39] further modified this technique and used two pressure bars to compress a specimen. This technique has become known as the split Hopkinson pressure bar or Kolsky bar technique. Kolsky went on to show that the stress and strain within the specimen could be related to the displacements of the bars. Since Kolsky's original experiments, many modifications have occurred to the Hopkinson bar. In 1961, strain gauges were first used to measure the strains and hence stresses in the bars [40]. The Hopkinson bar technique has also been used in torsional [32] and tensile [41,42] deformation modes.

A standard configuration for compressive split Hopkinson bars and the arrangement used in this research is shown in Figure 1.19. The configuration seen in Figure 1.19 (a) is the most widely used and will be discussed here. The striker bar is propelled towards the incident bar with a light gas gun or spring. When the striker impacts the incident bar, a compressive wave begins to propagate along the incident bar towards the specimen. Assuming that the bars are of equal cross-sectional area and made of the same material, a compressive wave of equal magnitude will propagate in the striker bar in the opposite direction. Figure 1.20 is a schematic representation of the impact and the stress waves generated in the incident and striker bars. The stress will propagate in the bars at the speed of sound in the bar material (c_0), which can be related to Young's

modulus (E) and the density (ρ) of the bar material by,

$$c_o = \left(\frac{E}{\rho} \right)^{1/2} \quad (1.34)$$

The magnitude of the stress generated by the impact of the striker can be related to the initial velocity of the striker (v_s), the bar density (ρ) and speed of sound (c_o),

$$\sigma = \frac{\rho c_o v_s}{2} \quad (1.35)$$

The compressive wave will continue to propagate along the striker bar until it reaches the end of the striker which will occur at $t=L/c_o$, where L is the length of the striker bar and t is the time starting at impact. At this time, the compressive wave will reflect off the free end of the striker bar as a tensile wave of equal magnitude as the incident compressive wave. The tensile wave will unload the striker bar since the two stress waves will cancel each other. Unloading will continue until $t=2L/c_o$ at which time the reflected tensile wave will have completely unloaded the striker bar. For times greater than $2L/c_o$ a compression wave of length $2L$ will propagate along the incident bar as shown in the figure.

In a compressive stress wave, the particle velocity will be in the same direction as the wave propagation direction [23]. In a tensile stress wave, the particle velocity will be in the opposite direction as the wave is travelling. Figure 1.21 is a schematic of the particle velocity within each bar. Initially, the particle velocity within the striker is v_o while the incident bar is at rest. During impact the compressive wave generated in the striker bar will reduce the velocity of the striker by one half. When the compressive wave reflects off the free end of the bar as a tensile pulse, the particle velocity in the

striker bar is reduced to zero. This unloading will continue until $t=2L/c_0$ at which point the entire striker bar will be at rest. The particle velocity within the incident bar will be $1/2 v_0$.

The compressive wave will propagate along the incident bar until it reaches the specimen. The specimen has a smaller cross-sectional area and lower flow stress and will deform plastically. During deformation a portion of the stress wave will be transmitted through the specimen to the transmitter bar while the remainder of the pulse will be reflected. The incident, reflected, and transmitted pulses are measured by strain gauges mounted on the surface of the incident and transmitter bars. These gauges must be mounted far enough from the specimens so that the pulses can be measured independently.

Nicholas [43] provides a derivation for the strain, stress, and strain rate in the specimen. This derivation assumes that the stress in the Hopkinson bars remains elastic. The wave propagation along the bars is also assumed to occur in a undispersed manner. If the wave transient time across the specimen length is small compared to the duration of the incident wave, then many wave reflections can occur within the specimen and uniform deformation can be assumed.

Figure 1.22 is a schematic of the specimen and Hopkinson bar. The incident, reflected, and transmitted pulses are shown as ϵ_i , ϵ_r , and ϵ_t respectively. The subscripts 1 and 2 will be used to represent the incident and transmitter ends of the specimen respectively.

The strain in the bar at the incident end of the specimen will be

$$\epsilon_1 = \epsilon_i - \epsilon_r \quad (1.36)$$

and the strain in the bar at the transmitter end of the specimen will be

$$\epsilon_2 = \epsilon_r \quad (1.37)$$

The displacement at the incident (U_1) and transmitter (U_2) end of the specimen are given by

$$U_1 = c_o \int_0^t (\epsilon_i - \epsilon_r) dt \quad (1.38)$$

and

$$U_2 = c_o \int_0^t \epsilon_t dt \quad (1.39)$$

The average strain in the specimen (ϵ_s) is given by,

$$\epsilon_s = \frac{U_1 - U_2}{L_s} \quad (1.40)$$

where L_s is the gauge length of the specimen. In terms of the strain pulses, the average strain (ϵ_s) becomes,

$$\epsilon_s = \frac{c_o}{L_s} \int_0^t (\epsilon_i - \epsilon_r - \epsilon_t) dt \quad (1.41)$$

The forces at the ends of the specimens are given by,

$$P_1 = EA_b(\epsilon_i + \epsilon_r) \quad (1.42)$$

and

$$P_2 = EA_b\epsilon_t \quad (1.43)$$

where E is Young's modulus and A_b is the cross-sectional area of the bars.

The average force can be calculated to be,

$$P_{av} = \frac{EA_b}{2}(\epsilon_i + \epsilon_r + \epsilon_t) \quad (1.44)$$

If it is assumed that the forces are equal at both ends of the specimen then,

$$\epsilon_i + \epsilon_r = \epsilon_t \quad (1.45)$$

Substituting equation (1.45) into (1.41) and simplifying gives the following expression for strain,

$$\epsilon_s = \frac{-2c_o}{L_s} \int_0^t \epsilon_r dt \quad (1.46)$$

The stress in the specimen can be determined by dividing equation (1.43) by the specimen area (A_s) as follows,

$$\sigma_s = E \frac{A_b}{A_s} \epsilon_t \quad (1.47)$$

Finally, the strain rate can be determined by differentiating equation (1.46) to obtain,

$$\dot{\epsilon}_s = \frac{-2c_o}{L} \epsilon_r = \frac{\Delta V}{L} \quad (1.48)$$

where ΔV is the closure velocity between the ends of the incident and transmitter bars.

Wulf [44] used a configuration similar to Figure 1.19 (b) and known as the direct impact configuration to study adiabatic shear banding in titanium using cylindrical specimens and strain rates between $3 \times 10^3 \text{ s}^{-1}$ and $3 \times 10^4 \text{ s}^{-1}$. The load on the specimen was measured with strain gauges mounted on the transmitter bar. The change in length of the specimen, and hence strain, was measured by monitoring the back end of the projectile with a 25 mm (1") co-axial capacitor. Graphite grease was used to hold the specimen in place on the transmitter bar prior to impact and serves as a lubricant as well.

Hartmann, Kunze, and Meyer [45] used a split Hopkinson bar and a 'top-hat' specimen design to study adiabatic shear banding in a CrMoV medium strength steel and Nimonic 80 A, a precipitation hardening austenitic superalloy. Figure 1.23 shows the test set-up and specimen. The amount of shear strain can be controlled using a collar as shown in Figure 1.23 and therefore interrupted tests can be performed to study the onset of adiabatic shear banding. A disadvantage of this specimen configuration is the higher cost of machining harder materials.

The double notched shear test is another testing technique which uses a slightly modified split Hopkinson bar apparatus. In this test, the transmitter bar is replaced by a tube with an inside diameter which will accommodate the incident bar as seen in Figure 1.24. Two notches are cut into the incident and transmitter bars into which the double notched specimen, Figure 1.25, will fit. Using this type of specimen Campbell and Ferguson [46] were able to achieve shear strain rates as high as $4 \times 10^4 \text{ s}^{-1}$ in mild steel. A major problem with the double-notch specimen is that at shear strains greater than 0.2 the deformation no longer remains pure shear. Figure 1.26 shows a specimen deformed to an apparent shear strain of 0.6, however the edges have rotated and deformed in compression. Upon examination of the shear strain in the region under the notches an average value of shear strain of 0.2 is obtained.

Several investigators [47-49] have used 'punching shear' to study adiabatic shear banding. Such an experimental set up uses a modified split Hopkinson bar similar to the one used in the double notched experiment as seen in Figure 1.27 [47]. Here the test sample is a thin disk in which a circular hole is punched. Meyer and Manwaring [48] used a similar punching arrangement shown in Figure 1.28, except here a ring and punch are used instead of machining the ends of the Hopkinson bars directly.

1.4 Present Research

The present research program has focused on (i) the development of a compressive split Hopkinson bar (CSHB) apparatus for the study of adiabatic shear banding and (ii) the investigation of adiabatic shear banding in a series of ferrous materials displaying a range of initial hardnesses. The CSHB designed and commissioned for this research is similar to the designs discussed in Section 1.3.3. The shear testing utilizes a ring and punch design similar to that used by Meyer and Manwaring [48] for the study of adiabatic

shear banding. Two high purity irons, Armco and Remco, and 4340 steel were tested. Armco and Remco irons are used in Explosively Formed Projectiles (EFP's). The 4340 steels were tested in a range of quenched and tempered conditions in order to vary the initial hardnesses and degree of work hardening and to determine the effect these conditions would have on propensity to adiabatic shear banding. Shock-hardened Remco iron was also tested in order to examine the effect of shock hardening on adiabatic shear band formation.

Metallographic examination of deformed specimens was performed to determine the extent and structure of the adiabatic shear bands. Supporting numerical simulations of the experiments were also performed utilizing a user material model developed as part of this research. The model incorporates the adiabatic shear band criterion discussed in Section 1.2 in order to investigate the suitability of these criteria.

The remainder of the thesis is organized as follows. Chapter 2 provides a description of the CSHB facility and experiments. Chapter 3 presents a description of the materials investigated as part of this research. Chapter 4 describes the numerical model used for this research. The experimental and numerical results are presented in Chapters 5 and 6, respectively. These results are discussed in Chapter 7 and the conclusions and recommendations are presented in Chapter 8.

Chapter 2

Experimental Design

2.1 Compressive Split Hopkinson Bar Apparatus

The compressive split Hopkinson bar (CSHB) at Carleton University is designed to test materials at high rates of strain. Depending on the specimen geometry, the CSHB is capable of testing materials in either uniaxial compression or "punching shear". Strain rates of 3000 s^{-1} can be attained in compression and $50,000 \text{ s}^{-1}$ in shear. A schematic of the CSHB is shown in Figure 2.1 (a) and a photograph of the apparatus can be found in Figure 2.1 (b).

The specimen to be tested is mounted between the ends of the incident and transmitter bars inside the test chamber. The striker bar is propelled by the gas gun and impacts the end of the incident bar away from the specimen. This impact induces a compressive pulse, termed the incident pulse, which travels from the impacted end towards the specimen. When the incident pulse reaches the specimen it is partially transmitted through the specimen and partially reflected back into the incident bar. The

incident, transmitted, and reflected pulses are recorded by strain gauges mounted on each bar. Using the Hopkinson bar equations developed in Chapter 1, the strain, strain rate and the stress histories of the specimen can be determined from the recorded pulses.

The striker, incident, and transmitter bars are made of drill rod due to its relatively high strength and ease of availability. The striker bar is 304.8 mm (12") in length and has a diameter of 12.7 mm (0.5"). The incident and transmitter bars are both 711 mm (28") in length and have a diameter of 12.7 mm (0.5"). The strain gauges are mounted in the middle of the incident and transmitter bar which ensures that the incident and reflected pulses do not overlap at the strain gauge location.

The general concept followed while designing the CSHB was that if the striker, incident and transmitter bars could be concentrically mounted within a long rigid tube, alignment of the bars would be ensured. This design philosophy is reflected in the schematic where the entire assembly, when connected together, acts as one long tube. The CSHB assembly consists of four main components: the gas gun; two alignment tubes; test chamber; and, the momentum trap. Each of these components are connected with a coupling that uses a brass bushing to facilitate alignment. Each of these main components will be described separately in the following sections.

2.1.1 Gas Gun Assembly

A schematic of the gas gun assembly can be found in Figure 2.2 (a) and a photograph in Figure 2.2 (b). The gas gun consists of four major components: the piston; accumulators; gun bushing; and the gun barrel.

The piston is made of aluminum and is machined into a dumbbell shape to reduce mass. The accumulators are mounted on either side of the gun barrel and are permanently open to the inside of the gun barrel. The bore of the brass gun bushing was fabricated using a gun drilling operation to ensure straightness and roundness, thus preventing the striker from becoming wedged in the gun bushing during firing. The radial clearance between the striker and gun bushing is only 0.0381 mm (0.0015"). The end of the gun bushing is machined so that there is a close running fit between the outside of the bushing and the inside of the gun barrel. This allows the launcher tube to slide onto the gun bushing and ensure proper alignment. The gun barrel and bushing are coupled together using a large nut which threads onto the outside of the gun barrel.

To prepare for firing, the piston is pushed to the far end of the gun barrel. The striker is positioned as shown between the piston and the gun bushing. The accumulators are pressurized with nitrogen. The two o-rings on the piston prevent the gas from entering the gun barrel. To fire the gas gun, the piston is pushed forward by flowing nitrogen through the bleed hole in behind the piston. This flow is controlled using a solenoid valve placed at the end of the bleed line so that the line can be fully charged before the valve is opened. Once the piston is pushed forward, the seal between the rear o-ring and the gun barrel is breached and the pressurized nitrogen in the accumulators enters the gun barrel. The expanding gas propels the piston along the length of the gun barrel until it impacts the end of the gun bushing. The piston then stops, releasing the striker to travel the length of the gun bushing where it will impact the incident bar. A soft buffer is placed between the piston and gun bushing to cushion the impact. After

firing, the coupling nut is removed and the gun barrel is pulled off of the gun bushing so that the piston and striker can be pushed back into firing position. The gun barrel is mounted onto a rail system which allows the gun barrel to slide on and off the gun bushing so that the striker bar and piston can be repositioned.

2.1.2 Alignment Tube Assembly

Figure 2.3 (a) is a schematic of the alignment tube assembly and Figure 2.3 (b) is a photograph of the assembly for the transmitter bar. There are two alignment tubes, one that aligns the incident bar and the second, the transmitter bar. These two tubes are identical except for the fact that they connect different components. The alignment tube used to align the incident bar runs between the gun bushing and test chamber. The end of the gun bushing that supports the incident bar is machined such that a close running fit exists between it and the inside of the alignment tube. The alignment tube is placed over the gun bushing and held in place by a coupling nut. This nut has a right- and left-handed, thread so that when it is tightened the two parts are drawn together. The other end of the alignment tube is connected to the test chamber. A brass alignment bushing is used to align the tube and test chamber and an identical coupling is used to connect the two parts.

The alignment tube used to support the transmitter bar runs between the test chamber and the momentum trap. An alignment bushing is used to locate one end of the alignment tube relative to the test chamber and a second bushing to centre the momentum trap. Once again, coupling nuts are used to connect the components together.

Both the incident and transmitter bars slide inside teflon bushings which assure accurate alignment while permitting stress waves to pass without dispersion [43]. The teflon bushings are press fit into the alignment bushings and the end of the gun bushing. Each of the alignment tubes has a 9.53 mm (0.375") hole drilled on either side at a distance of 140 mm (5.5") from the end closest to the test chamber. These holes provide access for the strain gauge wires.

2.1.3 Test Chamber Assembly

A schematic of the test chamber is shown in Figure 2.4 (a) with a corresponding photograph in Figure 2.4 (b). The test chamber connects the two alignment tubes to each other. The interior of the test chamber is a 216 mm (8.5") diameter cylinder 305 mm (12") in length. There is a 76.2 mm (3") hole cut in the top and a 76.2 x 76.2 mm (3" x 3") square cut in the bottom of the test chamber. There is also a 152.4 mm (6") long by 101.6 mm (4") high rectangle cut in either side of the test chamber. These holes allow access to the interior of the test chamber to mount specimens. The size of the chamber itself allows sufficient room for the specimen holder and heaters used for elevated temperature tests.

2.1.4 The Momentum Trap Assembly

Figure 2.5 (a) shows a schematic of the momentum trap assembly with a corresponding photograph in Figure 2.5 (b). The momentum trap is connected to the

transmitter bar alignment tube with an alignment bushing and a coupling nut as described earlier. The purpose of the momentum trap is to brake the forward movement of the transmitter and incident bars after a test. A pin made from drill rod is placed with one end in the alignment bushing and the other sandwiched between two pieces of rubber as shown in Figure 2.5 (a). Two half-moon steel spacers are placed on either side of the rubber pieces. These spacers are pushed inwards by two set screws threaded into the wall of the momentum trap tube, causing the rubber to compress around the pin. The pin is driven into the rubber by the transmitter bar, producing a braking action. Once the test is completed, the momentum trap end cap is removed, the set screws are loosened, and the pin can be reset.

2.1.5 Mounting and Alignment

The entire CSHB apparatus is supported on a W3x31 wide flange beam 3.15 m (124") in length. A series of 25.4 mm (1") plates are bolted to the beam to which each of the mounting components are attached. The test chamber is attached to the steel base plate by a cradle support as seen in Figure 2.4 (b). The launcher tube is mounted onto a rail system so the launcher tube can be pulled off the launcher bushing. The rail system is 1.22 m (48") in length and provides sufficient room between the launcher tube and bushing so the piston may be returned to its firing position.

Using the test chamber as a reference, the alignment tubes, momentum trap, and the gun bushing are attached with the coupling nuts and alignment bushings and are supported using the adjustable supports seen in Figures 2.3 (b), 2.5 (b), and 2.2 (b).

Using these adjustable supports the gun barrel and gun bushing are aligned to ensure the gun barrel slides freely onto the gun bushing. The test chamber is levelled using steel shims under the cradle support.

2.2 Specimen Design

Using the CSHB, specimens can be deformed in compression or shear depending on the specimen geometry. To deform specimens in compression, simple right cylinders are used; however to deform specimens in shear an assembly of four components.

2.2.1 Compression Specimen

The compression samples are simple right cylinders with a diameter of 6.35 mm (0.25") and a length of 3.18 mm (0.125"). The diameter was chosen to provide a large enough change in impedance to ensure plastic deformation within the specimen. To reduce friction, a length to diameter ratio (L/D) of between 1.5 and 2.0 is recommended [50]; however the optimum L/D ratio for a Hopkinson bar is one half that which is recommended for reducing friction, that is between 0.75 and 1.0 [51]. The dimensions of the compression specimen gives a L/D ratio of 0.5 which not expected to introduce serious errors as long as the interfaces are well lubricated [50].

The compression sample must be mounted along the centre line of the incident and transmitter bars. An alignment tool is used to centre the compression specimen, a schematic of which can be found in Figure 2.6. The compression specimen is placed in

the middle of the alignment tool as shown and the tool is machined such that when the bars are brought together and the tool cradles the bars the specimen will be concentric with the bars. A layer of lubricant, Lubroplate No. 105, is placed between the specimen and the bars to reduce friction. The viscosity of the lubricant offers the added benefit of holding the specimen in place prior to testing. A plastic compressive alignment sleeve, which is simple a hollow cylinder, is used to align the ends of the bars with each other. Figure 2.7 is a schematic of the final assembly prior to testing.

2.2.2 Shear Specimen

The shear specimen consists of an assembly of four components: a die plate; specimen; punch; and shear alignment sleeve. Figure 2.8 (a) is a schematic of the assembly and 2.8 (b) is a photograph of the individual components. The specimen is a 12.7 mm (0.5") diameter disc with a thickness of 1.27 mm (0.050"). The die plate and punch were fabricated from W1 drill rod and hardened via a water quenched followed by a 250 °C temper for 1 hour in an inert environment. The die plate has an inside diameter of 8.59 mm (0.338") and the punch has an outside diameter of 8.33 mm (0.328"). These dimensions produce a radial clearance of 0.127 mm (0.005") and a nominal diameter of 8.46 mm (0.333"). A radius of 0.127-0.178 mm (0.005-0.007") was machined on to the die plate and punch as indicated on the schematic. The radius ensures a consistent shear zone from test to test and simplifies the computer simulations.

The shear alignment sleeve serves a dual purpose. It aligns the specimen with the die plate and the punch and aligns the assembly with the incident and transmitter bars.

During a test the die plate is driven over the punch producing an annular shear zone of 0.127 mm (0.005") through the thickness of the specimen.

In order to interrupt the shear process during deformation different die plate thicknesses were used. A reduction in die plate thickness has the effect of shortening the punch travel. As the shear process proceeds the specimen will "bottom out" on the face of the incident bar. This will produce specimens with varying degrees of shear deformation and which can be removed for metallographic examination.

2.3 Instrumentation

2.3.1 Velocity Detector

The velocity of the striker is measured using a detector shown schematically in Figure 2.9 (a) and pictorially in Figure 2.9 (b). The velocity detector is positioned to measure the striker velocity prior to impact of striker and incident bars and after the piston has impacted the gun bushing. A beam, emitted by a 5 mW helium-neon laser, is split and reflected to produce two parallel beams across the path of the striker. These beams are detected by light sensors on the opposite side of the gun bushing. The sensors are connected to an exclusive OR gate which will produce an output voltage of five volts when only one of the sensors are excited and zero volts when both or neither of the sensors are excited. When the striker cuts the first beam, a counter is triggered which is stopped when the striker cuts the second beam after which the detector displays the

elapsed time. The distance between the sensors can be measured using a displacement gauge by monitoring the output voltage of the detector while the striker is moved manually down the launcher bushing. The velocity can then be calculated since the elapsed time and distance are known.

2.3.2 Strain Gauges

The stress waves in the incident and transmitter bars are determined using strain gauges that are mounted on the midpoint of the incident and transmitter bars. Two strain gauges are mounted on opposite side of each bar to eliminate any bending. Kulite semiconductor strain gauges (UGP-1000-060) having a gauge factor of approximately 155 were used. Semiconductor gauges were selected over foil gauges since the higher gauge factor means that no signal amplification is needed prior to recording. The strain gauges are connected to opposite arms of a standard Wheatstone bridge which has the effect of doubling the effective gauge factor. A fixed resistance and a potentiometer are connected to the remaining two arms of the bridge. The potentiometer is used to balance the bridge before the start of each test. In an effort to avoid electromagnetic interference, the bridges are housed separately and use separate Hewlett Packard 6213A power supplies. The excitation voltage used for all experiments was 1400 ± 0.1 mV. The output voltages from these bridges are recorded by a two channel Nicolet Pro 30 Digital Oscilloscope which has 12 bit resolution and can record 10 million sample points per second per channel with a temporal accuracy of 1 ns.

The full nonlinear form of the Wheatstone bridge equation [52] was used to convert bridge voltage to strains:

$$\left(\frac{\Delta E_a}{a V_e}\right) = \frac{GF_2 \epsilon + GF_1 \epsilon + GF_1 GF_2 \epsilon^2}{(1 + a)^2 + (1 + a)(GF_2 + a GF_1) \epsilon + GF_1 GF_2 \epsilon^2} \quad (2.1)$$

where,

$$a = \frac{R_f}{R_{sl}} \quad (2.2)$$

and ΔE_a is the change in bridge voltage, V_e is the excitation voltage, ϵ is the strain, and GF_1 and GF_2 are the gauge factors for the strain gauges. The full non-linear form of the equation (2.1) is required since the change in resistance of the semi-conductor gauge is large due to their large gauge factor.

2.3.3 Strain Gauge Calibration

The gauge factors of the strain gauges may be altered during installation, therefore the gauges must be calibrated to determine each of the gauge factors [51]. In order to calibrate the gauges while the incident and transmitter bars remain in place, several additional fixtures have been designed. Figure 2.10 is a schematic of the calibration setup. A load cell is placed within the test chamber between the incident and transmitter

bars. The gun barrel is pulled back and replaced by an end cap. A rod is placed in the gun bushing that bridges the gap between the end of the incident bar and the end cap. The momentum trap and the alignment bushing connecting it to the transmitter alignment tube are removed. An additional brass bushing is used to support the end of the transmitter bar and a second end cap is placed on the end of the alignment tube. A cap screw is threaded into the end cap connected to the alignment tube. The cap screw will push against the brass bushing which will put both the incident and transmitter bars into compression.

The gauge factor is defined as,

$$GF = \frac{\Delta R/R_i}{\epsilon} \quad (2.3)$$

where R_i is the initial resistance and ΔR is the change in resistance for a given strain of ϵ . The load in the load cell can be determined from,

$$P = \frac{P_o}{GE_o} \Delta V_c \quad (2.4)$$

where P_o is the maximum load in the load cell, G is the load cell calibration factor, E_o is the excitation voltage to the load cell, and ΔV_c is the change in voltage of the load cell.

The strain, ϵ , in the bar can be expressed as,

$$\epsilon = \frac{\sigma}{E} = \frac{P}{A_b E} = C_1 \Delta V_c \quad (2.5)$$

where σ is the stress in the bar, E is the Young's modulus of the bar material, A is the area of the bar, and $C_1 = P_0 / GE_0 AE$. Using equation (2.3) and substituting for ϵ in equation (2.5), the gauge factor becomes,

$$GF = \frac{\Delta R / R_i}{C_1 \Delta V_c} \quad (2.6)$$

As a load is applied to the incident and transmitter bars the resistance of the strain gauges and the voltage of the load cell is recorded. These two measurements are plotted as a resistance versus voltage graph, a typical graph can be seen in Figure 2.11. Using linear regression an approximation of this data having the form,

$$y = mx + b \quad (2.7)$$

is obtained where y is resistance, x is voltage, m is the slope and b the intercept can be obtained. Here $m = \Delta R / \Delta V_c$ and $b = R_i$ and therefore equation (2.6) becomes,

$$GF = \frac{m}{C_1 b} \quad (2.8)$$

The strain gauge calibration was also checked dynamically by comparing the

velocity recorded by the velocity detector and a calculated velocity determined from the incident pulse. The velocity can be determined from the incident pulse by considering conservation of momentum between the striker and incident bars. The striker bar has an initial momentum of $m_s V_s$, where m_s and V_s are the mass and velocity of the striker bar respectively. After the impact between the striker and incident bars, the striker comes to rest and therefore all of the striker's momentum is transferred to the incident bar. The change in momentum of the incident bar is equal to the impulse that acts on the incident bar during the collision by,

$$m_i V_i = \int_0^t P_i dt \quad (2.9)$$

where P_i is the force that acts on the incident bar during the impact. P_i can be expressed as,

$$P_i = \sigma_i A_i \quad (2.10)$$

where σ_i and A are the stress and area of the incident bar. The stress wave that propagates along the incident bar after impact remains elastic and therefore,

$$\sigma_i = E \epsilon_i \quad (2.11)$$

where E and ϵ_i are the Young's modulus and strain of the incident bar. Combining

equations (2.9), (2.10), and (2.11) yields.

$$V_s = \frac{EA_b}{m_s} \int \epsilon_i dt \quad (2.12)$$

Using this equation a FORTRAN program was written, which can be found in Appendix E, to calculate the striker velocity from the strain gauge data.

Table 2.1 compares the velocity obtained from the velocity detector, V_s , with the velocity calculated from equation 2.12, V_c . The table shows that there is error of 5.13% between V_s and V_c ; however, V_s is always greater than V_c , which raises some concern since it is the opposite trend to what was expected. The velocity detector is located before the impact of the striker and incident bars and therefore the striker is expected to decelerate due to friction between the striker and gun bushing. The source of this difference between the measured velocity and that determined using equation 2.12 is unknown at the present time. Future work should consider foil strain gauges, where response may be less rate sensitive than the semi-conductor gauges used for the present research. A redundant pressure measurement system such as Manganin pressure gauges may also be used to more accurately calibrate the strain gauge measurements. An improved velocity detector using redundant sensors may also help to detect both velocity and acceleration of the striker prior to impact.

2.3.4 Data Reduction

A FORTRAN 77 program called NICOLET, presented in Appendix A, was written by Pelletier [53] and only slight modifications are required for this research. NICOLET converts the voltage versus time data from the strain gauges to strain versus time data using equation (2.1). The program then takes this data and sections it into incident, transmitted and reflected pulses. Using these pulses and the Hopkinson bar equations from Chapter 1, the engineering stress and strain as well a strain rate are calculated. Since the Hopkinson bar equations assume a compression sample, no further conversion is need for the compression samples. Using,

$$\sigma_{true} = \sigma_{eng}(1 + \epsilon_{eng}) \quad (2.13)$$

and,

$$\epsilon_{true} = \ln(\epsilon_{eng} + 1) \quad (2.14)$$

the true stress and true strain can be calculated [54].

For the shear specimen, the nominal shear strain rate can be calculated using,

$$\dot{\gamma} = \frac{\Delta V}{2\delta} \quad (2.15)$$

where ΔV is the rate of closure between the Hopkinson bar ends and δ is the radial clearance. Comparing equation (2.15) with the strain rate calculated using equation (1.48), it can be seen that only a slight modification is required. If the gauge length (L_g)

is equated with the radial clearance (δ) and the resulting strain rate is divided by two. equation (1.48) becomes equation (2.15). A similar division by two and equating of L_s with δ in equation (1.46) will yield the shear strain. Shear stress can be calculated by equating the area of the specimens area in equation (1.47) to the average area where shear occurs. These modification were implemented in a version of NICOLET as part of the current research.

2.4 Elevated Temperature Tests

As part of this research, elevated temperature tests were conducted. It was desirable to achieve the heating of the test sample without heating the incident or transmitter bars, as wave reflection can occur due to the temperature gradient. To achieve this requirement, a shear specimen holder, shown in Figure 2.12, is used to hold the shear specimen in place while heating occurs. Heating of the shear specimen was achieved by using two heat guns, one mounted to blow in from the side and a second from the top, as seen in Figure 2.12. Heat guns were chosen over more conventional furnaces because of the speed at which temperatures of up to 300°C could be reached.

No attempt was made to record the temperature of the specimen during the actual shear experiments since the thermocouple would interfere with the operation of the CSHB, however the temperature of the sleeve was monitored during testing. To validate this procedure, thermocouples were installed on a dummy specimen and used to obtain representative heating and cooling curves. Temperature curves for the heating and

cooling processes are shown in Figure 2.13 (a) and (b). As can be seen in Figure 2.13 (a), the target temperature of approximately 300°C, is reached in 150 seconds and held for a period of 100 seconds. Three thermocouples were used during the heating and cooling experiments: one attached to the alignment sleeve, one at the specimen centre, and one on the specimen edge. The data acquisition system used in these tests only allowed for the monitoring of two of the thermocouples at a time. The output voltage from two of the thermocouples are feed into the Nicolet while the remaining thermocouple is connected to a thermocouple reader. The output voltage from a thermocouple can be converted into temperature using the following equation,

$$T = a_0 + a_1x + a_2x^2 + \dots + a_nx^n \quad (2.12)$$

where x is the output voltage from the thermocouple and the constants a_0 , a_1 , a_2 , ..., and a_n are constants that can be found in Table 2.2 for different types of thermocouples [55]. For these experiments a type K thermocouple was used.

The voltage that is recorded by the Nicolet can not be directly converted into temperature using in equation (2.12) since the ends of the thermocouples are attached to a coaxial cable and a BNC junction is used to connect to the Nicolet. These extra junctions result in a decrease in the recorded voltage compared to the voltage at the thermocouple. In order to correct for error the peak temperature measured by the third thermocouple is used as a reference. Using a temperature versus voltage table [56] for a type K thermocouple, the reference temperature is converted into a voltage and the voltages measured by the Nicolet are corrected to this value. Using this technique of

measurement it is felt that the thermocouple output voltage can be converted into temperature with a reasonable degree of accuracy.

It can be seen from Figure 2.13 (a) that there is no significant temperature gradient across the specimen since the recorded temperature from the centre and edge are indiscernible. Figure 2.13 (b) shows that there is only a slight difference in temperature between the specimen and the sleeve and therefore the sleeve temperature can be used as a reasonable approximation of the temperature of the specimen. When the bars are brought into place, the temperature within the specimen and sleeve will fall dramatically, as seen in Figures 2.13 (a) and (b). From Figure 2.13 (a) it can be estimated that there is a window of approximately 10 seconds before the specimen temperature falls below 250°C and that the test must occur within this time period.

Chapter 3

Materials

The materials studied in this research were Armco iron, Remco iron, and 4340 steel. The Armco and Remco irons are high purity irons with used as liners in Explosively Formed Projectiles (EFP's). To investigate the effect of shock-hardening on the shear susceptibility of iron, Remco iron was tested in the as-received and shock-hardened conditions. Armco iron was only available in the hot rolled condition. The 4340 steel was tested in a variety of heat treated conditions to investigate the effect of initial hardness (and reduced work hardening) on the onset of adiabatic shear localization.

Undeformed and deformed specimens were prepared using the standard metallographic technique of sectioning, polishing, and etching. The final preparation sequence is as follows:

1. Section to slightly above the mid-plane with an abrasive cut-off wheel.
2. Mounted in 1.25 inch bakelite mounts with small plastic clips to ensure that the specimen remained normal to the polishing surface during mounting.

3. 240 and 400 grit silicon paper with water as a lubricant to grind the specimen to 70 μm from the mid-plane.
4. 1-2 minutes on 600 grit microcut paper with water as a lubricant. Microcut was used in place of silicon paper to ensure no embedding of silicon particles in the specimen.
5. 3 minutes on Buehler Metcloth charged with 3 μm oil-based diamond suspension with Struers DP-Lubricant blue as a lubricant.
6. 3 minutes on Buehler Metcloth charged with 1 μm oil-based diamond suspension with Struers DP-Lubricant blue as a lubricant.
7. 45 seconds on Buehler Microcloth charged with 0.3 μm water-based gamma alumina suspension.
8. 45 seconds on Struers OP-Chem cloth charged with Struers OP-U suspension.

All grinding and polishing processes were performed on a Buehler ECONOMET III automatic polishing machine fitted with a WHIRLMET polishing attachment. The wheel was set to rotate at 125 rpm and a 1.42 kg mass was used for steps 3-6 and replaced with a 1.0 kg mass for the final two steps. The specimens were etched using a solution of 2% nitric acid and 98% methyl alcohol, commonly known as 2% nital. Etching times varied depending on the material and amount of plastic deformation.

3.1 Armco Iron

Armco iron is described at length in a recent Carleton M.Eng thesis by Pelletier [53] and therefore will only be discussed briefly here. Armco iron is a highly ductile high, purity alpha iron of about 99.93% Fe with impurity content given in Table 3.1. All of the Armco iron specimens were machined from 25 mm (1") plate supplied by SNC Industrial Technologies Inc.. The 25 mm (1") plate had a varying grain size through the thickness. The central portion of the plate had a fine grain structure (30-35 μm) while there was a coarse grain structure (350-500 μm) near the surfaces of the plate. The coarse structure extended to a depth of 3 to 5 mm below the surface of the plate. Photomicrographs of the fine and coarse grain structures can be found in Figures 3.1 (a) and (b). Both fine and coarse grain shear specimens were tested and the disc-shaped shear specimens were taken parallel to the plate's surface. Only fine grain compression samples were tested and with axes parallel to the rolling direction.

The Armco iron plate was annealed at 550°C for 2 hours in an nitrogen atmosphere. This heat treatment will relieve residual stresses due to hot rolling without significant grain growth [57].

Figures 3.2 and 3.3 show experimental true stress versus true strain rate data for Armco iron at various levels of strains from different researchers [17,24,28,42,58]. Johnson and Cook [24] and Nicholas [17] demonstrated an exponential relationship between stress and strain rate which is reflected in the Johnson-Cook constitutive equation (1.25). This exponential relationship is seen in the near linear variation of stress with the

logarithm of strain rate for strain rates up to 10^3 s^{-1} .

Lindholm [59] performed compression tests on Armco iron in the strain rate range of 10^{-3} to 10^3 s^{-1} and his results are also plotted in Figures 3.2 and 3.3. The compression tests showed a linear variation in stress with the logarithm of strain rate for strain values of 0.020 and 0.051 for the entire strain rate range and supports the exponential relationship suggested by both Johnson and Cook and by Nicholas. At strain levels above 0.1 and strain rates below 1.0 s^{-1} , the data does not follow the same relationship as that of the higher strain rate data. Lindholm noted the presence of a negative slope in the stress-strain rate curve at higher strains in the 0.01 to 1.0 s^{-1} strain rate range. Lindholm attributed this negative slope to strain aging which will occur by diffusion of the interstitial elements, carbon and hydrogen, to the active dislocation sites. Lindholm suggested that a straight line approximation could be used for strain rates below 1.0 s^{-1} and another for higher strain rates.

Reinders and Kunze [60] studied Armco iron in tension at different strain rates and grain sizes. The three grain sizes studied were 15, 18 and $300 \mu\text{m}$. A characteristic increase in flow stress with strain rate was obtained. They also demonstrated that under quasi-static loading the Hall-Petch relationship was followed, but under dynamic loading this did not occur. These results showed that at high strain rates the grain size dependence of the flow stress is minimal and that the large and fine grain materials have nearly the same strength.

The parameters for Armco iron for the Johnson-Cook [24,61] constitutive relation (equation (1.25)) can be found in Table 3.2.

3.2 Remco Iron

Like Armco iron, Remco iron is a high purity iron but has a carbon concentration of only 0.015 w% [62]. Remco iron is being considered for this research since it was available in the shocked and unshocked conditions whereas Armco iron was only available in the hot rolled condition. When a material is subjected to a high-intensity shock wave, a significant change in microstructure and therefore mechanical properties can occur. In the past, when modelling metal behaviour following impulsive or explosive loading, stress-strain data from materials in the unshocked condition have been used with poor success. Therefore it is important to have stress-strain data from materials in the shocked condition to accurately model the behaviour of impulsively loaded metals.

The shock-hardened material was provided by the Australian Defense Science and Technology Office. Shock-hardening was performed using a plane-wave generator based on the mouse-trap design, using compatible driver plate and target assembly materials, as seen in Figure 3.4. A complete description of the shock-hardening procedure can be found in [63]. After impact with the driver plate, the targets were decelerated in a water tank. This has a two-fold purpose, to reduce secondary damage and to restrict thermal recovery of the shocked microstructure.

Photomicrographs of the as-received and shocked hardened Remco iron, with a grain size of 22 μm , can be seen in Figures 3.5 (a) and (b). The shock-hardened material (Figure 3.5 (b)) shows a high density of mechanical twins as compared to the as-received material (Figure 3.5 (a)). A through-thickness hardness trace of the shocked material,

shown in Figure 3.6, shows that the hardness increased from 126 Hv in the as-received condition to approximately 260 Hv in the shocked condition. It should be noted from Figure 3.6 that there is a slight decrease in hardness from the impact to back surfaces. This drop in hardness is due to a damping out of the shock wave as it passed through the material. After shock loading, the thickness of the material was only decreased by 3% and therefore the large increase in hardness was due to the passing of the planar compressive shock pulse and not to strain hardening.

Figure 3.7 is a stress-strain curve for the as-received and shock-hardened materials produced by Weston [62]. The shocked material shows a high initial yield strength and a steady decrease in stress after initial yield whereas the as-received material shows a slight work hardening response. Figure 3.8 shows the strain rate dependence of both the as-received and shock-hardened materials. The as-received material shows the characteristic strain rate hardening whereas the shocked material is relatively independent of strain rate up to a strain rate of 100 s^{-1} . After strain rate of 100 s^{-1} there is a jump in the flow stress, however the mechanism for this jump could not be identified from the results [62].

The parameters of the Goldthorpe modified Zerilli-Armstrong constitutive relation [28] (equation (1.31)) can be found in Table 3.3 for both the as-received and shock hardened materials.

3.3 AISI 4340 Steel

Adiabatic shear bands readily form in 4340 steel and therefore this material will be considered for this research. 4340 is a nickel-chromium-molybdenum (Ni-Cr-Mo) steel with a composition listed in Table 3.1. The Ni in combination with Cr will increase the elastic limits, hardenability, and impact and fatigue resistance. The addition of Mo will further increase the hardenability while minimizing the susceptibility to temper embrittlement.

The 4340 specimens were austenitized at 870° C for 1 hour and then oil quenched. After quenching, the specimens were tempered for 2 hours at three different temperatures, 200, 450, and 650°C. The mechanical properties of 4340 with different tempering temperatures can be found in Table 3.4. The table shows good agreement between published and measured hardnesses.

The oil quench will produce a martensitic structure, seen in Figure 3.9, which will have high strength and hardness but will be extremely brittle. The tempering process transforms the martensite to tempered martensite composed of stable ferrite and cementite (Fe_3C) phases. The microstructures from the three different tempering processes can be found in Figure 3.10 (a), (b), and (c).

Cho, Chi, and Duffy [64] performed torsional split Hopkinson bar test on 4340 steel tempered at 450 and 200°C at a strain rate of 10^3 s^{-1} . The yield stresses for the 450 and 200°C temper are 865 and 1070 MPa, respectively, and the corresponding fracture strain are 0.2 and 0.15. The parameters for the Johnson-Cook constitutive model can be found in Table 3.2 for 4340 steel with a 650°C temper.

Chapter 4

Numerical Model

Computer simulations of the shear specimens were undertaken to assist in the interpretation of the experimental results. These models also provided a test bed to evaluate the criteria for predicting adiabatic shear band localization that were presented in Section 1.2.

Finite element simulations of the shear specimens were performed using the LS-DYNA2D [65] finite element code. LS-DYNA2D is a vectorized, lumped mass, explicit, two-dimensional, axisymmetric and plane strain finite element code for analyzing large deformation dynamic and hydrodynamic response of inelastic solids. Quadrilateral and triangular elements utilizing single point integration are used for spatial discretization and an hourglass viscosity is used to control so-called zero energy modes. The equations-of-state are integrated by the central difference method (Petrov-Galerkin). LS-DYNA2D uses linear-quadratic bulk viscosity to smear shock fronts over several elements to dampen numerical oscillations. LS-DYNA2D has an added feature called a 'user subroutine' that allows the user to incorporate a constitutive model that is not already part

of the LS-DYNA2D code. This feature was utilized in the present research to implement the models of adiabatic shear described in Chapter 1.

The geometry and meshes used for this research were prepared using the I-DEAS Masters Series [66] CAD software and were translated into a LS-DYNA2D input file using PATDY2 [67]. LS-ORION [68] was used as the post-processor for the analyses.

4.1 Finite Element Mesh

The axisymmetric nature of the die-specimen-punch assembly shown in Figure 2.8 (a) allows for the use of a two-dimensional (2D) axisymmetric formulation which gives considerable computational savings over a three-dimensional (3D) simulation. Figure 4.1 shows the finite element mesh used to model the die-specimen-punch assembly adopted this research. Figure 4.2 shows the shear zone and the punch and die radii in more detail. The mesh within the shear zone is extremely fine with an element size of 25 μm . Figure 4.2 shows that extensive mesh refinement has been employed in the shear region and around the radii of the die plate and punch and a coarser more computationally effective mesh has been used outside this region. Large transitions in mesh size were avoided, since large increases in element size may cause artificial reflection of stress waves.

With appropriate velocity boundary conditions placed on the punch and die faces in contact with the Hopkinson bars, only the die plate, specimen, and punch need to be modeled. This simplification will lead to considerable savings in computational time as compared to full bar simulations. The shear alignment sleeve was not considered in the

simulation due to its low density and acoustical impedance which should not interfere with wave propagation in the specimen and Hopkinson bars.

4.2 Boundary Conditions

Kinematic boundary conditions were applied to the die-specimen-punch assembly to simulate the experimental conditions. Using the strain rates calculated from the experimental results and employing equation 2.6 with a radial clearance, δ , of 0.127 mm (0.005 in), the closure velocity, ΔV , between the die and punch can be calculated. Figure 4.3 shows the closure velocity versus time curves for the materials tested. From these curves, approximate velocity load curves can be calculated and are shown in the figure (dashed curves). To enforce this closure velocity, the bottom of the punch is fixed in the axial direction and the prescribed velocity time history is applied to the top of the die. This approach will result in the die being pushed downward over the punch.

Intermittent contact boundary conditions, known as slide lines, were defined along the die-specimen and the specimen-punch interfaces. Slide lines use a penalty function formulation to prevent the contacting bodies from passing through each other. Essentially, "numerical springs" are inserted between contacting bodies to enforce separation. An automatic contact option was utilized which updates contact definitions to account for element failure since the adiabatic shear band will cut the contact boundary once failure initiates. The contact boundary is redefined at user specified intervals to account for element failure.

4.3 Constitutive Model

As mentioned in the introduction, LS-DYNA2D provides a 'user subroutine' interface that allows the user to introduce custom constitutive models. A 'user subroutine', called umat48, was written for this research and can be found in Appendix B. This subroutine calls a second subroutine called gflow, which contains seven different optional constitutive equations and can be found in Appendix C. The Johnson-Cook, Zerilli-Armstrong, and Goldthorpe-modified Zerilli-Armstrong were all coded into the gflow subroutine.

The maximum shear stress and flow localization criterion discussed in Section 1.2 were also coded into the umat48 subroutine. Using the maximum stress criterion, failure is predicted within an element when a value of less than zero for equation (1.5) is reached, i.e. when softening exceeds hardening. For the localization criterion, a critical value of α (equation (1.25)) is used to predict failure. Once failure is predicted, the element is deleted from the mesh. As failure proceeds a 'crack' will propagate through the entire thickness of the specimen. Failing elements in this manner may lead to numerical problem such as 'chatter' which will be described later in this section.

Simulations were performed on three materials: 4340 steel in the 650°C tempered condition and annealed and shock-hardened Remco iron. The Remco irons and the 4340 tempered steel were modeled using the Goldthorpe-modified Zerilli-Armstrong and Johnson-Cook constitutive equations, respectively. Elevated temperature simulations were performed on the annealed and shock-hardened Remco irons by setting the initial

temperature to 250°C. The shear deformation was treated as adiabatic with all plastic work being converted to thermal energy. No attempt was made to account for the heat loss due to conduction of heat to the surrounding material.

Preliminary simulations were performed using the umat48 user subroutine with the maximum shear stress criterion given by equation (1.5). For annealed Remco iron Figure 4.4 shows that failure has occurred not only in the shear band but also under the die and punch radii. The predicted failure in these regions was the result of large plastic strains at the punch and die radii which leads to thermal softening. Based on experimental observations the material in this region does not shear localize and removal of these elements would be inappropriate. The experimental results support that fracture did not occur in this area, which suggests that prediction of failure due solely to an attainment of maximum shear stress is insufficient.

A method for avoiding the erosion of elements which are in contact with the die or punch was developed. A second criterion for failure was added that requires a critical value of average principal shear strain rate be reached. This criterion to ensure that failure only occurs in regions of large shear strains. The principal shear strain rate is calculated using,

$$\dot{\gamma}_p = \sqrt{\left(\frac{\dot{\epsilon}_r - \dot{\epsilon}_z}{2}\right)^2 + \dot{\epsilon}_{rz}^2} \quad (4.1)$$

where $\dot{\epsilon}_r$, $\dot{\epsilon}_z$, and $\dot{\epsilon}_{rz}$ are the incremental radial, axial, and shear components of the strain

rate tensor at each time step. The principal shear strain rate is calculated for each time step and then a running average, $\dot{\gamma}_{avg}$, is calculated. Figures 4.5 (a) to (d) are plots of the instantaneous and average principal shear strain rates from four elements in the shear band at location; (a) near the die radius, (b) 1/4 and (c) 3/4 through the specimen thickness, and (d) near the punch radius. The figure shows that the average rate is smoother and less oscillatory than the instantaneous principal shear strain rate. Failure was then only allowed to occur in regions where $\dot{\gamma}_{avg}$ exceeds a critical value $\dot{\gamma}_{crit}$.

Values for $\dot{\gamma}_{crit}$ were defined as a percentage of the nominal shear strain rate taken from experiments. Simulations were performed using $\dot{\gamma}_{crit}$ equal to 25, 50, 62.5 and 75% of the nominal shear strain rate. Figure 4.6 shows that the element erosion problem near the tooling radii can be reduced by introducing this second condition.

It is also evident from Figures 4.4 and 4.6, that severe distortions to the elements in the die and punch radii can occur. These distortions are primarily caused by the erosion of the elements beneath the die and above the punch which lead to numerical instability in the form of 'chatter' within the contact interface treatment. Experiments show that the die and punch radii remain unaffected during deformation and are therefore these distortions are numerical artifacts.

4.4 Nominal shear stress-shear strain calculation

The experiments do not directly measure events within the shear band but relate the forces on the die and punch faces to the nominal stress and strain within the shear

band. In order to facilitate comparison between numerical and experimental results, the nominal stress and strain were calculated from post processing of the numerical simulations. A FORTRAN program, which can be found in Appendix D, was written that calculates the forces on the die and punch faces from the stresses in the elements along the top and bottom of the die and punch, respectively. In addition to these forces, the program tracks the relative displacements of the punch and die faces. Using these forces and displacements, the Hopkinson bar equations, discussed in Chapter 1, can be used to calculate nominal shear stress versus strain curves from the numerical simulations. These curves can then be compared to the experimental results to assess the validity of the simulations.

Chapter 5

Experimental Results

This Chapter presents results from the compressive split Hopkinson bar experiments in compression and punching-shear. The experiments were carried out prior to the simulations; from these experiments appropriate boundary conditions were derived so that the numerical simulations could be matched to the experimental conditions. The numerical results will be presented in Chapter 6.

The compressive split Hopkinson bar facility, discussed in Chapter 2, was used to test the Armco and Remco irons and heat treated 4340 steels. Armco iron was tested in uniaxial compression and all of the materials were considered in the punch-shear deformation mode.

5.1 Uniaxial Compression Results

Since the CSHB was developed and commissioned as part of this research, validation experiments were performed in uniaxial compression. Armco iron in the as-

received and annealed condition was the only material tested in uniaxial compression. Remco iron in the as-received and shock-hardened conditions could not be tested in uniaxial compression due to a lack of sufficient material. The hardness of the tempered 4340 was very high compared to the bars so this material could not be tested without damaging the bars. Table 5.1 summarizes the uniaxial compression results, where V_s is the measured striker velocity, $\dot{\epsilon}$ is the nominal strain rate, ϵ_t is the true strain, D_1 and T_1 are the specimen diameter and thickness respectively.

All experiments were performed using a striker velocity of approximately 13 m/s, as seen in Table 5.2. Figures 5.1 and 5.2 show the experimental results from the uniaxial compression tests in terms of strain rate versus time and true stress versus true strain curves. As would be expected, the annealed material has a slightly lower yield strength. Figure 5.1 shows initial strain rates of 2100 and 1750 s⁻¹ for the annealed and as-received Armco iron. For the same striker velocity, the strain rates will be lower for a higher strength material and will decrease during the experiment as the flow stress increases, which is reflected in Figure 5.1. The specimens unload elastically after roughly 12-14% strain in the case of the as-received samples and 15-17% strain for the annealed samples. Larger strains were reached in the as-received samples due to the higher strain rate.

The as-received and annealed results show a high degree of work hardening. The greater degree of work hardening in the CSHB results is commonly observed in compression data since tensile necking does not occur. Friction between the specimen and the bars will also lead to a higher degree of work hardening. Comparing the yield strengths of 620 and 480 MPa for the as-received and annealed materials, respectively,

with published data in Figures 3.1 and 3.2 at a similar strain rate and strain, shows that the results are quite similar. The good agreement in yield strength suggests that the Hopkinson bar is functioning properly.

5.2 Shear Results

Punching-shear experiments were performed using the compressive split Hopkinson bar, as described in Chapter 2. Tables 5.2 to 5.6 summarize the results from the punching-shear experiments for the Armco and Remco irons and heat treated 4340 steels.

A typical nominal shear stress-strain curve is shown in Figure 5.3. The stress-strain curve can be broken into four sections, as seen in the figure. In stage 1 there is an initial linear increase in stress with strain to the yield point. After yield, the material will strain harden (stage 2) to a point at which an instability will occur. In an adiabatic shear problem this instability occurs when thermal softening overcomes strain hardening. The onset of the instability is followed by a period in which the material will thermally soften (stage 3), until fracture occurs (stage 4). The figure shows that there is an apparent increase in stress after complete fracture has occurred at a strain of roughly 4.0. This increase in stress is a result of the specimen 'bottoming-out' on the Hopkinson bar. Note that if smaller die plate thicknesses are used, the test will be interrupted at different points along the stress strain curve. This sudden rise in stress serves as an indication of the strain at which the test was interrupted. Figure 5.3 also shows noise occurring at shear

strains of 1.25 and 3.5. This noise is a result of interference from the laser and could possible be eliminated through shielding; however, for the present research the noise was ignored.

5.2.1 Armco Iron

Nominal shear stress-strain curves comparing fine and coarse grain Armco irons are plotted in Figure 5.4. The figure shows that there is not an appreciable increase in strength with decreasing grain size under these high strain rate conditions which is consistent with the observations of Reinders and Kunze [60]. The coarse grain material has an apparent higher ductility than the fine grain material which is the opposite to the expected trend. This discrepancy may be due to differences in shear band widths and will be discussed in more detail in the optical metallography section.

Hardness traverses across the shear bands were performed on the deformed shear specimens using a Leco DM-100 Vickers hardness tester at a load of 25 grams. Figure 5.5 is a typical hardness traverse showing the relative size of the indenter and the shear band. The figure shows that the indenter is smaller than the width of the shear band and therefore several hardness measurement can be made within the shear band. Figures 5.6 (a) and (b) show the hardness traverses across the shear bands for the fine and coarse grain materials, respectively. The base line hardness refers to the hardness level in a region in the centre of the specimen well away from the shear band. Base line hardnesses of 93.5 and 111.4 Hv (500 g) are higher than the undeformed material hardnesses of 82.7 and 77.6 Hv (500 g) for the fine and coarse grain materials, respectively. This increase

in hardness may be attributed to work hardening since the region of the shear specimen between the punch and Hopkinson bar will experience a large compressive load when the punch 'bottoms-out'. The coarse grain material has a larger increase in hardness compared to the fine grain material due to extensive twinning in the coarse grain material. The figure shows that both the fine and coarse grain material reach a peak of approximately 225-250 Hv (25 g) in the shear band. This maximum hardness may represent a limit in the work hardening of the Armco iron.

5.2.2 Remco Iron

Figure 5.7 serves to compare the nominal shear stress-strain curves for fine grain Armco and as-received Remco irons. The figure shows that the Remco iron is slightly softer and more ductile than the Armco iron. Hardness traverses for the as-received Remco iron can be found in Figure 5.8 (a). Comparison of Figures 5.8 (a) and 5.6 (a) shows that the peak and base line hardnesses are quite similar for the fine grain Armco and as-received Remco irons.

5.2.3 Effect of Shock-hardening

The effect of shock-hardening on the shear deformation behaviour of Remco iron can be seen in Figure 5.9, which is a plot of the nominal shear stress-strain curves for as-received and shock-hardened Remco irons. The yield strength nearly doubles due to the shock hardening in agreement with the findings of Weston [62] for uniaxial compressive loading. The shock-hardened Remco iron shows no noticeable work hardening after initial yield which is also consistent with Weston's [63] observations. The hardness

traverses for the as-received and shock-hardened Remco can be found in Figures 5.8 (a) and (b) respectively. Outside of the shear band there is a large increase in hardness, as indicated by base line hardnesses of 94.8 and 224.4 Hv (500 g) for the as-received and shock-hardened Remco irons respectively. However, within the shear band, the shock-hardened Remco iron shows only a slight increase in hardness indicating that the shock hardening effectively exhausts the work hardening potential of the iron. The as-received Remco iron shows a considerable amount of work hardening in the shear band and reaches a maximum hardness similar to that of the shock-hardened Remco iron.

Shock-hardened Armco iron samples were not available for this research. Comparison of the results for the unshocked Armco and Remco irons show a similarity in the nominal shear stress-strain curves and hardness results. These similarities, suggest that shock-hardened Armco iron would behave in a similar manner to that of the shock-hardened Remco iron.

5.2.4 Effect of Temperature

The effect of temperature on the deformation behaviour of the fine grain Armco iron can be seen in Figure 5.10, which is a plot of the nominal shear stress-strain curves at room and elevated (250°C) temperatures. The figure shows a decrease in strength by a factor of approximately 1.5 and an increase in ductility as a result of the increase in temperature. Work hardening appears to progress at a slower rate but continues to larger strains due to the increase in temperature. There does not appear to be as significant a degree of thermal softening during deformation for the elevated compared to room temperature tests. This lower thermal softening may be attributed to the lower

temperature rise experienced by the elevated temperature test since the lower flow stress will result in a smaller amount of plastic work. Comparison of the hardness traverses for the room and elevated temperature tests in Figures 5.6 (a) and 5.11 (a), respectively, shows that there is only a slight decrease in the peak hardness with elevated temperature; however, there is an increase in the base line hardness from 93.5 to 104.8 Hv (500 g). The reason for this increase in hardness with temperature is not known at the present time.

The effect of temperature on the shock-hardened Remco iron can be seen in Figure 5.12, a plot of the nominal shear stress-strain curves at room and elevated temperatures. As in the fine grain Armco iron, the increase in temperature has the effect of lowering the strength and increasing the ductility. The rate of thermal softening is lower for the elevated temperature test, in fact it almost equals with the work hardening rate as reflected by the near zero slope. The hardness traverses for the elevated temperature tests, Figure 5.9 (b), shows a decrease in the base line hardness of 25.9 Hv (500 g) as compared to the room temperature tests, Figure 5.6 (b). The reason for this decrease in hardness may be attributed to recovery of the shock-hardened material during higher temperature testing. The peak hardness appears to be the same for both the room and elevated temperature tests.

Nominal shear stress-strain curves for unshocked Remco iron are also plotted in Figure 5.12. Comparison with the results for the high temperature, shocked material shows that there is an increase in strength and a decrease in ductility associated with shock-hardening; however, the increase in strength is not as dramatic as at room

temperature. This may be due to a loss in strength of the shock-hardened material during testing caused by recovery at the elevated temperature even though the heating times were kept short.

5.2.5 4340 Steel Results

Nominal shear stress-strain curves for the 4340 steel at different tempering temperatures and in the annealed condition are plotted in Figure 5.13. The figure shows that as the tempering temperature increases the strength decreases and a subsequent increase in ductility. Peak shear stresses of 1140 and 860 MPa for the 200 and 450°C tempered conditions compare well with yield stresses of 1070 and 865 MPa reported by Cho, Chi, and Duffy [64] for the same material conditions but tested in the torsional mode. The punching-shear tests show a larger amount of strain at yield compared to the values reported by Cho, Chi, and Duffy [64] which may be related to the differences in deformation modes but more likely to a longer rise times in the punching-shear test. The increase in ductility in the punching-shear test could also be artificial, due to differences in the shear band width and will be discussed in the optical metallography section. Hardness traverses were not performed on the shear bands of the 4340 tempered steel, since the bands are extremely narrow and all specimens fractured making hardness measurements impossible.

5.3 Optical Metallography

Deformed shear specimens were prepared using the standard metallographic technique of sectioning, polishing, and etching, as described in Chapter 3.

5.3.1 Armco Iron

Low magnification photomicrographs of the shear region of the fine grain annealed Armco iron specimens can be found in Figures 5.14 (a), (b), and (c). These photomicrographs show a progression of damage for nominal shear strains of 1.80, 2.21, and 2.53, respectively. A diffuse shear band forms from the beginning of deformation to a nominal shear strain of at least 1.80. Crack initiation occurs at an approximate nominal strain of 2.21. Final fracture occurs at a nominal shear strain of 2.53. This behaviour is reflected in the nominal shear stress-strain curves in Figure 5.4. This figure shows that the stress begins to drop dramatically at a nominal shear strain of 2.0 and that complete fracture occurs at a nominal shear strain of between 2.53 and 2.64, which is in agreement with the metallographic results.

Figure 5.15 shows a photomicrograph of the coarse grain Armco iron deformed to a nominal shear strain of 1.70. Comparing this figure with a fine grain Armco iron specimen with a similar degree of deformation (Figure 5.14 (a)) it is apparent that there is a larger degree of twinning in the coarse grain Armco iron. This observation is in agreement with Zerilli and Armstrong [27], who showed that the stress necessary to cause twinning was inversely proportional to the square root of the grain size, making the coarse grain material more susceptible to twinning. Twinning must have occurred early in the

deformation process since the twin boundaries curve with the shear band.

Figure 5.4 shows that the coarse grain material reaches a higher level of strain of 3.50 before fracture, compared to 2.64 for the fine grain material, indicating that the coarse grains lead to higher ductility. This trend is anomalous since finer grain materials generally exhibit better ductility due to an increased ability to accommodate slip and grain rotation under tensile plastic deformation. The apparent higher shear ductility for the coarse grain material may be only due to the way in which the strain has been calculated and not a true representation of the ductility of the material. The Hopkinson bar equations, discussed in Chapter 1, assume that the shear band width will be equal to the radial clearance between the punch and die; however, this is not necessarily true. Examining Figures 5.14 (a) and 5.15, it appears that the shear band width is greater in the coarse than the fine grain material. Actual measurements of the shear band widths are extremely difficult to obtain, since the location of the edge of the deformation zone is not well defined. In the case of the coarse grain material, the wider shear band will lower the local shear strains thus allowing larger nominal strains to occur prior to fracture.

5.3.2 Remco Iron

Figure 5.16 shows the progression in damage accumulation as the nominal shear strain proceeds from 0.68 to 1.13 to 1.88 in shock-hardened Remco iron. It should be noted that Figure 5.16 (c) and (d) come from opposite sides of the same shear specimen. Fracture has initiated in part of the specimen indicating that this test captured the onset of fracture.

Figure 5.17 shows the shear band for as-received Remco iron at a nominal shear strain of 1.79. Comparing this figure with fine grain Armco iron at a nominal shear strain of 1.80 (Figure 5.14, a), shows a very similar degree of shear localization. This similarity in the deformation process further supports the assumption that Remco iron is a suitable substitute for Armco iron.

5.3.3 Effect of Shock-hardening

The effect of shock-hardening on the shear deformation process in terms of metallography can be seen by comparing Figures 5.14 (a) and 5.16 (c). For a similar nominal shear strain, 1.88 and 1.80 for the shock-hardened and annealed material, respectively, the shock-hardened Remco appears to have a more localized shear deformation. This increase in localization can be related to the degree of hardening, as the degree of work hardening increases so will the width of the shear band. This behaviour implies that high strength, weakly hardening materials such as shock-hardened Remco iron, will be more susceptible to shear band localization.

Higher magnification photomicrographs of the annealed Armco and shock-hardened Remco irons are shown in Figures 5.18 (a) and (b) and 5.5 (a) and (b), respectively. From these figures it can be seen that the grains within the Armco iron shear band are clearly visible whereas the grains within the shock-hardened Remco iron shear band can not be distinguished easily.

Recrystallization is often evidence of adiabatic shear banding, since high temperatures and intense plastic deformation promote recrystallization. Although the

shear band in the shock-hardened Remco iron is an area of intense deformation, there does not appear to be any evidence of recrystallization: likewise there is no evidence of recrystallization in the Armco iron. If recrystallization had occurred there would be small equiaxed grains within the shear band, the absence of such grains leads to the assumption that no recrystallization has occurred. The reason for the absence of recrystallization could be that the temperature at which recrystallization occurs, 450°C for iron, was not reached. Even if such a temperature had been reached, there may not have been sufficient time at the elevated temperature to allow recrystallization to occur.

5.3.4 Effect of Temperature

Low magnification photomicrographs of deformed annealed Armco iron and shock-hardened Remco iron shear specimens tested at elevated temperatures and deformed to nominal shear strains of 1.63 and 1.70 respectively, are shown in Figures 5.19 and 5.20. By comparing Figures 5.14 (a) and 5.19, the effect of increasing the initial temperature on the metallography of Armco iron can be seen. The shear band has become more diffuse with the increase in temperature and therefore the local strains will decrease. Thus, the ductility based on nominal strain will increase, as seen in Figure 5.10. This increase in the width of the shear band is a result of the increase in work hardening at the higher temperature.

The effect of increasing the initial temperature on the shear deformation behaviour of shock-hardened Remco can be seen by comparing Figures 5.16 (c) and 5.20, which have been deformed to nominal shear strains of 1.88 and 1.70 respectively. Again,

increasing the initial temperature has the effect of increasing the width of the shear band. The grains within the shear band are more distinguishable as compared to the room temperature tests. As with the room temperature tests, there does not appear to be any evidence of recrystallization in either the annealed Armco or shock-hardened Remco irons.

5.3.5 4340 Steels

All three of the tempered 4340 steels proved to be susceptible to adiabatic shear banding. Figures 5.21 to 5.24 show low magnification photomicrographs of the annealed, 200, 450, and 650°C tempered 4340 steels respectively. With the exception of the annealed material these figures show that adiabatic shear bands, evidenced by the white etched bands that are typical of adiabatic shear bands, form in each of the 4340 steels. Comparing the low magnification photomicrographs (Figure 5.21 to 5.24) with those for the Armco and Remco irons, Figures 5.14 to 5.20, shows that there is a dramatic increase in the degree of localization in the 4340 steels. The annealed 4340 steel shows some accumulation of plastic strain prior to fracture; however, the quenched and tempered steels do not and the only evidence of the shear bands is the white etched material. The shear band widths for the Armco and Remco irons are on the order of 100 μm as compared to 10 μm or less for the 4340 steels. The small shear band width will also lead to high local strains compared to those from the Hopkinson bar data, as was discussed earlier. This amplification of shear strain may explain the differences between the results from the punching-shear experiments and those of reported by Cho, Chi, and Duffy [64], discussed earlier in Section 5.2.5.

Higher magnification photomicrographs are also given in Figures 5.21 to 5.24. If the width of the shear band is considered to be equal to the width of the white etched bands, then from these figures it appears that the shear band width is decreasing with increasing tempering temperature, which would be contrary to the findings of Wingrove and Wulf [11]. A survey of the 4340 tempered steel specimens indicates that although all of the 4340 tempered steels are susceptible to adiabatic shear failure, shear bands will form more readily in the higher hardness tempers. The width of the shear band also changes from specimen to specimen and therefore the observation of an increase band width with tempering temperature may not be accurate.

5.4 SEM Fractography

The fracture surfaces of a number of specimens were examined using a JSM-6400 scanning electron microscope manufactured by JEOL. The fracture surfaces examined were mounted using the halves of the specimens remaining after sectioning for optical metallography. In all cases only the fracture surface on the 'punched-through' section of the specimen was examined. Prior to mounting, the specimens were cleaned in an ultrasonic alcohol bath to remove any debris.

Figures 5.25 to 5.27 are low and higher magnification fractographs of annealed Armco iron, as-received and shock-hardened Remco irons, respectively. The low magnification fractographs show that the size of the fracture surface decreases as the ductility increases, with as-received Remco iron demonstrating the highest ductility and shock-hardened Remco iron being the least ductile. The higher magnification

fractographs show a dimpled surface, which indicates ductile fracture by microvoid coalescence. The figures show that the dimples are being smeared from left to right in the fractographs, which gives a clear indication of the shear direction. Comparing Figures 5.25 (b) and 5.26 (b) shows that the as-received Remco iron has a much finer dimpled surface than the annealed Armco iron. The finer dimpled surface suggests that the Remco iron had more fracture initiation sites than the Armco iron. The as-received Remco iron has a higher carbon, 0.015% compared to 0.005%, and finer grain size, 22 μm compared to 30-35 μm , than the annealed Armco iron, which could explain the differences in the appearance of the fracture surfaces. Comparison of Figures 5.26 (b) and 5.27 (b) show the effect that shock-hardening has on the fracture surface appearance. From these figures it can be seen that the dimple size appears to be similar, however, there is less shearing in the shock-hardened material. The smaller degree of shearing indicates a less ductile material, which is supported by the optical metallography as well as the stress-strain curves.

Low and higher magnification fractographs for annealed and tempered 4340 steel are shown in Figures 5.28 to 5.31. Figure 5.28 shows the fracture surface of the annealed 4340 steel, which has a dimpled surface indicating ductile fracture by microvoid coalescence. The figure also shows a small degree of smearing from left to right, once again indicating the shear direction. Figure 5.29 shows the fracture surface of the 200°C tempered 4340 steel. From Figures 5.29 (c) and (d), it can be seen that there are two areas on the same fracture surface with different fracture morphologies. Figure 5.29 (c) shows elongated dimples typical of ductile fracture by microvoid coalescence, that are

being smeared in the direction of shear. Figure 5.29 (d) shows a similar ductile fracture surface, however, the dimples are less frequent resulting in a flatter surface. This is evidence of faster fracture, which indicates that crack propagation is occurring at different speeds within the same specimen.

Figure 5.30 shows the fracture surface of the 4340 steel tempered at 450°C. From the figure it can be seen that there are three different fracture morphologies on the same fracture surface. Figures 5.30 (b) and (c) show regions of brittle fracture with chevron marks, indicating the direction of crack propagation. Figure 5.30 (d) shows the area indicated by the arrow in Figure 5.30 (b) at a higher magnification. It should be noted that the brittle regions seen in Figures 5.30 (b), (c), and (d) only represent approximately 12% of the entire fracture surface. Figures 5.30 (e) and (f) show the regions indicated by the arrows in Figure 5.30 (c). These figures show similar morphologies as to those in Figures 5.29 (c) and (d) and therefore indicate difference speeds of crack propagation. It is speculated that after a crack initiates, it propagates at a slower rate in the region in front of these brittle areas and at a faster rate in the adjacent regions. Once the crack has propagated through the entire thickness of the specimen in the adjacent regions, brittle fracture will occur in the remaining portions of the specimen.

Figure 5.31 shows the fracture surface of the 4340 steel tempered at 650°C at low and higher magnifications. Figures 5.31 (c) and (d) are higher magnification of the areas indicated by the arrows in Figure 5.31 (a). These figures show similar fracture morphologies to Figures 5.29 (c) and (d) and therefore indicate different crack propagation speeds. A survey of the fracture surface indicates that the structure seen in

Figure 5.30 (c) is typical of the structure at the top of the specimen, ie. the region near the die plate. The structure in Figure 5.30 (d) is indicative of the structure seen at the bottom of the specimen, ie. the region near the punch.

From the fractographic evidence presented above, it is theorized that fracture initiates at the die plate and propagates through the specimen. The reason for the initiation of fracture near the die plate can be attributed to the geometry of the punching-shear experiment. As the die plate is pushed over the punch, the outer edge of the specimen will begin to rotate slightly downward towards the punch. This rotation will generate tensile and compressive forces in the radial direction near the die and punch radii, respectively. The tensile force near the die radius will initiate fracture.

Chapter 6

Numerical Results

The three materials for which numerical simulations were performed were: annealed and shock-hardened Remco irons and 4340 steel tempered at 650°C. Numerical simulations were undertaken in order to examine local rather than nominal strain and stresses. The use of the program presented in Section 4.4 will allow for comparison of the nominal shear stress-strain curves for the numerical simulations and experiments. These simulations provide a test bed to evaluate the criteria for predicting adiabatic shear band localization. The results of these simulations are presented in this chapter.

6.1 Simulations without Failure

In order to examine the nature of the strain, strain rate and the temperature distribution within the shear band, simulations were first performed in which the failure criterion was disabled.

6.1.1 Annealed Remco Iron

Figures 6.1, 6.2, and 6.3 are fringe plots of the predicted effective plastic strain, average principal strain rate, and temperature, 60 μ s after the start of deformation for Remco iron tested at room temperature. The figures serve to illustrate the sharply focussed deformation (shear) zone and the high temperature reached within this zone. From examination of these figures, it is apparent that the deformation is not uniform across the shear zone and that a considerable strain concentration exists at the specimen surface around the die and punch radii. Based on these elevated values near the radii, it is evident that fracture should initiate at the specimen surface which is supported by the experimental observations.

Elevated temperature simulations, at an initial temperature of 250°C, were also undertaken. Figure 6.4 is a plot of the effective plastic strain for the elevated test with the failure criterion disabled. Comparing Figures 6.1 and 6.4 shows that the effective plastic strain distribution becomes more diffuse as the initial temperature is increased. This increase in width of the shear band with an increase in temperature is supported by the experimental observations as was noted in Section 5.3.4.

The effect that an increase in initial temperature has on the temperature rise within the shear band can be seen in Figure 6.5, which plots the temperature versus time history from an element in the centre of the shear band. Although the final temperature is greater (385 compared to 315°C) for the elevated temperature test, the overall increase in temperature is greater for the room temperature simulation (294 compared to 135°C). The difference in the increase in temperature can be explained by examining equation 1.4,

which shows that an increase in temperature is a function of the amount of plastic work done which in turn depends upon the flow stress. The room temperature simulation exhibits a more localized effective plastic strain and a higher flow stress, both of which will result in a larger temperature rise.

Nominal shear stress-strain curves for the room and elevated temperature simulations are plotted along with the experimental results in Figures 6.6 and 6.7 respectively. These figures show that there is good agreement between the experiments and numerical simulations. Figure 6.6 shows that the numerical and experimental curves begin to diverge noticeably at the point at which an instability occurs, i.e. the maximum in the shear stress-strain curve, which occurs at a shear strain of roughly 1.0. The divergence of the numerical and experimental results is to be expected since the failure criterion was disabled and therefore failure in the shear band after the instability could not be predicted. Figure 6.7 shows that at elevated temperatures, the divergence of the numerical and experimental results does not occur until after a shear strain of roughly 2.35 is reached. At elevated temperatures the strain hardening and thermal softening are nearly equal and therefore the point at which the instability occurs is difficult to discern.

6.1.2 Shock-hardened Remco Iron

As with the annealed material, simulations were performed for the shock-hardened Remco iron with the failure criterion disabled so that deformation within the shear band could be examined. Figures 6.8 and 6.9 are fringe plots of the effective plastic strain, 60 μ s after deformation begins for the room and elevated temperature tests.

Comparison of Figures 6.1 and 6.8 reveals the effect of shock-hardening on the evolution of effective plastic strain. The shock-hardened material, which exhibits negligible strain hardening, shows a much higher degree of localization in comparison to the annealed material. The effect of increasing the initial temperature on the strain distribution can be seen by comparing Figures 6.8 and 6.9 for the shock-hardened Remco iron. An increase in the initial temperature results in a considerably more diffuse strain distribution within the shear band.

One interesting effect of shock-hardening is to promote a more rapid temperature rise within the shear band. Figure 6.10 is a plot of the predicted temperature versus time history for the annealed and shock-hardened materials. The shock-hardened Remco iron will experience a larger temperature rise since the flow stress is higher and the effective plastic strain is more localized, both of which lead to greater plastic work. The effect of an increase in initial temperature on the temperature rise within the shear band of shock-hardened Remco iron can be seen in Figure 6.11. The figure shows that temperature rise in the room temperature simulation is much larger than for the elevated temperature. The reason for the greater temperature rise can be attributed to the higher flow stress and strain localization. Figure 6.12 shows the effect that shock-hardening has on the

temperature rise for elevated initial temperatures. From the figure, it can be seen that shock-hardening only slightly increases the overall temperature within the shear band.

Figures 6.13 and 6.14 show the room and elevated temperature plots, respectively, of the predicted and measured nominal shear stress-strain curves for the shock-hardened Remco iron. Figure 6.13 shows that the numerical solution slightly under predicts yield and shows a small degree of strain hardening that was not seen in the experiment. The simulation shows a decrease in shear stress at a shear strain value of approximately 1.1. This drop in stress is a result of the highly localized deformation within the shear band. The simulations and experiments show good agreement for the elevated temperature results, as seen in Figure 6.14.

6.1.3 4340 Quenched and Tempered Steel

Simulations were performed with the failure criterion disabled for the 4340 steel tempered at 650°C in order to examine the nature of the strain, strain rate, and temperature within the shear zone. Figures 6.15, 6.16, and 6.17 are fringe plots of the effective plastic strain, average principal strain rate, and temperature, 35 μ s after the beginning of deformation. These are plotted for an earlier time in the deformation process, as compared to the Remco irons, because the 4340 tempered steel is less ductile. Figures 6.15 and 6.16 show that deformation becomes focussed within the shear band and that there is a concentration of deformation around the die and punch radii. Unlike the results for the softer irons, there is a significant amount of deformation in the die and punch radii.

Figure 6.18 is a plot of temperature versus time for three elements in the shear band at locations (a) near the die, (b) in the centre and (c) near the punch. The plot shows that the temperature rises to nearly 1000°C near the die and punch radii, but to only 700°C in the middle of the shear band. The much greater temperature rise compared to the softer irons is a reflection of the higher flow stress of the 4340 quenched and tempered steel.

6.2 Maximum Shear Stress Failure Criterion

Numerical simulations using the maximum shear stress (MSS) criterion for predicting failure in annealed and shock-hardened Remco iron, and 4340 tempered steel are presented below.

6.2.1 Annealed Remco Iron

The effect of the temperature rise in promoting thermal softening is shown in Figure 6.19 which plots contours of $d\sigma/d\varepsilon|_{d\varepsilon=0}$ calculated using equation (1.5), 50 μ s after deformation begins. The figure shows the onset of thermal softening as seen by $d\sigma/d\varepsilon|_{d\varepsilon=0}$ becoming negative within the shear band. Figure 6.20 shows the results of the numerical simulations in which the MSS criterion, equation (1.5), was used. The plot sequence in Figures 6.20 (a)-(d) shows how the propagation of a crack across the shear band can be captured, at least qualitatively. From these figures significant erosion of the elements near the profile radii, as discussed in Section 4.3, can be seen. These elements

fail since they accumulate significant plastic work and begin to thermally soften. The shear strain rates are low in this vicinity indicating that the onset of thermal softening alone should not of itself be a sufficient failure criterion.

The nominal shear stress-strain curve from the calculations using the MSS criterion is plotted in Figure 6.21 along with an experimental shear stress-strain curve. The figure shows that the numerical simulation only slightly under predicts the strain at onset of the instability, however, severely under predicts the strain at complete failure. From the figure, complete fracture is predicted by the simulation to occur at a shear strain of 2.2, whereas the experiments show that fracture does not occur until a shear strain of roughly 3.0.

Figure 6.22 is a plot of the nominal shear stress-strain curve for the simulations performed using the MSS criterion with the addition critical strain rate requirement. Critical strain rates ($\dot{\gamma}_{crit}$) of approximately 25, 50, 62.5, and 75% of the nominal experimental shear strain rate were considered, which corresponds to $\dot{\gamma}_{crit}$ equal to 12,500, 22,500, 28,125, and 33,750 s^{-1} , respectively. Increased values of $\dot{\gamma}_{crit}$ are seen to increase the predicted localization and failure strains to values closer to the experimental results. Figures 6.23 to 6.26 show that elevated values of $\dot{\gamma}_{crit}$ greatly reduce the extent of erosion of elements directly in contact with the die and punch but not located within the shear band. The erosion of elements is not significantly improved between $\dot{\gamma}_{crit}$ values of 12,500 and 22,500 s^{-1} ; however, there is a significant improvement between 22,500 and 28,125 s^{-1} . Beyond this range for $\dot{\gamma}_{crit}$ there does not appear to be further improvement. The nominal shear stress-strain curves, Figure 6.22, show that the simulation performed with

$\dot{\gamma}_{\text{crit}}=28,125 \text{ s}^{-1}$ or 62.5 % of the nominal experimental strain rate gives the closest agreement with the experiments. Consequently $\dot{\gamma}_{\text{crit}}/\dot{\gamma}_{\text{nom}}=62.5\%$ was adopted for the remainder of the simulations employing the maximum shear stress criterion with $\dot{\gamma}_{\text{crit}}$. Note, the actual value of $\dot{\gamma}_{\text{crit}}$ varies between different materials due to differences in the nominal shear strain rates.

Figure 6.27 is a nominal shear stress-strain curve for the elevated temperature tests using the MSS criterion with $\dot{\gamma}_{\text{crit}}/\dot{\gamma}_{\text{nom}}=62.5\%$. The onset of the instability occurs at a strain of approximately 1.7. The onset of the instability is difficult to determine in the experimental curve since there is no sharp transition; however, it does seem to occur in this vicinity of strain. After the failure has initiated in the model, the stress drops at a slightly greater rate than seen in the experiment. Ultimate failure in the experiment occurs at a shear strain of 3.6, the numerical simulation somewhat over-predicts failure at a shear strain of 3.8.

6.2.2 Shock-hardened Remco Iron

Figure 6.28 plots predicted contours of $d\sigma/d\varepsilon|_{d\varepsilon=0}$ calculated using equation (1.5) for shock-hardened Remco iron tested at room temperature, 19 μs after deformation begins. The figure shows the onset of thermal softening as seen by $d\sigma/d\varepsilon|_{d\varepsilon=0}$ becoming negative, which occurs rapidly in the shock-hardened material. The nominal shear stress-strain curve from this model is compared to the measured data in Figure 6.29. The figure shows that failure is predicted at a much lower strain than was observed in the experiments.

The reason for the inability of the MSS criterion to accurately predict failure in the shock-hardened Remco can be attributed to (i) the shape of the stress-strain curve given by the Goldthorpe-modified Zerilli-Armstrong constitutive equation and (ii) the erosion of elements away from the shear band. The stress-strain response of shock-hardened Remco iron, will have an initially high yield followed by a steady decrease in stress, as seen in Figure 3.7. This shape in a stress strain curve means that the maximum shear stress criterion will predict failure as soon as the yield stress is reached and therefore the MSS criterion will not be able to adequately predict failure due to adiabatic shear banding. Figure 6.30 shows that elements far away from the shear zone are failing in addition to elements under the die and punch; in fact, no element remains that has any noticeable amount of plastic deformation. From Figure 6.30 it can be seen that failure is not predicted in the shear band but in the specimen away from the shear band which is physically unreasonable.

Figure 6.29 also plots the predicted nominal shear-stress strain curve using the MSS criterion with $\dot{\gamma}_{crit}=20,000 \text{ s}^{-1}$. The implementation of $\dot{\gamma}_{crit}$ serves to concentrate failure to elements within the shear band. Unfortunately, the figure shows that the instability is predicted to occur at a shear strain of approximately 0.76, whereas the experiment shows that the instability actually occurs at a shear strain of 0.26. After the onset of the instability, the numerical simulation shows a rapid progression of failure with complete failure occurring at a shear strain of 1.31, which is only 57% of the measure value of 2.30.

The predicted nominal shear stress-strain curve for the case of an elevated initial

temperature can be found in Figure 6.31. These calculations employed the MSS criterion with $\dot{\gamma}_{crit}=25,000 \text{ s}^{-1}$. The figure shows that the numerical model predicts the onset of the instability to occur at a shear strain of 0.65. From the figure it is difficult to determine where the onset of the instability occurs in the experiment. In the room temperature test, the instability occurred at yield, however, the elevated temperature test seems to indicate a small amount of work hardening before thermal softening occurs. Even once thermal softening has overcome work hardening, the decrease in stress with strain is small and the curve is almost horizontal until fracture is initiated. After the onset of the instability, the predicted stress is somewhat lower than was measured. Complete failure was not predicted by the model. Failure did not occur in the experiment, since large enough die thicknesses were not used, however, failure is expected to occur shortly after a shear strain of 2.6.

6.2.3 4340 Quenched and Tempered Steel

Figure 6.32 is a contour plot of $d\sigma/d\epsilon|_{d\epsilon=0}$ for the 4340 steel quenched and tempered at 650°C, 49 μs after deformation begins and shows the onset of thermal softening as seen by $d\sigma/d\epsilon|_{d\epsilon=0}$ becoming negative within the shear band. A numerical simulation was performed using the MSS criterion and the results are shown in Figure 6.33, which shows the propagation of a crack across the shear band. The figure show that there is not an erosion problem as there was with the annealed and shock-hardened Remco irons. The figure also shows that after the onset of the instability, propagation of the crack across the entire specimen occur within a 5 μs .

Figure 6.34 shows a plot of the nominal shear-stress strain curves for the numerical simulation using the MSS criterion and an experimental test. The figure shows that the numerical model slightly over predicts the onset of the instability and that the rate of damage accumulation is higher in the model than experiment. The model predicts that complete failure will occur at a shear strain of approximately 1.15, whereas the experiments show that failure does not occur until a shear strain of roughly 1.40.

The nominal shear stress-strain curve for the simulations using the MSS criterion with $\dot{\gamma}_{\text{crit}} = 20,625 \text{ s}^{-1}$ is also shown in Figure 6.34. The figure shows that the initiation of failure is over predicted and occurs at a shear strain of 1.03, whereas the experiments show that failure initiates at a shear strain of 0.85. After the initiation of failure the rate of damage accumulation is slower in this model than in the model using only the MSS criterion. At a shear strain of 1.25, rapid failure occurs in the model and complete failure occurs at 1.43, which is close to the experimental value of 1.40.

6.3 Flow Localization Criterion

Numerical simulations were performed using the flow localization (FL) failure criterion, discussed in Section 1.2.2, for annealed and shock-hardened Remco irons, and 4340 tempered steel. The results of these simulations are presented below.

6.3.1 4340 Quenched and Tempered Steel

A contour plot of the flow parameter, α , can be seen in Figure 6.35, 54 μ s after the beginning of deformation and shows that α becomes large within the shear band. As stated earlier Semiatin et al. [16] observed that failure would occur through adiabatic shear banding if α reaches a value between 3 and 4, and therefore the model predicts that the 4340 quenched and tempered steel will fail due to adiabatic shear localization under the experimental conditions. Experimental observations indicated that failure did indeed occur by adiabatic shear banding and therefore the model was successful in predicting adiabatic shear failure in the 4340 tempered steel.

Figure 6.36 is a plot of the nominal shear stress-strain curves for the numerical simulations using the flow localization model with α values of 3.0 and 4.0, along with the experiment results. The figure shows that the flow localization criterion over predicts the shear strain at which the onset of the instability will occur. However, the model does predict that shear strain at failure and the maximum stress at which the instability occurs. Changing the α parameter from 4 to 3 moves the point at which the onset of the stability is predicted to lower strain values, but does not significantly alter the prediction of failure.

6.3.2 Annealed Remco Iron

Figure 6.37 is a fringe plot of the flow localization parameter, α , calculated from equation (1.24) for annealed Remco tested at room temperature. The figure shows that α becomes largest within the shear band and reaches a value of just above zero. In order

to use the flow localization criterion as an effective way of predicting adiabatic shear banding, an α parameter at which catastrophic shear failure occurs in annealed Remco needs to be established. Failure through adiabatic shear banding was not observed under experimental conditions, and therefore it is assumed that the value for α at which adiabatic shear banding will occur is larger than the value of zero predicted by the model.

6.3.3 Shock-hardened Remco Iron

Figure 6.38 is a fringe plot of the flow localization parameter, α , 24 μ s after deformation has started. The figure shows that α becomes large within the shear band. Semiatin et al. [16] had observed that an α value of between 3 and 4 was necessary for adiabatic shear failure, and therefore for the purposes of this study it will be assumed that shock-hardened Remco iron would be susceptible to failure due to adiabatic shear banding at similar values of α .

Figure 6.39 is a plot of the nominal shear stress-strain curve from simulations using the flow localization model with two different α values. This model seems to predict the onset of the instability correctly, however, the deformation behaviour of the material after this point is not predicted well. The flow localization model predicts complete failure at a strain of 0.49 and 0.62 for $\alpha=4.0$ and $\alpha=4.5$ respectively. The experiments showed that failure does not occur until a shear strain of roughly 2.0. A reason for the inability of the flow localization to predict failure may once again be attributed to the decreasing slope of the stress-strain curve after yield. The model assumes that once an instability occurs, catastrophic failure quickly ensues, which is not

what the measured stress-strain curve shows for the shock-hardened material.

The experiments gave no evidence of the formation of adiabatic shear bands and therefore the model over predicts the susceptibility of the shock-hardened Remco to adiabatic shear banding. It is possible that the α parameter in shock-hardened Remco iron is higher than that of the tempered steel and therefore to make better use of the flow localization model, further experiments are required establish an appropriate value of α .

Chapter 7

Discussion

The results of this research will be discussed in three different sections: experimental apparatus; experimental results; and numerical predictions.

7.1 Experimental Apparatus

One of the main objectives of the present research was to design and construct a compressive split Hopkinson bar for high strain rate testing in compression or punching-shear. As indicated in Chapter 5, the CSHB has been shown to operate properly. Several areas of concern exist and should be addressed in future work. These include: the strength of the incident and transmitter bars; the accuracy of the velocity detector; the strain gauge calibration; and techniques for elevated temperature testing. Each of these will be discussed in this section.

As mentioned in Chapter 2, the incident and transmitter bars are made of drill rod. The yield strength of the drill rod is approximately 420 MPa, which limits the striker

velocity to 21 m/s since the propagating stress wave in the incident bar must remain elastic. As Table 5.7 indicates, this upper limit on striker velocity was approached but not passed in several experiments and therefore the CSHB is been pushed to the current upper limit of striker velocity. This limit on the striker velocity also imposes a limit on the strain rate at which materials can be deformed. As noted in Chapter 5, uniaxial compression testing of the 4340 tempered steels was not performed due to the hardness mismatch between the Hopkinson bars and the test material. In order to test materials at higher strain rates and higher hardnesses, Hopkinson bars with a higher yield strength and hardness must be fabricated. Maraging steels, which have yield stresses as high as 2400 MPa, are an attractive replacement; however, a source has not yet been found that can supply the required straightness, roundness, and smooth finish. This approach would allow detection of velocity and deceleration of the striker.

The velocity detector that is currently being used, as described in Chapter 2, utilizes only two beams and therefore gives no indication as to the rate at which the striker bar is decelerating before impact. The last beam in the detector is 70 mm (3") away from the impact site and therefore the measured and actual velocity may be different depending on the at which the striker bar decelerating. To give a more accurate measurement of the velocity at impact, a detector using three or more beams would be desirable.

A discrepancy between the measured and calculated velocities was noted in Section 5.3. This difference may be due to an incorrect measured velocity and therefore changing to a three or more beam velocity detector may give a better correlation between

measured and calculated velocities. This difference between velocities is more likely related to problems with the strain gauge calibration as noted in Section 2.3.3. The semiconductor gauges that are used for this research are distinctly non-linear [52], however, if calibration is performed in the range of testing the effect of the non-linearity of these gauges will be minimized. During static calibration of the gauges, loading was limited to a stress of 70 MPa to avoid buckling of the bars, whereas a typical punching-shear test has an incident pulse of 300 MPa. To alleviate this limitation, an improved velocity detector (above) would allow accurate dynamic calibration to be undertaken. Alternatively, changing from semi-conductor to foil gauges would reduce the need for calibration since foil gauges have a more repeatable, more linear, and less rate sensitive gauge factor. It should be reiterated that the difference between the measured and calculated velocity is small and is not expected to significantly effect the accuracy of results and general conclusions of this research.

The elevated temperature testing outlined in Chapter 2 worked reasonably well for the given temperature range. The capacity of the heat guns used for the present research limit the testing to temperatures below 300°C. To perform testing at higher temperatures an alternate set up is required. A radiant furnace would be an ideal choice as a replacement since rapid heating could be achieved and shielding of the bars would allow for testing with the bars in place, eliminating the need for a specimen holder.

7.2 Experiments

Uniaxial compression testing was undertaken with the primary purpose of validating the CSHB. These tests served to show that the CSHB was operating properly since the results obtained were consistent with the findings of other researchers. The results did show that there was a high degree of work hardening, which was partially attributed to friction between the Hopkinson bars and the compression specimen. The frictional effect on the measured stress can be reduced with improved lubrication between the bars and specimen. Compression testing was limited to materials with lower hardness than the Hopkinson bars; replacing the bars with a higher strength and hardness material would allow compression testing to be expanded.

An area of concern with the punching-shear experiment is the punch and die radii. Examination of the punch and die radii after testing showed that the radii were not consistent from test to test. This variation raises some concerns since changes in the radii of the punch and die may lead to different deformation behaviour and failure initiation. For the present research, the radii were machined into the punch and die using a cutting tool on the lathe and after which the punch and die are heat treated to the desired hardness. During the machining, inconsistencies in the radii are introduced due to machinist error and tool wear. Accuracy of the punch and die radii may be improved by switching to an electrical discharge machining (EDM) process. The punch and die could be machined to rough tolerances, heat treated, and then fine machined using EDM. This approach would produce punch and die sets with more consistent geometries; however,

the machining costs would be significantly higher.

Another problem associated with the current punching-shear specimen design is the width of the shear band. The current design assumes that the width of the shear band will be equal to the radial clearance between the punch and die. The radial clearance will vary from test to test due to machining error and may effect the degree of localization; however, this problem may be reduced by changing to an EDM fabrication process. Of more concern is the difference between the assumed and actual shear band widths, since experiments show that the shear band is not necessarily equal to the radial clearance. As discussed in Chapter 5, discrepancies between the actual shear band width and the radial clearance will lead to either under- or over-estimating the amount of shear strain. This error means that the shear stress-strain curves do not necessarily give an accurate picture of the degree of localization within the shear band. In order to give a more accurate picture of the deformation behaviour within the shear band, a method for measuring the shear band width from metallographic specimens needs to be developed. If the shear band width could be measured then this information could be used to calculate the shear strain from the experimental data and give a more accurate description of shear strain in the shear stress-strain curves.

The initial emphasis of the present research was on adiabatic shear band formation in Armco iron; however, Armco iron in the shock-hardened condition was not available, which lead to the use of Remco iron in the present research. Armco and Remco irons are both high purity irons with only slight differences in chemical composition. The punching-shear experiments demonstrated that Armco and Remco irons exhibit a similar

shear stress-strain behaviour. Metallographic results showed that the Armco and Remco irons deform in a similar manner and with a comparable degree of localization. These results lead to the conclusion that the effects of shock-hardening Remco and Armco irons will be similar.

Shock-hardening has the effect of increasing the strength of the material by nearly two-fold. In addition to strength, the hardness increases to levels consistent with extreme work hardening. When deformed in shear, the shock-hardened material shows virtually no work hardening after initial yield. The absence of work hardening can be attributed to the work hardening potential being exhausted during shock-hardening. The punching-shear experiment showed that the shock-hardened material forms a more localized shear band than the as-received material. No evidence of recrystallization was found in either the as-received or shock-hardened Remco irons and therefore it is concluded that Remco iron is not susceptible to adiabatic shear banding of the transformed type under the present loading conditions.

Increasing the initial temperature of the specimens to 250°C had a similar effect in the annealed Armco and shock-hardened Remco irons. The elevated temperature reduced the flow stress and increased the ductility in both materials. The increase in temperature reduced the rate of work hardening but extended it to larger strains in annealed Armco iron. The baseline hardness in the shock-hardened Remco iron dropped due to recovery during testing. The degree of localization was reduced as the testing temperature increased and therefore higher strains were reached before ultimate failure occurred.

The punching-shear experiments for the 4340 quenched and tempered steels showed that as the tempering temperature increased, the strength decreased with a corresponding increase in hardening rate and resultant ductility. The results showed that higher strains were reached in the punching-shear experiments than those reported by Cho, Chi, and Duffy [64]. This discrepancy in the amount of strain may be a result of the difference between the actual and assumed shear band widths.

The transformation type of adiabatic shear banding was not observed in annealed Armco, as-received nor shock-hardened Remco irons. Transformation bands have been observed in explosively formed projectiles (EFP's) formed from Armco iron [69] and therefore under certain circumstances Armco iron is susceptible to failure through adiabatic shear banding. The thermo-mechanical history of an EFP is extremely complex and therefore reproducing structures found in EFP's is very difficult. In order to gain a better understanding of adiabatic shear banding, a technique for producing these bands under experimental conditions needs to be found.

7.3 Numerical Simulations

The numerical simulations followed the general trends found in the experiments in terms of the effect of shock-hardening and initial temperature. The models showed that the effect of shock-hardening was to increase the degree of localization as seen in the experiments. Increasing the initial temperature had the effect of decreasing the degree of localization for both the annealed and shock-hardened Remco irons. The shock-

hardened Remco iron demonstrated a larger temperature rise than the annealed Remco iron. The difference in temperature rise can be attributed to the higher flow stress and plastic work associated with the shock-hardened Remco iron.

The numerical simulations with failure disabled gave good agreement with the experimental data in terms of the nominal shear stress-strain curves for the annealed Remco iron. These results showed that the model only slightly over predicts the shear stress up to the point at which the instability occurs. At the point of the instability, the two curves begin to diverge, which is not surprising since these particular calculations did not incorporate failure. The model without failure for the shock-hardened Remco iron only slightly under predicted the peak in shear stress, but did so at a higher shear strain. The model predicted a small degree of work hardening which was not seen in the experimental results.

The results for the numerical simulations for which the failure has been disabled for the 650°C tempered 4340 steel provided good agreement with the measured stress-strain results. The model predicts a temperature of approximately 100°C 35 μ s after deformation begins in the most severely deformed elements. The metallography showed that there was a narrow band of white etched material along the fracture path, which indicates that a phase transformation into austenite followed by rapid cooling into martensite has occurred. When 4340 steel is rapidly heated, it will begin to transform to austenite at 720°C (A_{c1}) and will completely transform to austenite at 750°C (A_{c3}). Thus the numerical model severely under predicts the temperature rise. The failure of the model to predict the temperature rise may be related to the size difference between the

elements and the white etched band. The band is approximately 10 μm wide whereas the elements in the shear zone are 25 μm wide. A smaller element size may produce a more localized shear deformation and lead to a higher temperature rise in the model to a value approaching the transformation temperature of 4340 steel. This narrow band also supports the use of a localization model since deformation did collapse into a narrow band for these higher strength materials.

The maximum shear stress (MSS) criterion was used to predict failure in all three of the materials considered in the numerical modelling. The model for annealed Remco iron using the MSS criterion was successful in predicting the onset of the instability; however, the subsequent deformation was not predicted well and failure was severely under predicts. The MSS criterion with $\dot{\gamma}_{\text{crit}}$ reduced the erosion problem and extended failure to higher strains. The model for the shock-hardened Remco iron using the MSS criterion did not predict failure well, which was attributed to the shape of the stress-strain curve. The MSS criterion only slightly under predicted failure in the 650°C tempered 4340 steel; however, the rate of damage accumulation was much higher in the numerical model than experimental testing. In general, the MSS criterion proved to be able to capture the onset of the instability; however, deformation after the onset of the instability was not described very well. The inability of the MSS criterion to adequately describe damage accumulation is due to what is assumed by the criterion. The criterion assumes that complete failure will occur as soon as an instability occurs, which is not what is seen in the experiments. For this reason the MSS criterion will not adequately predict deformation after the instability, but may be used to predict the onset of the instability

and then a second criterion could be used to predict damage accumulation.

The flow localization failure model was used to predict failure in only the shock-hardened Remco iron and 4340 steel. The shock-hardened Remco iron model with α set to 4.0 and 4.5 captured the onset of the instability fairly accurately, however, showed a much more rapid accumulation of damage, resulting in lower predicted failure strain. Modelling 650°C tempered 4340 steel with α set to 3.0 and 4.0 showed that the onset of the instability and the rate of damage accumulation is over predicted, but complete failure is predicted at a shear strain near to the experimental value. Failure in the 4340 tempered steel is via adiabatic shear banding and therefore the localization criterion provided a good prediction of failure.

One area of interest that has not been addressed by the present research program is mesh sensitivity. It is expected that these calculations will be strongly mesh dependent, and therefore any future work should address this issue. To maintain efficiency, consideration should be given to using adaptive re-meshing algorithms that are triggered by the constitutive model to insert fracture interfaces and to refine the surrounding mesh. This research work is currently underway at Carleton.

Chapter 8

Conclusions and Recommendations

The objectives of the current research program were to (i) develop a compressive split Hopkinson bar (CSHB) apparatus for the study of adiabatic shear localization and (ii) investigate adiabatic shear banding in a series of ferrous materials displaying a range of initial hardnesses. To this end, the following contributions were made:

- The design and commissioning of a compressive split Hopkinson bar (CSHB) facility for testing materials at high strain rates in compression and shear.
- The application of the CSHB facility to investigate adiabatic shear banding in several ferrous materials.
- Numerical modelling of adiabatic shear localization and evaluation of the (i) maximum shear stress and (ii) flow localization failure criteria for several ferrous materials.

8.1 Conclusions

The following are the major conclusions made from the present research program:

- The CSHB facility is functioning properly and can be used to test materials at high strain rates in either uniaxial compression or punching-shear modes at temperatures ranging from room to 250 °C.
- Under the shear strain rates and temperatures investigated, adiabatic shear localization will not occur in annealed Armco, as-received nor shock-hardened Remco irons. Adiabatic shear localization occurred in 4340 steel for all the tempers tested.
- The Goldthorpe-modified Zerilli-Armstrong constitutive equation with the current parameters gave good agreement between the experiments and numerical simulations.
- Using either the (i) maximum shear stress, (ii) maximum shear stress with a critical strain rate, or (iii) flow localization criteria, the onset of an instability is predicted well; however, material behaviour after the instability is not predicted with a high degree of accuracy.

8.2 Recommendations

The following recommendations are made based on the findings of the present research program:

- Replace the current incident and transmitter bars with higher strength and hardness materials so that higher strain rates can be reached and higher hardness materials can be tested.
- To improve accuracy of the velocity measurement, the current two-beam detector should be replaced with a three or more beam detector.
- Replace the semi-conductor strain gauges used at present with foil strain gauges and high speed amplifiers.
- Use a radiant heater in place of the heat guns currently being used. This desire will provide faster heating of the specimen, better control of the test temperature, and will increase the testing temperature range.
- A method needs to be developed to fabricate the punch and die radii with more accuracy and consistency. Switching to an electrical discharge machining process may provide necessary improvements.
- A method of measuring the width of the shear band in a deformed specimen needs to be developed, to allow for a more accurate calculation of local strains and strain rates from the experiments.
- A method for producing adiabatic shear localization in annealed Armco iron needs

to be developed so that the failure criteria presented in this research can be evaluated more rigorously.

- The present adiabatic shear model should be combined with a ductile fracture criterion to encompass the range of failure modes in ductile materials.
- Future research should consider remeshing the area of failure and to allow insertion of a crack surface which should lead to improved numerical solutions.

References

- [1] Worswick, M.J., Qiang, N., Niessen, P., and Pick, R.J., *Microstructure and Fracture During High Rate Forming of Iron and Tantalum in Shock-Wave and High-Strain-Rate Phenomena in Materials*, Proc. EXPLOMET '90, MDI, NY, NY 1992, Pg 87.
- [2] Worswick, M.J. and Pick, R.J., *Void Growth and Coalescence during High Velocity Impact*, Mechanics of Materials, 19, 1995, Pg. 293-309.
- [3] Worswick, M.J., Nahme, H., Clarke, J., and Fowler, J., *Ductile Fracture at High Rates of Strain*, DYMAT Journal, 1, (3), September, 1994, Pg 1-16.
- [4] Worswick, M.J., Pelletier, P., Fowler, J, Gallagher, P.J., and Weickert, C.A., *Ductile Fracture During High Rate Forming of Armco Iron*, EXPLOMET '95, ed. Murr, L.E., Standhammer, K.P., and Meyers, M.A., Elsevier Science Publishers B.V., El Paso, Texas, August 6-10, 1995, Pg. 227-234.
- [5] Timothy, S.P., *The Structure of Adiabatic Shear Bands in Metals: A Critical Review*. Acta Metall., 35, 1987, Pg. 301-306.

- [6] Staker, M.A., *The Relationship Between Adiabatic Shear Instability Strain and Material Properties*, Acta Metall., 29, 1981, Pg. 683-689.
- [7] Rogers, H.C., *Adiabatic Shearing - General Nature and Material Aspects* in *Material Behaviour under High Stress and Ultrahigh Loading Rates*, ed. Mascull, J. and Weiss, U., 1983, Pg. 101-118.
- [8] Zener, C. and Hollomon, J.H., *Effect of Strain Rate Upon Plastic Flow of Steel*, J. Appl. Phys., vol. 15, 1944, Pg. 22-32.
- [9] Bai, Y.L. and Johnson, W., *Plugging: Physical Understanding and Energy Absorption*, Metals Technology, 9, 1982, Pg. 182-190.
- [10] Wingrove, A.L., *The Influence of Projectile Geometry on Adiabatic Shear and Target Failure*, Metallurgical Transactions, Vol. 4, 1973, Pg. 1829-1833.
- [11] Wingrove, A.L. and Wolf, G.L., *Some Aspects of Target and Projectile Properties on Penetration Behaviour*, Aust. Inst. Metals, 18, 1973, Pg. 167-172.
- [12] Rogers, H.C. and Shastry, C.V., *Material Factors in Adiabatic Shearing in Steels* in *Shock-Waves and High-Strain-Rate Phenomena in Metals*, ed. Murr, L.E. and Meyers, M.A., Plenum Press, New York, 1981, Pg. 285-297.
- [13] Derop, J.L., *Microstructure Transformation Induced by Adiabatic Shearing in Armour Steel*, Acta Metall., 31, 1987, Pg. 1245-1249.

- [14] Giovanola, J.H., *Adiabatic Shear Banding Under Pure Shear Loading - Part I: Direct Observation of Strain Localization and Energy Dissipation Measurements*, Mech. of Materials, 7, 1988, Pg. 59-71.
- [15] Giovanola, J.H., *Adiabatic Shear Banding Under Pure Shear Loading - Part II: Fractographic and Metallographic Observations*, Mech. of Materials, 7, 1988, Pg. 73-87.
- [16] Semiatin, S.L., Staker, M.R., and Jonas, J.J., *Plastic Instability and Flow Localization in Shear at High Rates of Deformation*, Acta Metall., 32, 1984, Pg. 1347-1354.
- [17] Nicholas, T. and Rajendran, A.M., *Material Characterization at High Rates*, Chapter 3 of *High Velocity Impact Dynamics*, Zukas, J.A. ed., Wiley Interscience, New York, Hy, 1990.
- [18] Wulf, G.L., *The High Strain Rate Compression of 1023 and 4130 Steels*, Int. J. Mech. Sci., 20, 1978, Pg. 843-848.
- [19] Jonas, J.J., Holt, J., and Coleman, C.E., *Plastic Stability in Tension and Compression*, Acta metall., 24, 1976, Pg. 911-918.
- [20] Semiatin, S.L., Lahoti, G.D., and Oh, S.I., *The Occurrence of Shear Bands in Metalworking in Material Behaviour under High Stress and Ultrahigh Loading Rates*, Plenum Press, New York, 1983, Pg. 119-159.

- [21] Semiatin, S.L. and Jonas, J.J., *Formability and Workability of Metals: Plastic Instability and Flow Localization*, Am. Soc. Metals, Metals Park, OH, 1984, Pg.
- [22] Ning, Q., *Numerical and Experimental Simulation of Adiabatic Shear Localization in Tantalum and Armco Iron*, Ph.D. Thesis, University of Waterloo, 1993.
- [23] Zukas, J.A., *Stress Waves in Solids*, Chapter 1 of *Impact Dynamics* by Zukas, J.A., Nicholas, T., Swift, F.H., Greszczuk, L.B., and Curran, D.R., John Wiley & Sons, Inc., New York, 1982.
- [24] Johnson, G.R., and Cook, W.H., *A Constitutive Model and Data for Metals Subjected to Large Strains, High Strain Rates and High Temperatures*, in ADPA 7th International Symposium of Ballistics Proceedings, The Hague, NL, 1983, Pg. 541.547.
- [25] Zerilli, F.J., and Armstrong, R.W., *Dislocation-Mechanics-Based Constitutive Relations for Materials Dynamics Calculations*, J. Appl. Phys., Vol. 61, No. 5, 1987, Pg. 1816-1825.
- [26] Armstrong, R.W., *The Yield and Flow Stress Dependence on Polycrystal Grain Size* Chapter 1 of *Yield, Flow and Fracture of Polycrystals*, edited by T.N. Baker (Applied Science, London, 1983.

- [27] Zerilli, F.J. and Armstrong, R.W., *Dislocation-Mechanics-Based Constitutive Relations for Material Dynamics Modelling: Slip and Deformation Twinning in Iron*, in Shock Waves in Condensed Matter 1987 Proceedings, Elsevier Science Publishers B.V., Amsterdam, NL, 1988, Pg. 273-276.
- [28] Goldthorpe, B.D., *Constitutive Equations for Annealed and Explosively Shocked Iron for Application to High Strain Rates and Large Strains* in 3rd International Conference on Mechanical and Physical Behaviour of Materials Under Dynamic Loading - DYMAT 91, Les Editions de Physique, Les Ulix Cedex, Pg. 829- 835.
- [29] Bai, Y. and Dodd, B., *Adiabatic Shear Localization: Occurrence, Theories, and Applications*, Pergamon Press, Toronto, 1992.
- [30] Lindholm, U.S., *High-Speed Hydraulic Torsional Machines* in *The Metals Handbook*, 9th edn., vol. 8, ASM, Metals Park, OH, 1985, Pg. 215-216.
- [31] Lindholm, U.S., *Torsional Impact Testing* in *The Metals Handbook*, 9th edn., vol. 8, ASM, Metals Park, OH, 1985, Pg. 216-218.
- [32] Baker, W.W. and Yew, C.H., *Strain Rate Effects in the Propagation of Torsional Plastic Waves*, J. Appl. Mech., vol. 33, 1966, Pg. 917-923.
- [33] Staker, M.R., *On Adiabatic Shear Band Determinations by Surface Observation*, Scripta Metallurgica, 14, 1980, Pg. 677-680.

- [34] Curran, D.R., Seaman, I., and Shockey, D.A., *Dynamic Failure of Solids*, Physics Reports, 147, 1987, Pg. 253-388.
- [35] Hockett, J.E., *The Cam Plastometer* in *The Metals Handbook*, 9th edn., vol. 8, ASM, Metals Park, OH, 1985, Pg. 193.
- [36] Hockett, J.E., *The Cam Plastomer* in *The Metals Handbook*, 9th edn., vol. 8, ASM, Metals Park, OH, 1985, Pg. 193-196.
- [37] Dubber, G.B., *Drop Tower Compression Test* in *The Metals Handbook*, 9th edn., vol. 8, ASM, Metals Park, OH, 1985, Pg. 215-216.
- [38] Hopkinson, B., *A Method of Measuring the Pressure Produced in the Detonation of Explosives or by the Impact of Bullets*, Phil. Trans. A., vol. 213, 1914, Pg. 437.
- [39] Kolsky, H., *An Investigation of the Mechanical Properties of Materials at Very High Rates of Loading*, Proc. Royal Soc., Vol 62, Pg. 676.
- [40] Hauser, F.E., Simmons, J.A., and Dorn, J.E., *Strain Rate Effects in Wave Propagation, in Response of Metals to High Velocity Deformation*, Metallurgical Society Conferences, Vol. 9. Shewmon, P.G. and Zackay, V.F., Ed., Interscience, New York, 1961, Pg. 93.
- [41] Lindholm, U.S. and Yeakley, L.M., *High Strain-Rate testing: Tension and Compression*, Exper. Mech., Vol. 8, 1968, Pg. 1.

- [42] Nicholas, T., *Tensile Testing of Materials at High Rates of Strain*, Experimental Mechanics, 21, 1981, Pg. 177-185.
- [43] Nicholas, T., *Material Behaviour at High Rates of Strain*, Chapter 8 of *Impact Dynamics* by Zukas, J.A., Nicholas, T., Swift, F.H., Greszczuk, L.B., and Curran, D.R., John Wiley & Sons, Inc., New York, 1982.
- [44] Wulf, G.L., *High Strain Rate Compression of Titanium and some Titanium Alloys*, Int. J. Mech. Sci., 21, 1979, Pg. 713-718.
- [45] Hartmann, K.H., Kunze, H.D., and Meyer, L.W., *Metallurgical Effects on Impact Loaded Materials*, In *Shock Waves and High-Strain-Rate Phenomena in Metals*, eds. Meyers, M.A. and Murr, L.E., Plenum Press, New York, 1981, Pg. 325-337.
- [46] Campbell, J.D., and Ferguson, W.G., *The Temperature and Shear Strain Rate Dependence of Shear Strength of Mild Steel*, Philos. Mag., Vol. 21, 1970, Pg. 63-82.
- [47] Harding, J., *Double-Notch Shear Testing and Punch Loading in The Metals Handbook*, 9th edn., vol. 8, ASM, Metals Park, OH, 1985, Pg. 228-230.
- [48] Meyer, L.W. and Manwaring, S., *Critical Adiabatic Shear Strength of Low Alloyed Steel Under Compressive Loading*, In *Metallurgical Applications of Shock Wave and High-Strain-Rate Phenomena*, eds. Murr, L.E., Staudhammer, K.P., and Meyers, M.A., Marcel Dekker Inc., 1986 Pg 657-674.

- [49] Chou, P.C., Hashemi, J., Chou, A., and Rogers, H.C., *Experimental and Finite Element Simulation of Adiabatic Shear Bands in Controlled Penetration Impact*, Int. J. Impact Engng., vol. 11, No. 3, 1991, Pg. 305-321.
- [50] Follansbee, P.S., *Introduction to High Strain Rate Compression Testing in The Metals Handbook*, 9th edn., vol. 8, ASM, Metals Park, OH, 1985, Pg. 192.
- [51] Follansbee, P.S., *The Hopkinson Bar in The Metals Handbook*, 9th edn., vol. 8, ASM, Metals Park, OH, 1985, Pg. 201.
- [52] Murray, W.M. and Miller, W.R., *The Bonded Electrical Resistance Strain Gage*, Oxford University Press, New York, 1992.
- [53] Pelletier, P., *Mechanical Behaviour of Armco Iron at High Rates and Elevated Temperatures*, M.Eng. Thesis, Carleton University, June 1995.
- [54] Hertzberg, R.W., *Deformation and Fracture Mechanics of Engineering Materials*, 3rd edn., Wiley, Toronto, 1989.
- [55] Omega, *The Temperature Handbook*, vol. 27, Omega Engineering, Inc., 1989, Pg. Z-14.
- [56] Omega, *The Temperature Handbook*, vol. 27, Omega Engineering, Inc., 1989, Pg. Z-64.
- [57] Pilkey, A.K., Personal communication, 1995.

- [58] Nicholas, T., *Experimental Methods in Material Behaviour at High Strain Rates Courses Notes*, Computational Mechanics Associated, Towson, MD, 1992, Pg. 230.
- [59] Lindholm, U.S., *Dynamic Deformation of Metals in Behaviour of Materials Under Dynamic Loading* - Proceedings of the ASME colloquium on Behaviour of Materials Under Dynamic Loading, N.J. Huffington Jr. ed., ASME, New-York, NY, 1965, Pg. 42-61.
- [60] Reinders, B.O. and Kunze, H.D., *Influence of Mechanical Twinning on the Deformation Behaviour of Armco Iron in Shock-Wave and High-Strain-Rate Phenomena in Materials* in Explomet 1990 Proceedings, MDI, New-York, NY, 1992, Pg. 127-136.
- [61] Johnson, G.R. and Cook, W.H., *Fracture Characteristics of Three Metals Subjected to Various Strains, Strain Rates, Temperatures and Pressures*. Engineering Fracture Mechanics, Vol. 21, No. 1, 1985, Pg. 31-48.
- [62] Weston, G.M., *Flow Stress of Shock-Hardened Remco Iron over Strain Rates from 0.001 to 9000 s⁻¹*, Journal of Material Science Letters, 11, 1992, Pg. 1361-1363.
- [63] Weston, G.M. and Whitty, F., MRL Report in preparation.
- [64] Cho, K, Chi, Y.C., and Duffy, J., *Microscopic Observation of Adiabatic Shear Bands in Three Different Steels*, AD-A202522, 1988.

- [65] Hallquist, J.O., *LS-DYNA2D - An Explicit Two-dimensional Hydrodynamic Finite Element Code with Interactive Rezoning and Graphical Display*, Livermore Software Technology Corporation, LSTC-1004, 1993.
- [66] *I-DEAS Master Series*, Release 2, Structural Dynamics Research Corporation (SDRC), 1994.
- [67] Worswick, M.J, *A Suite of Patran Translators for DYNA and EPIC Codes*, Suffield Research Note No. 1327, Defense Research Establishment Suffield, Ralston, ALTA, 1990.
- [68] Hallquist, J.O., *LS-ORION - An Interactive Color Post-Processor for NIKE2D, DYNA2D, and TOPAZ2D*, Livermore Software Technology Corporation, LSTC-1003, 1990.
- [69] Smith, F.W., *Structure and Properties of Engineering Alloys*, 2nd edn., McGraw-Hill, Inc., 1993.
- [70] Philip, T.V., Heat Treating of Ultrahigh-Strength Steels in *The Metals Handbook*, 9th edn., vol. 4, ASM, Metals Park, OH, 1985, Pg. 119-132..
- [71] Hartley, K.A., Duffy, J., and Hawley, R.H., *The Torsional Kolsky (Split-Hopkinson) Bar* in *The Metals Handbook*, 9th edn., vol. 8, ASM, Metals Park, OH, 1985, Pg. 218-228.

- [72] Cleland, D.L. and Stevenson, M.G., *Development of an Improved Torsional Hopkinson Bar Apparatus to Produce High Strains at Shear Strain Rates Up to 10,000 1/s*, Report 1980/IE/2, University of New South Wales, Australia, 1980.

Tables

Table 2.1 - Velocity Data

Spec. #	V _s (m/s)	V _c (m/s)	Error (%)	Spec. #	V _s (m/s)	V _c (m/s)	Error (%)
CS022	12.93	13.58	5.01	SS051	14.82	15.80	6.64
CS023	13.17	13.64	3.55	SS052	14.88	15.84	6.45
CS024	12.56	13.20	5.08	SS055	15.15	15.93	5.18
CS025	12.98	13.70	5.51	SS056	15.70	16.45	4.80
CS026	12.69	13.23	4.48	SS057	15.59	16.40	5.22
CS027	12.78	13.39	5.01	SS059	14.94	15.70	5.07
CS028	13.40	13.87	3.66	SS060	15.08	15.89	5.39
CS029	13.60	13.97	2.55	SS061	14.65	15.41	5.21
SS032	16.08	17.16	6.72	SS062	14.86	15.60	4.99
SS033	16.84	17.76	5.44	SS063	14.74	15.60	5.80
SS034	16.60	17.59	5.95	SS064	19.47	19.97	2.59
SS035	N.A.	17.58	-	SS065	19.52	20.17	3.32
SS036	15.25	16.35	7.20	SS066	19.13	19.75	3.23
SS037	16.18	17.01	5.12	SS067	18.89	19.48	3.13
SS039	16.34	17.33	6.06	SS068	18.84	19.37	2.84
SS040	16.38	17.35	5.90	SS069	14.77	15.78	6.82
SS041	20.37	21.28	4.49	SS070	15.23	16.13	5.94
SS042	19.48	20.49	5.21	SS071	14.31	15.36	7.30
SS043	19.68	20.44	3.85	SS072	15.25	16.09	5.53
SS044	20.06	20.68	3.11	SS073	14.35	15.23	6.13
SS045	19.68	20.23	2.78	SS074	14.49	15.37	6.07
SS046	N.A.	20.59	-	SS075	14.27	15.20	6.49
SS047	18.35	18.91	3.03	SS076	13.76	14.60	6.09
SS048	15.32	16.27	6.20	SS077	14.13	15.11	6.90
SS049	14.41	15.43	7.09	SS078	14.33	15.12	5.51
SS050	15.04	15.90	5.73				

Table 2.2 - Constants for equation (2.12) for different types of thermocouple

TYPE E	TYPE J	TYPE K	TYPE R	TYPE S	TYPE T
Nickel-10% Chromium(+) Versus Constant(-)	Iron(+) Versus Constant(-)	Nickel-10% Chromium(+) Versus Nickel-5%(-) (Aluminum Silicon)	Platinum-13% Rhodium(+) Versus Platinum(-)	Platinum-10% Rhodium(+) Versus Platinum(-)	Copper(+) Versus Constant(-)
-100°C to 1000°C* ±0.5°C 9th order	0°C to 760°C* ±0.1°C 5th order	0°C to 1370°C* ±0.7°C 8th order	0°C to 1000°C* ±0.5°C 8th order	0°C to 1750°C* ±1°C 9th order	-160°C to 400°C* ±0.5°C 7th order
a_0 0.104967248	-0.048868252	0.226584602	0.263632917	0.927763167	0.100860910
a_1 17189.45282	19873.14503	24152.10900	179075.491	169526.5150	25727.94369
a_2 -282639.0850	-218614.5353	67233.4248	-48840341.37	-31568363.94	-767345.8295
a_3 12695339.5	11569199.78	2210340.682	1.90002E + 10	8990730663	78025595.81
a_4 -448703084.6	-264917531.4	-860963914.9	-4.82704E + 12	-1.63565E + 12	-9247486589
a_5 1.10866E + 10	2018441314	4.83506E + 10	7.62091E + 14	1.88027E + 14	6.97688E + 11
a_6 -1.76807E + 11		-1.18452E + 12	-7.20026E + 16	-1.37241E + 16	-2.66192E + 13
a_7 1.71842E + 12		1.38690E + 13	3.71496E + 18	6.17501E + 17	3.94078E + 14
a_8 -9.19278E + 12		-6.33708E + 13	-8.03104E + 19	-1.56105E + 19	
a_9 2.06132E + 13				1.69535E + 20	

Table 3.1 - Chemical composition of Armco iron and 4340 steel (Refs. [53] and [69])

	Chemical Composition, nominal w%								
Material	C	Mn	P	S	Cu	N	Ni	Cr	Mo
Armco Iron	0.005	0.04	0.005	0.003	0.01	0.005	-	-	-
AISI 4340	0.40	0.60	-	-	-	-	1.83	0.80	0.25

Table 3.2 - Johnson-Cook parameters for Armco Iron and a 4340 steel quenched and tempered 650 °C (Ref. [24])

Material	Material Properties			Constitutive Constants				
	Density (kg/m ³)	Specific Heat (J/kgK)	Melt. Temp. (K)	A (MPa)	B (MPa)	n	C (MPa)	m
Armco Iron	7890	452	1811	175	380	0.32	0.060	0.55
4340 Steel	7830	477	1793	792	510	0.26	0.014	1.03

Table 3.3 - Goldthorpe modified Zerilli-Armstrong constants for as-received and shock hardened Remco iron (Ref. [28])

Materials	Constitutive Constants						
	C_1 (MPa)	C_2 (MPa)	C_3 (K ⁻¹)	C_4 (K ⁻¹)	C_5 (MPa)	n (-)	C_6 (MPa)
As-received	50	1130	0.00515	0.000262	357	0.52	0
Shock-hardened	50	1130	0.00515	0.000262	125	0.34	350

Table 3.4 - Mechanical properties for different tempers of a 4340 steel (Ref. [70])

	Published					Actual
Tempering Temp. (°C)	Tensile Strength (MPa)	Yield Strength (MPa)	Elongation in 50 mm (%)	Reduction in area (%)	Hardness (HRC)	Hardness (HRC)
205°C	1980	1860	11	39	53	50
425°C	1500	1365	14	48	46	43
650°C	120	860	20	60	31	28

Table 5.1 - Uniaxial Compression Results

Spec. #	V_s (m/s)	$\dot{\epsilon}$ (s ⁻¹)	Before Tests		After Test		ϵ_t (m/m)	Condition
			D1 (in)	T1 (in)	D2 (in)	T2 (in)		
CS022	12.9	1480	0.2485	0.1240	0.2740	0.1040	0.139	As-received
CS023	13.2	1480	0.2485	0.1250	0.2745	0.1035	0.141	As-received
CS024	12.6	1350	0.2475	0.1240	0.2700	0.1055	0.127	As-received
CS025	13.0	1500	0.2490	0.1230	0.2755	0.1020	0.141	As-received
CS026	12.7	1610	0.2480	0.1245	0.2785	0.1015	0.157	Annealed
CS027	12.8	1600	0.2490	0.1235	0.2765	0.1005	0.153	Annealed
CS028	13.4	1760	0.2490	0.1245	0.2780	0.0980	0.170	Annealed
CS029	13.6	1780	0.2480	0.1240	0.2805	0.0980	0.174	Annealed

Table 5.2 - Punching-shear experimental results for fine grain Armco iron

Spec. #	V_s (m/s)	$\dot{\gamma}$ (s^{-1})	Specimen Geometry			γ (mm/mm)	Testing Temp. ($^{\circ}C$)
			D1 (in)	D2 (in)	T1 (in)		
SS032	16.08	43 200	0.3280	0.3390	0.0255	1.80	21.0
SS033	16.80	43 300	0.3280	0.3395	0.0355	2.53	21.0
SS034	16.60	42 200	0.3280	0.3395	0.0520	2.64*	21.0
SS039	16.34	43 900	0.3285	0.3395	0.0300	2.21	21.0
SS069	14.77	40 700	0.3280	0.3390	0.0265	1.63	250
SS070	15.23	42 700	0.3280	0.3390	0.0345	2.44	250
SS071	14.31	41 000	0.3280	0.3390	0.0525	3.63*	250
SS076	13.76	40 800	0.3280	0.3390	0.0305	2.09	250

* Specimen did not fractured

Table 5.3 - Punching-shear experimental results for coarse grain Armco iron

Spec. #	V_s (m/s)	$\dot{\gamma}$ (s^{-1})	Specimen Geometry			γ (mm/mm)	Testing Temp. ($^{\circ}C$)
			D1 (in)	D2 (in)	T1 (in)		
SS035	N.A.	43 900	0.3280	0.3390	0.0255	1.70	21.0
SS036	15.25	44 500	0.3280	0.3380	0.0360	2.96*	21.0
SS037	16.18	43 000	0.3280	0.3390	0.0510	3.50*	21.0
SS040	16.33	41 600	0.3280	0.3395	0.0305	2.16	21.0
SS052	14.88	37 800	0.3285	0.3390	0.0265	1.90	21.0

* Specimen did not fractured

Table 5.4 - Punching-shear experimental results for as-received Remco iron

Spec. #	V_s (m/s)	$\dot{\gamma}$ (s^{-1})	Specimen Geometry			γ (mm/mm)	Testing Temp. ($^{\circ}C$)
			D1 (in)	D2 (in)	T1 (in)		
SS055	15.15	44 500	0.3290	0.3390	0.0355	2.89	21.0
SS056	15.70	42 300	0.3280	0.3390	0.0515	3.40*	21.0
SS057	15.59	43 900	0.3285	0.3390	0.0305	2.22	21.0
SS059	14.94	36 800	0.3280	0.3395	0.0255	1.79	21.0

* Specimen did not fractured

Table 5.5 - Punching-shear experimental results for shock-hardened Remco iron

Spec. #	V_s (m/s)	$\dot{\gamma}$ (s^{-1})	Specimen Geometry			γ (mm/mm)	Testing Temp. ($^{\circ}C$)
			D1 (in)	D2 (in)	T1 (in)		
SS060	15.08	32 200	0.3280	0.3390	0.0255	2.30*	21.0
SS061	14.65	32 700	0.3285	0.3390	0.0305	2.20	21.0
SS062	14.86	32 100	0.3280	0.3390	0.0255	1.88	21.0
SS063	14.74	26 800	0.3280	0.3390	0.0100	0.51	21.0
SS072	15.25	38 100	0.3280	0.3390	0.0360	2.59	250
SS074	14.49	34 400	0.3280	0.3390	0.0260	1.70	250
SS075	14.27	36 000	0.3285	0.3395	0.0295	1.94	250
SS077	14.13	26 500	0.3275	0.3390	0.0125	0.68	21.0
SS078	14.33	31 600	0.3280	0.3390	0.0175	1.13	21.0

* Specimen did not fractured

Table 5.6 - Punching-shear experimental results for 4340 steel with various heat treatments

Spec. #	V_s (m/s)	$\dot{\gamma}$ (s^{-1})	Specimen Geometry			γ (mm/mm)	Tempering Temp. ($^{\circ}C$)
			D1 (in)	D2 (in)	D1 (in)		
SS041	20.37	27 600	0.3280	0.3390	0.0255	1.17*	200
SS042	19.48	27 100	0.3280	0.3390	0.0310	1.43*	200
SS043	19.68	22 300	0.3280	0.3390	0.0250	0.90*	450
SS044	20.06	26 700	0.3280	0.3390	0.0295	0.98*	450
SS045	19.68	33 900	0.3280	0.3390	0.0260	1.40*	650
SS046	N.A.	29 000	0.3275	0.3380	0.0305	1.22*	650
SS047	18.35	35 700	0.3280	0.3390	0.0265	1.58*	Annealed
SS048	15.32	27 000	0.3280	0.3395	0.0260	1.63*	Annealed
SS049	14.41	27 200	0.3280	0.3390	0.0305	1.80*	Annealed
SS050	15.04	15 000	0.3280	0.3390	0.0255	0.97*	450
SS051	19.82	24 600	0.3280	0.3390	0.0255	1.77*	650
SS064	19.47	22 200	0.3285	0.3400	0.0255	0.91*	200
SS065	19.53	18 000	0.3280	0.3390	0.0105	0.66	200
SS066	19.13	20 100	0.3275	0.3390	0.0095	0.57	450
SS067	18.89	22 300	0.3280	0.3390	0.0090	0.57	650
SS068	18.84	17 000	0.3270	0.3390	0.0090	0.46	Annealed

* Specimen did not fractured

Figures

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(a)

(b)

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(a) Schematic view

(b) Photograph

Figure 2.1 - Compressive Split Hopkinson Bar (CSHB) apparatus

(a) Schematic view

(b) Photograph

Figure 2.2 - Gas gun assembly

(a) Schematic view

(b) Photograph

Figure 2.3 - Alignment tube assembly

(a) Schematic view

(b) Photograph

Figure 2.4 - Test chamber assembly

(a) Schematic view

(b) Photograph

Figure 2.5 - Momentum trap assembly

Figure 2.6 - Compression alignment tool

Figure 2.7 - Compression testing assembly

(a) Schematic view

(b) Photograph

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(a) Schematic view

(b) Photograph

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(a)

(b)

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(a) Fine grain

(b) Coarse grain

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Figure 3.2 - True stress versus strain rate for various strain values (Ref. [53])

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(a) As-received Remco iron

(b) Shock-hardened Remco

Figure 3.5 - Optical micrograph of as-received and shock-hardened Remco iron

Figure 3.6 - Though-thickness hardness trace of the shock-hardened Remco iron (Ref. [62])

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(b) 450 °C tempered 4340

(c) 650 °C tempered 4340 steel

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Figure 4.2 - Close up view of shear region in the die-specimen-punch assembly

(a) Fine grain Armco iron deformed at room temperature

(b) Fine grain Armco iron deformed at 250 °C

Figure 4.3 - Velocity load curves

(c) Shock-hardened Remco iron deformed at room temperature

(d) Shock-hardened Remco iron deformed at 250 °C

Figure 4.3 - Velocity load curves (con't)

(e) 4340 steel tempered at 650 °C and deformed at room temperature

Figure 4.3 - Velocity load curves (con't)

Figure 4.4 - Deformed mesh with maximum shear stress criterion enabled

(a) Element near die radius

(b) Element 1/4 of the way through the specimen

Figure 4.5 - Instantaneous and average principal shear strain rates versus time curves

(c) Element 3/4 of the way through the specimen

(d) Element near punch radii

Figure 4.5 - Instantaneous and average principal shear strain rates versus time curves (con't)

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Figure 5.1 - Strain rate versus time data for uniaxial compression specimens

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(a)

(b)

Figure 5.5 - Higher magnification micrographs showing relative size between the indenter and shear band in fine grain Armco iron

(a) Fine grain

(b) Coarse grain

Figure 5.6 - Hardness traverse for Armco iron

Figure 5.7 -Nominal shear stress-strain curves for annealed Armco and as-received Remco iron

(a) As-received

(b) Shock-hardened

Figure 5.8 - Hardness traverse for Remco iron

Figure 5.9 - Nominal shear-stress strain curves for as-received and shock-hardened Remco iron

Figure 5.10 - Nominal shear-stress strain curves for Armco iron deformed at room temperature and 250 °C

(a) Annealed Armco iron

(b) Shock-hardened Remco iron

Figure 5.11 - Hardness traverse for elevated temperature experiments

Figure 5.12 - Nominal shear-stress strain curves for shock-hardened Remco deformed at room temperature and 250 °C and annealed Armco deformed at 250 °C

Figure 5.13 - Nominal shear-stress strain curves for 4340 annealed and tempered steels

(a)

(b)

Figure 5.14 - Optical micrograph showing shear region in fine grain Armco iron

(c)

Figure 5.14 - Optical micrograph showing shear region in fine grain Armco iron (con't)

Figure 5.15 -Optical micrograph showing shear region in coarse grain Armco iron

(a)

(b)

Figure 5.16 - Optical micrograph showing shear region in shock-hardened Remco iron

(c)

(d)

Figure 5.16 - Optical micrograph showing shear region in shock-hardened Remco iron (con't)

Figure 5.17 -Optical micrograph showing shear region in as-received Remco iron

(a)

(b)

Figure 5.18 - Higher magnification micrograph showing shear region in fine grain Armco iron

Figure 5.19 - Optical micrograph of fine grain Armco iron deformed at 250 °C

Figure 5.20 - Optical micrograph of shock-hardened Remco iron deformed at 250 °C

(a)

(b)

Figure 5.21 - Low and higher magnification micrographs of annealed 4340 steel steels

(a)

(b)

Figure 5.22 - Low and higher magnification micrographs of 200 °C tempered 4340 steel

(a)

(b)

Figure 5.23 - Low and higher magnification micrographs of 450 °C tempered 4340 steel

(a)

(b)

Figure 5.24 - Low and higher magnification micrographs of 650 °C tempered 4340 steel

Shear Direction →

(a)

Shear Direction →

(b)

Figure 5.25 - Low and higher magnification fractographs of annealed Armco iron

Figure 5.26 - Low and higher magnification fractographs of annealed Remco iron

Figure 5.27 - Low and higher magnification fractographs of shock-hardened Remco iron

(a)

(b)

Figure 5.28 - Low and higher magnification fractographs of annealed 4340 steel

Figure 5.29 - Low and higher magnification fractographs of 200 °C tempered 4340 steel

← Shear Direction

(c)

← Shear Direction

(d)

Figure 5.29 - Low and higher magnification fractographs of 200 °C tempered 4340 steel
(con't)

Figure 5.30 - Low and higher magnification fractographs of 450 °C tempered 4340 steel

Figure 5.30 - Low and higher magnification fractographs of 450 °C tempered 4340 steel (con't)

← Shear Direction

(e)

← Shear Direction

(f)

Figure 5.30 - Low and higher magnification fractographs of 450 °C tempered 4340 steel (con't)

(a)

(b)

Figure 5.31 - Low and higher magnification fractographs of 650 °C tempered 4340 steel

(c)

(d)

Figure 5.31 - Low and higher magnification fractographs of 650 °C tempered 4340 steel (con't)

Figure 6.1 - Contours of effective plastic strain for annealed Remco iron tested at room temperature (21 °C), 60 μ s after deformation begins

Figure 6.2 - Contours of average principal strain rate for annealed Remco iron tested at room temperature (21 °C), 60 μ s after deformation begins

Figure 6.3 - Contours of temperature (°C) for annealed Remco iron tested at room temperature (21 °C), 60 μ s after deformation begins

Figure 6.4 - Contours of effective plastic strain for annealed Remco iron tested at an elevated temperature (250 °C), 60 μ s after deformation begins

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Figure 6.12 - Temperature versus time history for an element in centre of shear band for annealed and shock-hardened Remco iron tested at an elevated temperature (250 °C)

Figure 6.13 - Nominal shear stress-strain curve for numerical model with failure disabled and experiment for shock-hardened Remco iron tested at room temperature (21 °C)

Figure 6.14 - Nominal shear stress-strain curve for numerical model with failure disabled and experiment for shock-hardened Remco iron tested at an elevated temperature (250 °C)

Figure 6.15 - Contours of effective plastic strain for 4340 steel quenched and tempered at 650 °C tested at room temperature (21 °C), 35 μ s after deformation begins

Figure 6.16 - Contours of average principal shear strain rate for 4340 steel quenched and tempered at 650 °C tested at room temperature (21 °C), 35 μ s after deformation begins

Figure 6.17 - Contours of temperature ($^{\circ}\text{C}$) for 4340 steel quenched and tempered at 650°C tested at room temperature (21°C), $35\text{ }\mu\text{s}$ after deformation begins

Figure 6.18 - Temperature versus time histories for elements in the shear band for 4340 steel quenched and tempered at 650 °C tested at room temperature (21 °C)

Figure 6.19 - Contours of hardening rate, $d\sigma/d\varepsilon|_{d\varepsilon=0}$, for annealed Remco iron tested at room temperature (21°C), 48 μ s after deformation begins

(a) 48 μ s after deformation begins

(b) 51 μ s after deformation begins

Figure 6.20 - Predicted localization and crack propagation using MSS criterion for annealed Remco iron

(c) 54 μ s after deformation begins

(d) 58 μ s after deformation begins

Figure 6.20 - Predicted localization and crack propagation using MSS criterion for annealed Remco iron (con't)

Figure 6.21 - Nominal shear stress-strain curve for numerical model using the MSS and experiment for annealed Remco iron tested at room temperature (21 °C)

Figure 6.22 - Nominal shear stress-strain curve for numerical model using the MSS criterion with $\dot{\gamma}_{crit}$ and experiment for annealed Remco iron tested at room temperature (21 °C)

(a) 60 μs (b) 77 μs

Figure 6.23 - Deformed mesh for annealed Remco iron using MSS criterion with $\dot{\gamma}_{\text{crit}}=12,500 \text{ s}^{-1}$, 60 μs after deformation begins

(a) 60 μs (b) 77 μs

Figure 6.24 - Deformed mesh for annealed Remco iron using MSS criterion with $\dot{\gamma}_{\text{crit}} \approx 22,500 \text{ s}^{-1}$, 60 μs after deformation begins

(a) 60 μs (b) 80 μs

Figure 6.25 - Deformed mesh for annealed Remco iron using MSS criterion with $\dot{\gamma}_{\text{crit}}=28,125 \text{ s}^{-1}$, 60 μs after deformation begins

(a) 60 μs (b) 76 μs

Figure 6.26 - Deformed mesh for annealed Remco iron using MSS criterion with $\dot{\gamma}_{\text{crit}}=33,750 \text{ s}^{-1}$, 60 μs after deformation begins

Figure 6.27 - Nominal shear stress-strain curve for numerical model using the MSS criterion with $\dot{\gamma}_{crit}=26,250 s^{-1}$ and experiment for annealed Remco iron tested at an elevated temperature (250 °C)

Figure 6.28 - Contours of hardening rate, $d\sigma/d\varepsilon|_{d\varepsilon=0}$, for shock-hardened Remco iron tested at room temperature (21°C), 19 μ s after deformation begins

Figure 6.29 - Nominal shear stress-strain curve for numerical model using the MSS and MSS with $\dot{\gamma}_{crit}=20,000 \text{ s}^{-1}$ and experiment for annealed Remco iron tested at room (21 °C)

Figure 6.30 - Deformed mesh for shock-hardened Remco iron using MSS criterion, 60 μs after deformation begins

Figure 6.31 - Nominal shear stress-strain curve for numerical model using the MSS with $\dot{\gamma}_{crit}=25,000 \text{ s}^{-1}$ and experiment for annealed Remco iron tested at an elevated temperature (250 °C)

Figure 6.32 - Contours of hardening rate, $d\sigma/d\varepsilon|_{d\varepsilon=0}$, for 4340 steel tempered at 650°C tested at room temperature (21°C), 49 μ s after deformation begins

(a) 38 μ s after deformation begins

(b) 39 μ s after deformation begins

Figure 6.33 - Predicted localization and crack propagation using MSS criterion for 4340 steel quenched and tempered at 650 °C

(c) 40 μ s after deformation begins

(d) 41 μ s after deformation begins

Figure 6.33 - Predicted localization and crack propagation using MSS criterion for 4340 steel quenched and tempered at 650 °C (con't)

Figure 6.34 - Nominal shear stress-strain curve for numerical model using the MSS, MSS with $\dot{\gamma}_{crit}=20,625 s^{-1}$ and experiment for 4340 steel quenched and tempered at 650 °C tested at room temperature (21 °C)

Figure 6.35 - Contours of flow localization parameter, α , for 4340 steel tempered at 650 °C tested at room temperature (21 °C), 54 μ s after deformation begins

Figure 6.36 - Nominal shear stress-strain curve for numerical model using the flow localization criterion with $\alpha=3.0$ and 4.0 and experiment for 4340 steel quenched and tempered at 650 °C tested at room temperature (21 °C)

Figure 6.37 - Contours of flow localization parameter, α , for annealed Remco iron tested at room temperature (21 °C), 49 μ s after deformation begins

Figure 6.38 - Contours of flow localization parameter, α , for shock-hardened Remco iron tested at room temperature (21 °C), 49 μ s after deformation begins

Figure 6.39 - Nominal shear stress-strain curve for numerical model using the flow localization criterion with $\alpha=4.0$ and 4.5 and experiment for shock-hardened Remco tested at temperature room (21 °C)

Appendix A
Data Reduction Program (Nicolet)


```

PROGRAM Nicolet
C Written by P. Pelletier and Modified by C.R. Mason January, 1997
C CONVERTS NICOLET BRIDGE VOLTAGE DATA INTO AXIAL STRAINS
C THEN DETERMINES SPECIMEN STRESS, STRAIN RATE, AND STRAIN.
C CALCULATES AVERAGE SPECIMEN STRESS BASED ON TRANSMITTED
C PULSE AND (INCIDENT+REFLECTED) PULSE
C CALCULATES STRAIN RATE BASED ON REFLECTED PULSE AND
C (INCIDENT-REFLECTED-TRANSMITTED) PULSE
C THIS PROGRAM USES THE COMPLETE BRIDGE EQUATION METHOD
C THE NUMBER OF SAMPLES IN THE FILE CAN BE UP TO 20000.
C MODIFIED ON DECEMBER 11 1996 BY C.R. MASON

```

```

CHARACTER*20 NAME0,NAME1,NAME2
CHARACTER*10 PRO
CHARACTER*72 JUNK
INTEGER I,INDEXI,INDEXR,NB,J,N,NG(2),NFG(2)
REAL CH1(2,20000), CH2(2,20000),GAUGE(2,17),VE(2),VD
REAL CH12(2,20000), CH22(2,20000),INC,REFL,TRAN
REAL L,ML,D,ASPEC,PI,TRUEERIT,D1,D2,T3,TYP
REAL EZINC,EZRE(2000),EZREFL,EZTRAN,SUM,EZRIT,EZRITE(2000)
REAL SAV(2000),STRAN(2000),SIR(2000),DOFS,ABARC,ABAR
REAL ESPEC(2000),ESPECR,C,E,ABART,YMIN,DIS,SL,SUM2,EERIT(2000)
REAL ESAV(2000),ESTRAN(2000),ESIR(2000),EE(2000),F(4)
PARAMETER (PI=3.141592654, E=2.0685E+05, C=5121.2)
PARAMETER (ABART=197.9326,ABARC=126.6769,DIS=160.0)
REAL TRUEEE,TRUEA,TRUEEALL,TSVRIT,TSTRANRIT,TSIRRIT
REAL EZALL(2000),EEALL(2000),SUM3,DEE(2000),DTJC,DTZA,TJC,TZA
REAL A,B,C7,C6,M,Tref,Tmelt,T,ddsig,C1,C3,C4,C5,K,GS,JC,ZA
REAL C8,TREDEE,RHO,CP,JCZA,TJCZA,DVEL,TDVEL,AGEDVEL,NUM
REAL P1,P2,P3,P4,AP5,AP6,AN,SP5,SP6,SN,GINT,GSLOPE,AGMZA,SGMZA
REAL TAGMZA,TSGMZA,DTAGMZA,DTSGMZA

```

```

C ABART AND ABARC ARE IN mm^2
C STRESSES ARE IN MPa
C DATA FILES CAN HAVE UP TO 20000 SAMPLES
C UPPER TRIGGER LEVEL MUST BE 20 mV

```

```

C ASK IF IT IS A TYPICAL FILE
PRINT *, 'IS THIS A TYPICAL FILE (1-YES 2-NO):'
PRINT *, 'NUMBER OF POINTS IN FILE = 7000'
PRINT *, 'STRIKER LENGTH = 12 IN'
PRINT *, 'DIST. FROM END OF INC. BAR TO GAUGE = 14 IN'
PRINT *, 'OFFSET DISTANCE = 0.0'
PRINT *, 'TRIGGER VALUE = 40 mv'
READ *,TYP

```

```

C BYPASS INPUT IF IT IS TYPICAL
IF (TYP.EQ.1) THEN
  NB=7000
  SL=12
  DT=2*SL*2.54/C*10000
  DG=14
  DT2=2*DG*2.54/C*10000
  DOFS=0.0
  DOFS=DOFS*50.8/C*10000
  TR=40
  GOTO 5
ENDIF

```

```

C ASK HOW MANY POINTS IN THE FILE
  PRINT *, 'ENTER THE NUMBER OF POINTS IN YOUR FILES: '
  READ *, NB

C ASK FOR THE STRIKER LENGTH
  PRINT *, 'ENTER THE STRIKER LENGTH (inches): '
  READ *, SL
  DT=2*SL*2.54/C*10000

C ASK FOR DISTANCE FROM END OF BAR TO STRAIN GAUGE
  PRINT *, 'ENTER DIST. FROM END OF INC. BAR TO GAUGE (inches): '
  READ *, DG
  DT2=2*DG*2.54/C*10000

  PRINT *, 'OFFSET DIST. BETWEEN INC. AND TRANS. GAUGES (inch): '
  READ *, DOFS
  DOFS=DOFS*50.8/C*10000

C ASK FOR TRIGGER VALUE
  PRINT *, 'ENTER TRIGGER VALUE (mV): '
  READ *, TR

C ACQUIRE SPECIMEN STATISTICS

5  PRINT *, 'ENTER THE NUMBER FOR THE TYPE OF SAMPLE USED'
  PRINT *, ' (1) J-NOTCH WITH LARGE SHOULDERS (NJ1)'
  PRINT *, ' (2) J-NOTCH WITH SMALL SHOULDERS VER. A (NJ2)'
  PRINT *, ' (3) J-NOTCH WITH SMALL SHOULDERS VER. B (NJ3)'
  PRINT *, ' (4) C-NOTCH (NC1)'
  PRINT *, ' (5) UNIAXIAL LONG (UA1)'
  PRINT *, ' (6) UNIAXIAL SHORT (UB1)'
  PRINT *, ' (7) J-NOTCH FOR OFHC COPPER'
  PRINT *, ' (8) COMPRESSION'
  PRINT *, ' (9) SHEAR'
  READ (*,940) N

IF (N.LE.8) THEN
  PRINT *, 'ENTER GAUGE LENGTH OF SPECIMEN (inches): '
  READ *, ML

  PRINT *, 'ENTER GAUGE DIAMETER OF SPECIMEN (inches): '
  READ *, D

  ASPEC = PI/4.0*(D*25.4)**2
  L=ML*25.4

  PRINT *, 'DO YOU WANT THE J-C, Z-A, AND GMZA EQUATIONS'
  PRINT *, '(1-YES 2-NO)'
  READ *, JCZA
  TJCZA=JCZA

  IF (JCZA.EQ.1) THEN
    PRINT *, 'ARE THE J-C, Z-A, AND GMZ-A TYPICAL VALUES?'
    PRINT *, '(1-YES 2-NO)'
    PRINT *, 'J-C PARAMETERS:'
    PRINT *, ' A=175 MPa,B=380 MPa,n=0.32,C=0.06,m=0.55'
    PRINT *, ' Tref=21 C,Tmelt=1538'
    PRINT *, 'Z-A BCC PARAMETERS:'
    PRINT *, ' ddsig=0,C1=1033 MPa,C3=0.00698 1/K,C4=0.000415 1/K'
    PRINT *, ' C5=266 MPa,n=0.289,k=22 MPa mm^0.5,GS=0.1111 mm'
  
```

```

PRINT *, 'GOLDTHORPE MODIFIED Z-A PARAMETERS:'
PRINT *, 'ANNEALED MATERIAL:'
PRINT *, 'C1=50,C2=1130,C3=0.00515,C4=0.000262,'
PRINT *, 'C5=357,n=0.52,C6=0,Gint=1.13,Gslope=-0.000445'
PRINT *, 'SHOCKED MATERIAL:'
PRINT *, 'C1=50,C2=1130,C3=0.00515,C4=0.000262,'
PRINT *, 'C5=125,n=0.34,C6=350,Gint=1.13,Gslope=-0.000445'
PRINT *, 'COMMON PARAMETERS'
PRINT *, 'T=21 C, RHO= 7886.9 kg/m^2, Cp= 452.6 J/(kg*K)'
READ *,TYP2

IF (TYP2.EQ.1) THEN
  A=175.0
  B=380.0
  C7=0.32
  C6=0.06
  m=0.55
  Tref=294.0
  Tmelt=1811.0

  ddsig=0.0
  C1=1033.0
  C3=0.00698
  C4=0.000415
  C5=266.0
  C8=0.289
  k=22.0
  GS=0.1111

  P1=50.0
  P2=1130.0
  P3=0.00515
  F4=0.000262
  AP5=357.0
  AP6=0.0
  AN=0.52
  SP5=125.0
  SP6=350.0
  SN=0.34
  GINT=1.13
  GSLOPE=-0.000445

  RHO=7886.9
  CP=452.6
  FACT1=1000000/(RHO*CP)
  T=294
  TJC=294
  TZA=294
  TAGMZA=294
  TSGMZA=294
  ZZZ=0.0
ELSE
  PRINT *, 'THE J-C, Z-A, AND GMZ-A WILL NOT BE CALCULATED'
  PRINT *, 'CORRECTLY.'
END IF

OPEN (UNIT=411, FILE='s-e.jc.tran.r.exp', STATUS = 'NEW')
OPEN (UNIT=412, FILE='s-e.za.tran.r.exp', STATUS = 'NEW')
OPEN (UNIT=413, FILE='ts-e.jc.tran.r.exp', STATUS = 'NEW')
OPEN (UNIT=414, FILE='ts-e.za.tran.r.exp', STATUS = 'NEW')
OPEN (UNIT=415, FILE='temp.jc.exp', STATUS = 'NEW')
OPEN (UNIT=416, FILE='temp.za.exp', STATUS = 'NEW')

```

```

OPEN (UNIT=417, FILE='temp.tjc.exp', STATUS = 'NEW')
OPEN (UNIT=418, FILE='temp.tza.exp', STATUS = 'NEW')
OPEN (UNIT=419, FILE='s-e.agmza.tran.r.exp', STATUS = 'NEW')
OPEN (UNIT=420, FILE='s-e.sgmza.tran.r.exp', STATUS = 'NEW')
OPEN (UNIT=421, FILE='ts-e.agmza.tran.r.exp', STATUS = 'NEW')
OPEN (UNIT=422, FILE='ts-e.sgmza.tran.r.exp', STATUS = 'NEW')
OPEN (UNIT=423, FILE='temp.agmza.exp', STATUS = 'NEW')
OPEN (UNIT=424, FILE='temp.sgmza.exp', STATUS = 'NEW')
OPEN (UNIT=425, FILE='temp.tagmza.exp', STATUS = 'NEW')
OPEN (UNIT=426, FILE='temp.tsgmza.exp', STATUS = 'NEW')
END IF

```

```

ELSE IF (N.EQ.9) THEN
  PRINT *, 'ENTER INNER RING DIAMETER (inches): '
  READ *, D1

```

```

  PRINT *, 'ENTER PUNCH DIAMETER (inches): '
  READ *, D2

```

```

  PRINT *, 'ENTER SPECIMEN THICKNESS (inches): '
  READ *, T3

```

```

  ASPEC=(D1+D2)*25.4**2*PI*T3/2
  L=(D1-D2)*12.7
END IF

```

```

IF (N.LE.7) THEN
  ABAR=ABART
ELSE IF (N.GE.8) THEN
  ABAR=ABARC
END IF

```

C READ DATA FROM FILES INTO ARRAYS

```

PRINT *, 'ENTER FILE NAME FOR CHANNEL 1: '
READ (*,900), NAME1
PRINT *, 'ENTER FILE NAME FOR CHANNEL 2: '
READ (*,900), NAME2
OPEN (UNIT=43, FILE=NAME1, STATUS = 'OLD')
OPEN (UNIT=44, FILE=NAME2, STATUS = 'OLD')

DO 10 I=1,14
  READ (43,FMT=900) JUNK
  READ (44,FMT=900) JUNK
10 CONTINUE

DO 20 I=1,NB
  READ (43,FMT=910) CH1(1,I),CH1(2,I)
  READ (44,FMT=910) CH2(1,I),CH2(2,I)
20 CONTINUE

DO 25 I=1,NB
  CH12(1,I)=CH1(1,I)
  CH12(2,I)=CH1(2,I)
  CH22(1,I)=CH2(1,I)
  CH22(2,I)=CH2(2,I)
25 CONTINUE

CLOSE (43)
CLOSE (44)

```

C GET THE BRIDGE DATA

```

      PRINT *, 'ENTER THE NAME OF THE FILE WITH THE BRIDGES DATA: '
      READ (*,900), NAME0
      OPEN (UNIT=42, FILE=NAME0, STATUS='OLD')
      DO 30 I=1,5
      READ (42,FMT=900) JUNK
30    CONTINUE

      DO 70 I=1,2
      DO 40 J=1,6
      READ (42,FMT=915) PRO,GAUGE(I,J)
40    CONTINUE

```

C CALCULATE REQUIRED DATA

C CHECK FOR THE NUMBER OF AND TYPE OF STRAIN GAUGES

```

      IF (GAUGE(I,4).EQ.0.0) THEN
        NG(I)=1
        NFG(I)=1
        GOTO 50
      END IF

      IF (GAUGE(I,4).LT.3.0) THEN
        NG(I)=2
        NFG(I)=2
        GOTO 60
      END IF

      NG(I)=2
      NFG(I)=1
      GAUGE(I,7)=GAUGE(I,1)+GAUGE(I,5)
      GAUGE(I,8)=GAUGE(I,2)+GAUGE(I,6)
      GAUGE(I,9)=GAUGE(I,1)*GAUGE(I,2)-GAUGE(I,5)*GAUGE(I,6)
      GAUGE(I,10)=GAUGE(I,1)*GAUGE(I,2)*(GAUGE(I,3)+GAUGE(I,4))
      GAUGE(I,11)=GAUGE(I,1)*GAUGE(I,2)*GAUGE(I,3)*GAUGE(I,4)
      GAUGE(I,12)=GAUGE(I,7)*GAUGE(I,8)
      GAUGE(I,13)=(GAUGE(I,1)*GAUGE(I,3)*GAUGE(I,8))+(GAUGE(I,2)*GAUGE(I,4)*GAUGE(I,7))
      GOTO 60

50    GAUGE(I,7)=GAUGE(I,1)*GAUGE(I,2)+GAUGE(I,1)*GAUGE(I,6)+GAUGE(I,5)*GAUGE(I,2)
      GAUGE(I,7)=GAUGE(I,7)+GAUGE(I,5)*GAUGE(I,6)
      GAUGE(I,8)=GAUGE(I,1)*GAUGE(I,2)-GAUGE(I,5)*GAUGE(I,6)
      GAUGE(I,9)=GAUGE(I,1)*GAUGE(I,2)*GAUGE(I,3)
      GAUGE(I,10)=(GAUGE(I,2)+GAUGE(I,6))*GAUGE(I,3)*GAUGE(I,1)

```

C ACQUIRE TEST RUN EXCITATION VOLTAGES

```

60  WRITE (*,930) I
     READ *, VE(I)

```

```

70  CONTINUE
     CLOSE (42)

```

C CALCULATE STRAINS FROM VOLTAGES

```

      DO 110 I=1,NB
      DO 100 J=1,2
      IF (J-1) 80,80,90
80    VD=CH1(2,I)*1000
      CALL VOLSTR(VD,J,NG,NFG,VE,GAUGE,N)

```

```

          CH1(2,I)=VD
          GOTO 100
90      VD=CH2(2,I)*1000
          CALL VOLSTR(VD,J,NG,NFG,VE,GAUGE,N)
          CH2(2,I)=VD
100     CONTINUE
110     CONTINUE

C      OPEN OUTPUT FILES
      OPEN (UNIT=296, FILE='tezinc.exp', STATUS = 'NEW')
      OPEN (UNIT=297, FILE='teztran.exp', STATUS= 'NEW')
C      GET STARTING INDICES FOR PULSES
C      DEFINITION OF FACTOR TO START LOOKING FOR FIRST MAXIMUM TAKING
C      THE TYPE AND NUMBER OF GAUGES INTO ACCOUNT

C      DEFINITION OF FACTOR TO LOOK FOR PULSE BEGINNING
      FACI=(GAUGE(1,3)+GAUGE(1,4))/(157*2)*0.5
      J=1
      CALL VOLSTR(FACI,J,NG,NFG,VE,GAUGE,N)
      FACR=(GAUGE(1,3)+GAUGE(1,4))/(157*2)*0.75
      J=1
      CALL VOLSTR(FACR,J,NG,NFG,VE,GAUGE,N)
      FACT=(GAUGE(2,3)+GAUGE(2,4))/(157*2)*0.5
      J=2
      CALL VOLSTR(FACT,J,NG,NFG,VE,GAUGE,N)

C      INCIDENT PULSE
      J=1
      CALL VOLSTR(TR,J,NG,NFG,VE,GAUGE,N)
      DO 120 I=1,NB
      IF (CH1(2,I).GE.TR) THEN
        NPOS=I
        NPOS2=NPOS
        GOTO 130
      END IF
120     CONTINUE

C      SLOPE AND INTERCEPT CALCULATION FROM DATA NEAR THE TRIGGER POINT
130     SUMXY=0.0
      SUMX2=0.0
      SUMY=0.0
      DO 140 I=(NPOS-5),(NPOS+5)
        SUMXY = SUMXY + CH1(1,I) * CH1(2,I)
        SUMX2 = SUMX2 + CH1(1,I)**2
        SUMY = SUMY + CH1(2,I)
140     CONTINUE
      SLOPE = SUMXY/SUMX2
      SINT = SUMY/10.0
      XORI = -SINT/SLOPE

      DO 150 I=1,NB
      IF (CH1(1,I).GE.XORI) THEN
        NPOS=I-1
        GOTO 160
      END IF
150     CONTINUE
160     DO 170 I=NPOS,1,-1
      IF (CH1(2,I).LE.FACI) THEN
        INDEXI=I
        GOTO 180
      ENDIF

```

```

170  CONTINUE

C    REFLECTED PULSE

C    FIRST GUESS AT STARTING POINT FOR REFLECTED PULSE
180  INDEXR=INDEXI+INT(DT2/0.2)
    IF (CH1(2,INDEXR) - FACR) 210,210,190
190  DO 200 I=INDEXR,(INDEXR+2000)
    IF (CH1(2,I).LT.FACR) THEN
        INDEXR=I
        GOTO 230
    END IF
200  CONTINUE
210  DO 220 I=INDEXR,(INDEXR-2000),-1
    IF (CH1(2,I).GE.FACR) THEN
        INDEXR=I
        GOTO 230
    END IF
220  CONTINUE

C    TRANSMITTED PULSE
C    FIRST GUESS AT STARTING POINT FOR TRANSMITTED PULSE
230  INDEXT=INDEXI+INT((DT2+DOFS)/0.2)
    IF (CH2(2,INDEXT) - FACT) 240,260,260
240  DO 250 I=INDEXT,(INDEXT+2000)
    IF (CH2(2,I).GT.FACT) THEN
        INDEXT=I
        GOTO 280
    END IF
250  CONTINUE
260  DO 270 I=INDEXT,(INDEXT-2000),-1
    IF (CH2(2,I).LE.FACT) THEN
        INDEXT=I
        GOTO 280
    END IF
270  CONTINUE

C    PUT THE EXPERIMENTAL STRAIN RESULTS IN FILES

280  DO 290 I=1,NB
    WRITE (296,FMT=920) ((I-1-NPOS2)*0.2),CH1(2,I)
    WRITE (297,FMT=920) ((I-1-NPOS2)*0.2),CH2(2,I)
290  CONTINUE

C    CALCULATE SPECIMEN STRESS AND STRAIN RATE

C    OPEN OUTPUT FILES
OPEN (UNIT=298, FILE='erater.exp', STATUS = 'NEW')
OPEN (UNIT=299, FILE='erate.exp', STATUS = 'NEW')
OPEN (UNIT=300, FILE='s.av.exp', STATUS = 'NEW')
OPEN (UNIT=301, FILE='s.tran.exp', STATUS = 'NEW')
OPEN (UNIT=302, FILE='s.i+r.exp', STATUS = 'NEW')
OPEN (UNIT=303, FILE='eraterit.exp', STATUS = 'NEW')
OPEN (UNIT=304, FILE='ezinc.exp', STATUS = 'NEW')
OPEN (UNIT=305, FILE='ezrefl.exp', STATUS = 'NEW')
OPEN (UNIT=306, FILE='eztran.exp', STATUS = 'NEW')
OPEN (UNIT=307, FILE='ezrefit.exp', STATUS = 'NEW')
OPEN (UNIT=311, FILE='inc.exp', STATUS = 'NEW')
OPEN (UNIT=312, FILE='refl.exp', STATUS = 'NEW')
OPEN (UNIT=313, FILE='tran.exp', STATUS = 'NEW')

```

```

OPEN (UNIT=314, FILE='dvel.exp', STATUS = 'NEW')

300 NP2=INDEXR-INDEXI
DO 310 I=1,NP2
  EZINC=CH1(2,INDEXI+1)
  INC=CH12(2,INDEXI+1)
  EZREFL=CH1(2,INDEXR+1)
  REFL=CH12(2,INDEXR+1)
  EZRE(I)=EZREFL
  EZTRAN=CH2(2,INDEXT+1)
  TRAN=CH22(2,INDEXT+1)
  EZRIT=-(EZINC-EZTRAN)
  EZRITE(I)=EZRIT
  EZALL(I)=EZINC-EZREFL-EZTRAN

  ESPECR = -2*C*EZREFL/L*1000.0
  ESPECRIT = -2*C*EZRITE/L*1000.0
  ESPEC(I) = C*(EZINC-EZREFL-EZTRAN)/L*1000.0
  SAV(I) = E*ABAR/ASPEC*(EZINC+EZREFL+EZTRAN)/2
  STRAN(I) = E*ABAR/ASPEC*EZTRAN
  SIR(I) = E*ABAR/ASPEC*(EZINC+EZREFL)

  IF (N.EQ.8) THEN
    DVEL=ESPEC(I)*L/1000
    IF (ESPEC(I).GE.0.0) THEN
      TDVEL=TDVEL+DVEL
      NUM=NUM+1
    END IF
  END IF

  IF (N.EQ.9) THEN
    ESPECR=ESPECR/2
    ESPECRIT=ESPECRIT/2
    ESPEC(I)=ESPEC(I)/2
    DVEL=ESPEC(I)*L/500
    IF (ESPEC(I).GE.0.0) THEN
      TDVEL=TDVEL+DVEL
      NUM=NUM+1
    END IF
  END IF

  WRITE (298,FMT=920) (I*0.2),ESPECR
  WRITE (299,FMT=920) (I*0.2),ESPEC(I)
  WRITE (300,FMT=920) (I*0.2),SAV(I)
  WRITE (301,FMT=920) (I*0.2),STRAN(I)
  WRITE (302,FMT=920) (I*0.2),SIR(I)
  WRITE (303,FMT=920) (I*0.2),ESPECRIT
  WRITE (304,FMT=920) (I*0.2),EZINC
  WRITE (305,FMT=920) (I*0.2),EZREFL
  WRITE (306,FMT=920) (I*0.2),EZTRAN
  WRITE (307,FMT=920) (I*0.2),EZRITE
  WRITE (311,FMT=920) (I*0.2),INC
  WRITE (312,FMT=920) (I*0.2),REFL
  WRITE (313,FMT=920) (I*0.2),TRAN
  WRITE (314,FMT=920) (I*0.2),DVEL

310 CONTINUE

OPEN (UNIT=501, FILE='avgdvel.exp', STATUS = 'NEW')
AVGDVEL=TDVEL/NUM

```



```
WRITE (501,FMT=950) A VGDVEL
```

```
DO 320 I=1,14
```

```
    CLOSE (296+I)
```

```
320 CONTINUE
```

C CALCULATE ENGINEERING STRESS-STRAIN CURVES

C OPENING FILES

```
OPEN (UNIT=303, FILE='s-e.av.r.exp', STATUS = 'NEW')
OPEN (UNIT=304, FILE='s-e.tran.r.exp', STATUS = 'NEW')
OPEN (UNIT=305, FILE='s-e.i+r.r.exp', STATUS = 'NEW')
OPEN (UNIT=306, FILE='s-e.av.nt.exp', STATUS = 'NEW')
OPEN (UNIT=307, FILE='s-e.tran.nt.exp', STATUS = 'NEW')
OPEN (UNIT=308, FILE='s-e.i+r.rilexp', STATUS = 'NEW')
OPEN (UNIT=309, FILE='s-e.tran.all.exp', STATUS = 'NEW')
OPEN (UNIT=310, FILE='s-e.av.all.exp', STATUS = 'NEW')
```

C CALCULATE ENGINEERING STRESS AND ENGINEERING STRAIN

```
SUM = 0.0
```

```
SUM2 = 0.0
```

```
DO 330 I=1,NP2-1
```

```
    ESAV(I) = (SAV(I)+SAV(I+1))/2.0
    ESTRAN(I) = (STRAN(I)+STRAN(I+1))/2.0
    ESIR(I) = (SIR(I)+SIR(I+1))/2.0
    SUM = SUM + (EZRE(I)+EZRE(I+1))/2.0*0.2E-6
    DEE(I) = -2.0*C*((EZRE(I)+EZRE(I+1))/2.0*0.2E-6)/L*1000
    EE(I) = -2.0*C*SUM/L*1000.0
    SUM2 = SUM2 + (EZRITE(I)+EZRITE(I+1))/2.0*0.2E-6
    EERIT(I) = -2.0*C*SUM2/L*1000.0
    SUM3 = SUM3 + (EZALL(I)+EZALL(I+1))/2.0*0.2E-6
    EEALL(I) = C*SUM3/L*1000.0
```

```
    IF (ESPEC(I).LE.0.0) THEN
```

```
        JCZA=0.0
```

```
    END IF
```

```
    IF (JCZA.EQ.1.AND.N.EQ.8) THEN
```

```
        DTJC= JC*DEE(I)*FACTI
```

```
        TJC=TJC+DTJC
```

```
        DTZA= ZA*DEE(I)*FACTI
```

```
        TZA=TZA+DTZA
```

```
        DTAGMZA= AGMZA*DEE(I)*FACTI
```

```
        TAGMZA=TAGMZA+DTAGMZA
```

```
        DTSGMZA= SGMZA*DEE(I)*FACTI
```

```
        TSGMZA=TSGMZA+DTSGMZA
```

```
        JC=(A+B*EE(I)**C7)*(1+C6*ALOG(ESPEC(I)))
```

```
1      (1-((TJC-Tref)/(Tmelt-Tref))**M)
      ZA=ddsig+C1*(EXP(-C3*TZA+C4*ALOG(ESPEC(I))))+
1      C5*EE(I)**C8+K/GS**0.5
      AGMZA=P1+P2*(EXP((-P3+C4*ALOG(ESPEC(I)))*TAGMZA))+
1      (AP5*EE(I)**AN+AP6)*(GINT+GSLOPE*TAGMZA)
      SGMZA=P1+P2*(EXP((-P3+C4*ALOG(ESPEC(I)))*TSGMZA))+
1      (SP5*EE(I)**SN+SP6)*(GINT+GSLOPE*TSGMZA)
    END IF
```

```

IF (N.EQ.9) THEN
  EE(I)=EE(I)/2
  EERIT(I)=EERIT(I)/2
  EEALL(I)=EEALL(I)/2
END IF

```

```

WRITE (303,FMT=920) EE(I),ESAV(I)
WRITE (304,FMT=920) EE(I),ESTRAN(I)
WRITE (305,FMT=920) EE(I),ESIR(I)
WRITE (306,FMT=920) EERIT(I),ESAV(I)
WRITE (307,FMT=920) EERIT(I),ESTRAN(I)
WRITE (308,FMT=920) EERIT(I),ESIR(I)
WRITE (309,FMT=920) EEALL(I),ESTRAN(I)
WRITE (310,FMT=920) EEALL(I),ESAV(I)

```

```

IF (JCZA.EQ.1.AND.N.EQ.8) THEN
  WRITE (411,FMT=920) EE(I),JC
  WRITE (412,FMT=920) EE(I),ZA
  WRITE (415,FMT=920) EE(I),TJC
  WRITE (416,FMT=920) EE(I),TZA
  WRITE (419,FMT=920) EE(I),AGMZA
  WRITE (420,FMT=920) EE(I),SGMZA
  WRITE (423,FMT=920) EE(I),TAGMZA
  WRITE (424,FMT=920) EE(I),TSGMZA
END IF

```

```

330 CONTINUE

```

```

DO 340 I=1,12
  CLOSE (302+I)

```

```

340 CONTINUE

```

C CALCULATE TRUE STRESS-STRAIN CURVES

C OPEN FILES

```

IF (N.EQ.9) THEN
  GOTO 360
ENDIF
OPEN (UNIT=303, FILE='ts-e.av.r.exp', STATUS = 'NEW')
OPEN (UNIT=304, FILE='ts-e.ur.r.exp', STATUS = 'NEW')
OPEN (UNIT=305, FILE='ts-e.i+r.r.exp', STATUS = 'NEW')
OPEN (UNIT=306, FILE='ts-e.av.rit.exp', STATUS = 'NEW')
OPEN (UNIT=307, FILE='ts-e.ur.rit.exp', STATUS = 'NEW')
OPEN (UNIT=308, FILE='ts-e.i+r.rit.exp', STATUS = 'NEW')
OPEN (UNIT=309, FILE='ts-e.av.all.exp', STATUS = 'NEW')
OPEN (UNIT=310, FILE='ts-e.ur.all.exp', STATUS = 'NEW')
OPEN (UNIT=311, FILE='ts-e.av.all.all.exp', STATUS = 'NEW')

```

C CONVERT EXPERIMENTAL STRESS-STRAIN DATA TO TRUE STRESS-STRAIN

```

IF (N.EQ.1) THEN
  F(1)=281.21
  F(2)=33.5737
  F(3)=8.21119
  F(4)=0.00167224
ELSE IF (N.EQ.2) THEN
  F(1)=277.821
  F(2)=32.2096
  F(3)=8.0921
  F(4)=0.00157984
ELSE IF (N.EQ.3) THEN

```

```

F(1)=161.568
F(2)=-20.4753
F(3)=6.24717
F(4)=0.000785055
ELSE IF (N.EQ.4) THEN
F(1)=14.0082
F(2)=15.8193
F(3)=5.36873
F(4)=0.000462324
ELSE IF (N.EQ.5) THEN
F(1)=12.246
F(2)=-0.617301
F(3)=1.3607
F(4)=0.00087
ELSE IF (N.EQ.6) THEN
F(1)=5.4252
F(2)=1.391
F(3)=1.09353
F(4)=0.000115
ELSE IF (N.EQ.7) THEN
F(1)=-466.842
F(2)=134.834
F(3)=-1.61527
F(4)=0.00293152

END IF

DO 350 I=1,NP2-1
IF (N.LE.7) THEN
TRUEEE=F(1)*EE(I)**3 + F(2)*EE(I)**2 + F(3)*EE(I) + F(4)
TRUEERIT=F(1)*EERIT(I)**3 + F(2)*EERIT(I)**2 + F(3)*EERIT(I)
+ F(4)
TRUEA=ASPEC/EXP(TRUEEE)
TSAV=ESAV(I)*ASPEC/TRUEA
TSTRAN=ESTRAN(I)*ASPEC/TRUEA
TSIR=ESIR(I)*ASPEC/TRUEA
TRUEARIT=ASPEC/EXP(TRUEERIT)
TSAVRIT=ESAV(I)*ASPEC/TRUEARIT
TSTRANRIT=ESTRAN(I)*ASPEC/TRUEARIT
TSIRRIT=ESIR(I)*ASPEC/TRUEARIT

ELSE IF (N.EQ.8) THEN
TRUEEE=ALOG(EE(I)+1)
TRUEDEE=ALOG(DEE(I)+1)
TRUEERIT=ALOG(EERIT(I)+1)
TRUEEALL=ALOG(EEALL(I)+1)
TSAV=ESAV(I)*(1+EE(I))
TSTRAN=ESTRAN(I)*(1+EE(I))
TSIR=ESIR(I)*(1+EE(I))
TSAVRIT=ESAV(I)*(1+EERIT(I))
TSTRANRIT=ESTRAN(I)*(1+EERIT(I))
TSIRRIT=ESIR(I)*(1+EERIT(I))
TSAVALL=ESAV(I)*(1+EEALL(I))
TSTRANALL=ESTRAN(I)*(1+EEALL(I))
TSIRALL=ESIR(I)*(1+EEALL(I))
DTJC= JC*TRUEDEE*FACTI
TJC=TJC+DTJC
DTZA= ZA*TRUEDEE*FACTI
TZA=TZA+DTZA
DTAGMZA= AGMZA*TRUEDEE*FACTI
TAGMZA=TAGMZA+DTAGMZA
DTSGMZA= SGMZA*TRUEDEE*FACTI

```

```

      TSGMZA=TSGMZA+DTSGMZA
      JC=(A+B*TRUEEE**C7)*(1+C6*ALOG(ESPEC(I)))*
1      (1-((TJC-Tref)/(Tmelt-Tref))**M)
      ZA=ddsig+C1*(EXP(-C3*TZA+C4*TZA*ALOG(ESPEC(I))))+
1      C5*TRUEEE**C8+K/GS**0.5
      AGMZA=P1+P2*(EXP((-P3+C4*ALOG(ESPEC(I)))*TAGMZA))+
1      (AP5*TRUEEE**AN+AP6)*(GINT+GSLOPE*TAGMZA)
      SGMZA=P1+P2*(EXP((-P3+C4*ALOG(ESPEC(I)))*TSGMZA))+
1      (SP5*TRUEEE**SN+SP6)*(GINT+GSLOPE*TSGMZA)
      END IF

      WRITE (303,FMT=920) TRUEEE.TSAV
      WRITE (304,FMT=920) TRUEEE.TSTRAN
      WRITE (305,FMT=920) TRUEEE.TSIR
      WRITE (306,FMT=920) TRUEERIT.TSAVRIT
      WRITE (307,FMT=920) TRUEERIT.TSTRANRIT
      WRITE (308,FMT=920) TRUEERIT.TSIRRIT
      WRITE (309,FMT=920) TRUEEALL.TSAV
      WRITE (310,FMT=920) TRUEEALL.TSTRAN
      WRITE (311,FMT=920) TRUEEALL.TSAVALL

      IF (ESPEC(I).LE.0.0) THEN
        TJCZA=0.0
      END IF

      IF (TJCZA.EQ.1.AND.N.EQ.8) THEN
        WRITE (413,FMT=920) TRUEEE.JC
        WRITE (414,FMT=920) TRUEEE.ZA
        WRITE (417,FMT=920) TRUEEE.TJC
        WRITE (418,FMT=920) TRUEEE.TZA
        WRITE (421,FMT=920) TRUEEE.AGMZA
        WRITE (422,FMT=920) TRUEEE.SGMZA
        WRITE (425,FMT=920) TRUEEE.TAGMZA
        WRITE (426,FMT=920) TRUEEE.TSGMZA
      END IF

350  CONTINUE

C  CLOSE FILES
360  DO 370 I=1,6
      CLOSE (302+I)
370  CONTINUE

380  PRINT *, 'DONE'

900  FORMAT (A)
910  FORMAT (E15.7,E15.7)
915  FORMAT (A10,F10.5)
920  FORMAT (F20.10,F20.10)
930  FORMAT ('ENTER EXCITATION VOLTAGE FOR CHANNEL',I2,' (mv): ')
940  FORMAT (I2)
950  FORMAT (F20.10)

      END

C .....
      SUBROUTINE VOLSTR(VALJ,NG,NFG,VE,GAUGE,N)

      DIMENSION NG(*),NFG(*),VE(*),GAUGE(2,*)
      INTEGER J

```

```

IF (N.GT.7) THEN
  VAL=-1*VAL
ENDIF

NCH=1
IF (J.EQ.2) THEN
  NCH=2
ENDIF

  IF (NG(NCH).EQ.1) GOTO 220
  IF (NFG(NCH).EQ.2) GOTO 230

  GAUGE(NCH,15)=GAUGE(NCH,11)*(VAL-VE(NCH))
  GAUGE(NCH,16)=GAUGE(NCH,13)*VAL-GAUGE(NCH,10)*VE(NCH)
  GAUGE(NCH,17)=GAUGE(NCH,12)*VAL-GAUGE(NCH,9)*VE(NCH)

  BET=SQRT((GAUGE(NCH,16)**2)-(4*GAUGE(NCH,15)*GAUGE(NCH,17)))
  RAC1=(-GAUGE(NCH,16)+BET)/(2*GAUGE(NCH,15))
  RAC2=(-GAUGE(NCH,16)-BET)/(2*GAUGE(NCH,15))

C  CHOOSING OF THE ADEQUATE ROOT
  IF (ABS(RAC1).LT.ABS(RAC2)) THEN
    VAL=RAC1
  ELSE
    VAL=RAC2
  ENDIF
  GOTO 240

220  VAL=((GAUGE(NCH,7)*VAL)-(GAUGE(NCH,8)*VE(NCH)))
    1  /((GAUGE(NCH,9)*VE(NCH))-(GAUGE(NCH,10)*VAL))
      GOTO 240

230  GF=1/((1/GAUGE(NCH,3))+(1/GAUGE(NCH,4)))
      STR=VAL/(VE(NCH)*GF)

240  IF (N.GT.7) THEN
    VAL=-1*VAL
  ENDIF
  RETURN
END

```

Appendix B
User Subroutine, umat48 program

```

      subroutine umat48 (cm,eps,sig,hisv,dil,etype,ume)
c-----
c! adiabatic shear constitutive model - stress integration
c! -----
c! written by M. Worswick, February, 1994
c! Modified by C.R. Mason, January, 1997
c-----
c
c variables
c material constants in array cm:
c 1      g0      shear modulus
c 9      bk      bulk modulus
c
c 2      xtype   key to flow stress model:
c      xtype = 1: linear hardening
c 3      qs      initial yield strength
c 4      qh      hardening modulus
c      xtype = 2: power law hardening:
c 3      qs      initial yield strength
c 4      qh      power law exponent
c      xtype = 3: Johnson-Cook flow stress:
c 3      qs      initial yield strength
c 4      qh      hardening coefficient
c 5      qn      hardening exponent
c 6      qc      strain rate coefficient
c 7      qm      thermal softening coefficient
c 11     tmelt   melting temperature
c 12     tref    reference temperature
c 13     erref   reference strain rate
c      xtype = 4: cubic polynomial fit
c 3      qaa     initial yield strength
c 4      qbb     linear hardening term (B)
c 5      qcc     quadratic hardening term (C)
c 6      qdd     cubic hardening term (D)
c 7      qee     strain rate sensitivity
c 8      qer     reference strain rate
c      xtype = 5: Zerilli-Armstrong FCC flow stress:
c 3      phi0    initial yield strength
c 4      phi2    hardening coefficient
c 5      phi3    thermal softening coefficient
c 6      phi4    strain rate coefficient
c 13     erref   reference strain rate
c 33     t0      initial temperature (absolute)
c      xtype = 6: Zerilli-Armstrong BCC flow stress:
c 3      phi0    initial yield strength
c 4      phi1    pre-exponential coefficient
c 5      phi3    thermal softening coefficient
c 6      phi4    strain rate coefficient
c 7      phi5    hardening coefficient
c 8      q       hardening exponent
c 13     erref   reference strain rate
c 24     phi6    grain size effect  $k/l^{0.5}$ 
c 33     t0      initial temperature (absolute)
c 40     ddsig   increase in flow stress due to twinning
c 45     dsigG   solute and the original dislocation density
c 48     sigt    twinning stress
c      xtype = 7: Goldthorpes modified Zerilli-Armstrong BCC flow stress:
c 3      phi0    initial yield strength
c 4      phi2    pre-exponential coefficient
c 5      phi3    thermal softening coefficient
c 6      phi4    strain rate coefficient
c 7      phi5    hardening coefficient

```

```

c 8    q    hardening exponent
c 10   cp    specific heat
c 13   eref  reference strain rate
c 24   phi6  shock hardening coefficient
c 33   t0    initial temperature (absolute)
c 38   Gamma amount of work converted into heat
c 40   ddsig increase in flow stress due to twinning
c 45   dsigG solute and the original dislocation density
c 48   sigt  twinning stress
c
c 23   xfail  failure flag if 1 then fail if 0 no failure
c 15   E     Young's modulus
c 16   poi   Poisson's ratio
c 10   cp    specific heat*initial density (initially read
c         in as specific heat)
c 14   rho0  initial density
c 35   relax change in relaxation parameter
c 36   Gslope slope of temperature dependent G curve
c 37   Gint  Intercept of temperature dependent G curve
c 38   Gamma amount of work converted into heat
c
c extra history output:
c 41   xlhist element id for extra history output
c 42   delt  time history output interval
c 43   refrate strain rate value used in single element test cases
c 44   refstr stress used to normalize in single element test cases
c 46   critrate critical principal shear strain rate used in failure
c 47   critshear critical shear used in failure
c
c -----
c
c axisymmetry
c eps(1)=local incremental r strain
c eps(2)=local incremental z strain
c eps(3)=local incremental theta strain
c eps(4)=local incremental rz strain - Engineering (not tensor)
c
c sig(1)=local r stress
c sig(2)=local z stress
c sig(3)=local theta stress
c sig(4)=local rz stress
c
c history variables:
c hisv(1) = equivalent plastic strain
c hisv(2) = temperature (redundant if energy also stored, but useful for
c         post-processing)
c hisv(3) =
c hisv(4) = internal energy density
c hisv(5) = dsdep
c hisv(6) = flow stress
c hisv(7) = principal shear strain rate
c hisv(8) = angle to principal shear strain rate plane
c hisv(9) = thermal softening rate
c hisv(10)= plastic modulus
c hisv(11)= average principal shear strain rate
c hisv(12)= total number of time steps
c hisv(13)= total of principal shear strain rate
c hisv(14)= local rz stress
c hisv(15)= twinning parameter
c hisv(16)= localization parameter (actual temperature)
c hisv(17)= change in effective plastic stress
c hisv(18)= localization parameter (room temperature)

```



```

c hisv(19)= relaxation parameter
c hisv(20)= dsig/dtemp
c hisv(21)= dsig/dstrain rate
c hisv(22)= dsig/dstrain
c hisv(23)= dsig/dtemp (room temperature)
c hisv(24)= dsig/dstrain rate (room temperature)
c hisv(25)= dsig/dstrain (room temperature)
c
c dtl=current time step size
c etype:
c   eq."quad" for all quad elements
c   note: so far only quad elements have been defined
c
c time=current problem time.
c
c
c All transformations into the element local system are
c performed prior to entering this subroutine. Transformations
c back to the global system are performed after exiting this
c subroutine.
c
c All history variables are initialized to zero in the input
c phase. Initialization of history variables to nonzero values
c may be done during the first call to this subroutine for each
c element.
c
c gur38: d4      shear strain rate
c
c gur39: pold     pressure at beginning of increment
c
c element id counter
c
c common/counter/ idl,ltvi
c
c common/gursl/
1 pnw(128),cc(128),scale(128),aj2(128),ak(128),davg(128),
2 sj2(128)
c common/gur38/d4d(128)
c common/gur39/ pold(128)
c common/gur41/dd(128)
c common/gurma/g0,xtype,qs,qh,qn,qc,qm,xjnk1,
1 bk,cp,tmelt,tref,erho0,E,poi,q1,q2,q3,
2 toler,xiter,phimax,xfail,phi6,enucl,fnucl,snucl,f0,
3 fc,ff,visc,sin,t0,cint,s1,s2,s3,gamma,gamma1,
4 ddsig,xlhist,delt,refrate,refstr,dsigG,critrate,
5 critshear,sigt
c equivalence (qs,qaa),(qh,qbb),(qn,qcc),(qc,qdd),(qm,qee),
1 (xjnk1,qcr)
c equivalence (qs,phi0),(qh,phi1),(qh,phi2),(qn,phi3),(qc,phi4),
1 (qm,phi5),(xjnk1,q),(s1,relax),(s2,Gslope),(s3,Gint)
c common/gurson/soft(128),hydro(128),phi(128),eval(128),
1 evali(128),qlf(128),q3f2(128),alpha(128),
2 gs(128),bkas(128),xa(128),xb(128),xc(128),
3 xe(128),dlamb(128),dpdl(128),dla3(128),
4 dp1(128),dp2(128),dp3(128),dp4(128),
5 dep(128),dfnucl(128),
6 tmp(128),tmpf(128),effs(128),
7 dld(128),d2d(128),d3d(128),epsf(128),
8 a(128),b(128),hard(128),g3qh(128),rv(128),
9 rv2(128),rv3(128),rvi(128)
c common/subinc/str1(128),str12(128),str13(128),
1 str14(128),

```

```

2      dpres(128),punal(128).
3      dsig1(128),dsig2(128),dsig3(128).
4      dsig4(128).
5      nsub(128),xsub(128)
common/fstar/f(128)
dimension cm(*),cmau(48)
equivalence (g0,cmat)
dimension f(128),eplas(128),fpar(128)
real ninv,ninv1

c
c logical failure
common/failed/failure
character*(*) ctype
dimension eps(*),sig(*),hisv(*)

c
c common used for test cases
c
c common/testcase/ tnext,deltat
c
c kill vectorization
c
c i=1
c
c first check if element already failed
c
c if((sig(4).eq.0.0000010).and.
1 (hisv(1).eq.0.0000010))return
c
c Set the strain increment values
c
c if (dt1.gt.0.) then
c   d1=eps(1)/dt1
c   d2=eps(2)/dt1
c   d3=eps(3)/dt1
c   d4=eps(4)/dt1
c   d4d(1)=d4
c else
c   d1=0.
c   d2=0.
c   d3=0.
c   d4=0.
c end if
c ep=hisv(1)
c
c calculate principal shear strain rate and orientation
c
c val1=0.5*(d1-d2)
c val2=0.5*d4
c pssrate=sqrt(val1**2+val2**2)
c angle=57.29577951*atan2(val1,val2)
c hisv(7)=pssrate
c hisv(8)=angle
c hisv(12)=hisv(12)+1
c hisv(13)=hisv(13)+hisv(7)
c hisv(11)=hisv(13)/hisv(12)

c
c set material constant arrays
c do 10 j=1,48
10 cmau(j)=cm(j)
c
c lhist=nint(xlhist)

```

```

itype=nint(xtype)
fail=nint(xfail)
c
c extra time history output - setup
c initialization of some variables
c
  if(time.eq.0.)then
    tmext=0.
    deltat=delt
    hisv(1)=0.0
    hisv(6)=cm(3)
c
c initialization part
  if (sin.eq.0.0)then
c
    failure=.false.
    cm(16)=(3.*cm(9)-2.*cm(1))/2.*(cm(1)+6.*cm(9))
    cm(15)=cm(1)*(2.+2.*cm(16))
    if (cm(13).eq.0.)cm(13)=1.
    if (cm(10).eq.0.0)cm(10)=1.0
    if (cm(14).eq.0.0)cm(14)=1.0
    cm(10)=cm(10)*cm(14)
    cm(32)=1.0
c
    cmat(10)=cm(10)
    cmat(13)=cm(13)
    cmat(14)=cm(14)
    cmat(15)=cm(15)
    cmat(16)=cm(16)
    cmat(20)=cm(20)
    cmat(21)=cm(21)
    cmat(22)=cm(22)
    cmat(32)=cm(32)
    trvi=0.
    hisv(11)=0.
    hisv(12)=0.
    hisv(13)=0.
    hisv(15)=0.
    hisv(16)=0.
    hisv(19)=1.0
    tmp(i)=cm(33)
    if (fail.gt.1) then
      open(76,file='fail.dat',status='unknown',
1    form='formatted')
      end if

c set up initial temperature for Johnson-Cook
    if(itype.eq.3)then
      if(t0.lt.tref)cm(33)=tref
      cmat(33)=cm(33)
      write(*,'(/,a,a/,a,e12.6,a,e12.6/)')
1    ' Warning: Initial temperature reset to equal',
2    ' reference temperature', tref = ', tref,' t0 = ',t0
      end if
    end if

    if(thist.gt.0)then
      open(77,file='combined.dat',status='unknown',
1    form='formatted')
      open(81,file='tnax.dat',status='unknown',
1    form='formatted')

```

```

        open(82,file='f.dat',status='unknown',
1       form='formatted')
        open(83,file='edot.dat',status='unknown',
1       form='formatted')
        open(85,file='temp.dat',status='unknown',
1       form='formatted')
        end if
        hisv(2)=t0
    end if

c
c
c resetting the number of elements
    if (time.gt.ttvi) then
        idl=0
        ttvi=time
    end if

c
    g0=g0*(Gint+Gslope*(tmp(1)+273))
    g=dt1*g0
    g2=2.0*g
    g3=3.*g0
    qlq24=.25*ql*q2
    qlq23=.15*ql*q2
    q223=.15*q2
    bk3=3.*bk
    niter=nint(xiter)
    cnucl=fnucl/(2.506628275*snucl)
    bkdt=dt1*bk

c
c power law hardening code:
c
    if(itype.eq.2)then
        ninv=1./qh
        ninvl=ninv-1.
    end if

c
c compute trial elastic stresses - store as total deviatoric components in
c strlal-6 and incremental components in dsig1-6
c compute deviatoric strain rates
c
    dd(i)=-(d1+d2+d3)
    davg(i)=dd(i)*.333333333333333
    d1d(i)=d1+davg(i)
    d2d(i)=d2+davg(i)
    d3d(i)=d3+davg(i)
    dsig1(i)=g2*d1d(i)
    dsig2(i)=g2*d2d(i)
    dsig3(i)=g2*d3d(i)
    dsig4(i)=g*d4
    pold(i)=-(sig(1)+sig(2)+sig(3))/3.
    strlal(i)=pold(i)+sig(1)+dsig1(i)
    strlal2(i)=pold(i)+sig(2)+dsig2(i)
    strlal3(i)=pold(i)+sig(3)+dsig3(i)
    strlal4(i)=sig(4)+dsig4(i)

c
c update elastic pressure (ptrial)
c update volume strain used for eos
c
    dpres(i)=bkdt*dd(i)
    hisv(5)=hisv(5)-dd(i)*dt1
    20 ptrial(i)=pold(i)+dpres(i)

```

```

c
c compute flow stress (ak)
c
call gflow(i,g3,hisv,ninv,ninv1)
if (amin1(ak(i),sj2(i)).gt.sig1.and.itype.eq.6) then
  hisv(15)=ddsig
end if
ak(i)=ak(i)+hisv(15)
c New fail criteria: dln(er)/de
if (itype.eq.3) then
  tstar=amax1(0.0001,tum)/(tmelt-tref)
  tstar2=0.0001/(tmelt-tref)

  sig1=((qs+qh*(hisv(1)+0.0001)**qn)*(1+qc*effs(i))*(-qm*
1  tstar**(qm-1)))/(tmelt-tref)
  hisv(20)=sig1
  siger1=qc*(qs+qh*(hisv(1)+0.0001)**qn)*(1-tstar**qm)/hisv(17)
  hisv(21)=siger1
  sig1=(qn*qh*(hisv(1)+0.0001)**(qn-1))*(1+qc*effs(i))*
1  (1-tstar**qm)
  hisv(22)=sig1
  hisv(16)=-((sig1/siger1)-((ak(i)*sig1)/(cp*siger1)))/hisv(17)
  pram1=-((qn*qh*(hisv(1)+0.0001)**(qn-1))*(1+qc*effs(i)))/(qc*
1  (qs+qh*(hisv(1)+0.0001)**qn))+ak(i)*(1+qc*effs(i))*qm*
2  tstar**(qm-1)/((tmelt-tref)*(1-tstar**qm)*qc*cp)

  sig2=((qs+qh*(hisv(1)+0.0001)**qn)*(1+qc*effs(i))*(-qm*
1  tstar2**(qm-1)))/(tmelt-tref)
  hisv(23)=sig2
  siger2=qc*(qs+qh*(hisv(1)+0.0001)**qn)*(1-tstar2**qm)/hisv(17)
  hisv(24)=siger2
  sig2=(qn*qh*(hisv(1)+0.0001)**(qn-1))*(1+qc*effs(i))*
1  (1-tstar2**qm)
  hisv(25)=sig2
  hisv(18)=-((sig2/siger2)-((ak(2)*sig2)/(cp*siger2)))/hisv(17)
  pram2=-((qn*qh*(hisv(1)+0.0001)**(qn-1))*(1+qc*effs(i)))/(qc*
1  (qs+qh*(hisv(1)+0.0001)**qn))+ak(2)*(1+qc*effs(i))*qm*
2  tstar2**(qm-1)/((tmelt-tref)*(1-tstar2**qm)*qc*cp)
end if

if (itype.eq.6) then

  sig1=epsf(i)*(-phi3+phi4*effs(i))
  siger1=phi4*(tmp(i)+273)*epsf(i)/hisv(17)
  sig1=q*phi5*(hisv(1)+0.0001)**(q-1)
  hisv(16)=-((sig1/siger1)-((ak(i)*sig1)/(cp*siger1)))/hisv(17)
  pram1=-((q*phi5*(hisv(1)+0.0001)**(q-1))/(phi4*(tmp(i)+273)
1  *epsf(i))+ak(i)*(phi3-phi4*effs(i))/(phi4*cp*(tmp(i)+273))

  sig2=epsf(2)*(-phi3+phi4*effs(i))
  siger2=phi4*(tref+273)*epsf(2)/hisv(17)
  sig2=q*phi5*(hisv(1)+0.0001)**(q-1)
  hisv(18)=-((sig2/siger2)-((ak(2)*sig2)/(cp*siger2)))/hisv(17)
  pram2=-((q*phi5*(hisv(1)+0.0001)**(q-1))/(phi4*(tref+273)
1  *epsf(2))+ak(2)*(phi3-phi4*effs(i))/(phi4*cp*(tref+273))
end if

if (itype.eq.7) then
  sig1=epsf(i)*(-phi3+effs(i))+(phi5*(hisv(1)+0.0001)**q+phi6)*Gslope
  hisv(20)=sig1
  siger1=epsf(i)*phi4*(tmp(1)+273.0)/hisv(17)

```

```

hisv(21)=siger1
sige1=q*phi5*(hisv(1)+0.0001)**(q-1)*(Gint+Gslope*(tmp(i)+273.0))
hisv(22)=sige1
hisv(16)=(-(sige1/siger1)-((ak(i)*sig1/(cp*siger1)))/hisv(17)

sig2=epsf(2)*(-phi3+effs(i))+(phi5*(hisv(1)+0.0001)**q+phi6)*Gslope
hisv(23)=sig2
siger2=epsf(2)*phi4*(tref+273.0)/hisv(17)
hisv(24)=siger2
sige2=q*phi5*(hisv(1)+0.0001)**(q-1)*(Gint+Gslope*(tref+273.0))
hisv(25)=sige2
hisv(18)=(-(sige2/siger2)-((ak(2)*sig2/(cp*siger2)))/hisv(17)
end if
c
idl=idl+1
c
c Mises:
c
aj2(i)=.50*(strial1(i)**2+strial2(i)**2+strial3(i)**2)+
l   strial4(i)**2
sj2(i)=sqrt(3.*aj2(i))
sj2(i)=amax1(1.e-30,sj2(i))
scale(i)=ak(i)/sj2(i)
dep(i)=0.
if(scale(i).lt.1.0)then
  strial1(i)=scale(i)*strial1(i)
  strial2(i)=scale(i)*strial2(i)
  strial3(i)=scale(i)*strial3(i)
  strial4(i)=scale(i)*strial4(i)
  dep(i)=(1.-scale(i))*sj2(i)/g3qh(i)
  ep=ep+dep(i)
c
c update plastic work and temperature
c
hisv(4)=hisv(4)+ak(i)*dep(i)
hisv(2)=gamma*hisv(4)/cp+t0
c
c update flow stress if power law hardening
c
if(itype.eq.2)then
  hisv(6)=hisv(6)+dep(i)*hard(i)
else
  hisv(6)=ak(i)
  time2=time
end if
end if
pnew(i)=ptrial(i)
sig(1)=strial1(i)-ptrial(i)
sig(2)=strial2(i)-ptrial(i)
sig(3)=strial3(i)-ptrial(i)
sig(4)=strial4(i)
hisv(14)=sig(4)
c
c calculate flow stress increment/dep ratio
c
c flow stress increment/dep ratio for Johnson-Cook
if (itype.eq.3) then
  tnum=amax1(0.0001,(tmp(i)-tref))
  hisv(5)=ak(i)/cp*(qs+qh*(hisv(1)+0.0001)**qn)*(1.+qc*effs(i))*
l   (qmv/(tmelt-tref))*(tnum/(tmelt-tref))**(qm-1.)
end if

```

```

c   flow stress increment/dep ratio for Zerilli-Armstrong
   if (itype.eq.6) then
     hisv(5)=ak(i)/cp*epsf(i)*(effs(i)-phi3)
   end if

c   flow stress increment/dep ratio for Goldthorpe modified Zerilli-Armstrong
   if (itype.eq.7) then
     hisv(5)=ak(i)/cp*(epsf(i)*(effs(i)-phi3)+
1      (phi5*(hisv(1)+0.0001)**q + phi6)*Gslope)
   end if

   hisv(10)=hard(i)
   hisv(9)=hisv(5)
c   write(77,*) ' thermal: ',hisv(5), ' hardening: ',hard(1)
   hisv(5)=hisv(5)+hard(i)

c
c update the plastic strain
   hisv(1)=ep

c
c   check for failure through thermal instability
c
c   additional fail criteria: none
   if (fail.eq.1) then
     crit1=hisv(5)
     crit2=1.0
     crit3=1.0
   end if

c   additional fail criteria: value of principal shear strain rate
   if (fail.eq.2) then
     crit1=hisv(5)
     crit2=hisv(7)
     crit3=1.0
   end if

c   additional fail criteria: value of an average principal shear strain rate
   if (fail.eq.3) then
     crit1=hisv(5)
     crit2=hisv(11)
     crit3=1.0
   end if

c   additional fail criteria: value of equivalent plastic strain
   if (fail.eq.4) then
     crit1=hisv(5)
     crit2=1.0
     crit3=hisv(1)
   end if

c   additional fail criteria: critical value of an average principal shear
c   strain rate and critical value of equivalent plastic strain
   if (fail.eq.5) then
     crit1=hisv(5)
     crit2=hisv(11)
     crit3=hisv(1)
   end if

   if (fail.eq.6) then
     crit1=1.0
     crit2=hisv(16)
     crit3=1.0

```

```

end if

if (fail.ge.1) then
  if (crit1.le.0.0.and.crit2.ge.
1    critrate.and.crit3.ge.critshear) then
    sig(1)=0.0
    sig(2)=0.0
    sig(3)=0.0
    sig(4)= 0.0000010
    hisv(1)=0.00000101
    failure=.true.
    write(76,*) ' element: ',idl,' failure at time: ',time
  end if
end if

c
c
c output time history data for selected element - use order in results file
c
  if(idl.eq.lhist)then
    if(time.ge.ttnext)then
      shyd=(sig(1)+sig(2)+sig(3))/3.
      seq=sqrt(1.5*((sig(1)-shyd)**2+(sig(2)-shyd)**2+
1      (sig(3)-shyd)**2)+3.*sig(4)**2)
      temp=tmp(i)
      erate=0.4342944819*effs(i)
      triax=shyd/hisv(6)
      write(77,*) time,hisv(1),hisv(2),hisv(3),temp,erate,triax

c debug:
c   write(77,*) 'tmp: ',tmp(i)
c   write(77,*) 'tmpf: ',tmpf(i)
c   write(77,*) 'effs: ',effs(i)
c   write(77,*) 'epsf: ',epsf(i)
c   write(77,*) 'hard: ',hard(i)
c   write(77,*) 'g3qh: ',g3qh(i)
c   write(77,*) 'ak: ',ak(i)
      write(81,*) time,triax
      write(82,*) time,hisv(2)
      write(83,*) time,erate
      write(85,*) time,temp
      ttnext=ttnext+deltat
    end if
  end if

c test for dumping variable
  shyd=(sig(1)+sig(2)+sig(3))/3.
  seq=sqrt(1.5*((sig(1)-shyd)**2+(sig(2)-shyd)**2+
1  (sig(3)-shyd)**2)+3.*sig(4)**2)
  erate=0.4342944819*effs(i)
  triax=shyd/hisv(6)

c
c-----
c output section for the three single element test cases:
c
c lhist = -1 uniaxial tension case from aravas paper
c lhist = -2 volumetric expansion case from aravas paper
c lhist = -3 constrained uniaxial extension - third test case in my paper
c lhist = -4 uniaxial compression test case(s)
c lhist = -5 plane strain shear
c
  if(lhist.ge.0)return
  if(time.eq.0.)then
    ttnext=0.

```



```

    deltat=delat
c
    open(77,file='combined.dat',status='unknown',
    l form='formatted')
    open(81,file='stress.dat',status='unknown',
    l form='formatted')
    open(82,file='f.dat',status='unknown',
    l form='formatted')
    open(83,file='ep.dat',status='unknown',
    l form='formatted')
    open(84,file='ep1.dat',status='unknown',
    l form='formatted')
    open(85,file='plastic.dat',status='unknown',
    l form='formatted')
    open(86,file='rate.dat',status='unknown',
    l form='formatted')
    open(87,file='temp.dat',status='unknown',
    l form='formatted')
    open(88,file='ak(i).dat',status='unknown',
    l form='formatted')
    open(89,file='hisv(6).dat',status='unknown',
    l form='formatted')
    open(90,file='fail.dat',status='unknown',
    l form='formatted')
    end if
    if(time.ge.tmext)then
c
c stress-strain measures - test case specific, eref used to convert
c time to strain measure
c
    if(lhist.eq.-1)then
c
c uniaxial tension problem:
c
    strain=alog(time*refrate+1.)
    stress=sig(2)/refstr
    flow=ak(i)/refstr
    flow2=hisv(6)/refstr
c
    else if(lhist.eq.-2)then
c
c volumetric expansion problem:
c
    strain=3.*alog(time*refrate+1.)
    stress=pnew(1)/refstr
c
    else if(lhist.eq.-3)then
c
c constrained axial extension problem:
c
    strain=alog(time*refrate+1.)
    stress=sig(2)/refstr
c
    else if(lhist.eq.-4)then
c
c uniaxial compression problem:
c
    strain=alog(tt*refrate+1.)
    stress=sig(2)/refstr
c
    else if(lhist.eq.-5)then
c

```

```

c plane strain shear problem:
c
    strain=alog(time*refrate+1.)
    stress=sig(2)/refstr
    flow=ak(i)/refstr
    flow2=hisv(6)/refstr
c
    end if
c
    porous=f(1)
    temp=tmp(1)
c    write(77,*) strain,stress,porous,epl,temp
c debug:
    write(77,*) 'Time: ',time
    write(77,*) 'hisv(5): ',hisv(5)
    write(77,*) 'tmp: ',tmp(i)
    write(77,*) 'tmpf: ',tmpf(i)
    write(77,*) 'effs: ',effs(i)
    write(77,*) 'epsf: ',epsf(i)
    write(77,*) 'hard: ',hard(i)
    write(77,*) 'g3qh: ',g3qh(i)
    write(77,*) 'ak: ',ak(i)
    write(77,*) 'hisv(6): ',hisv(6)
    write(77,*) 'hisv(1): ',hisv(1)
    write(77,*) 'hisv(16): ',hisv(16)
    write(77,*) 'G: ',g0
c
c gnuplot or xmgr:
c
    write(81,*) strain,stress
    write(82,*) strain,porous
    write(83,*) strain,epl
    write(85,*) time,hisv(1)
    write(86,*) time,hisv(17)
    write(87,*) time,temp
    write(88,*) time,flow
    write(89,*) time2,flow2
    write(90,*) time,hisv(16)
c
    tmext=tmext+deltat
    end if
c
c-----
c
    return
end

```

Appendix C
Flow Stress Calculation Program (gflow)

```

      subroutine gflow(i,g3,hisv2,ninv,ninv1)
c
c flow stress calculation for adiabatic shear material model
c
c this routine can be modified to add additional constitutive
c descriptions
c
c Written by M. Worswick, March, 1994
c Modified to be included as a part of user subroutine by P. Pelletier
c Additional modifications made by C.R. Mason, January, 1997
c -----
c Currently 7 material models are supported:
c
c   xtype = 1: linear hardening
c   xtype = 2: power law hardening
c   xtype = 3: Johnson-Cook flow stress (eos and thermal softening
c             not implemented)
c   xtype = 4: cubic polynomial fit to a stress strain curve
c   xtype = 5: Zerilli-Armstrong FCC material
c   xtype = 6: Zerilli-Armstrong BCC material
c   xtype = 7: Goldthorpe modified Zerilli-Armstrong BCC material
c -----
c
c material constants:
c   1   g0   shear modulus
c   9   bk   bulk modulus
c
c   2   xtype key to flow stress model:
c   xtype = 1: linear hardening
c   3   qs   initial yield strength
c   4   qh   hardening modulus
c   xtype = 2: power law hardening:
c   3   qs   initial yield strength
c   4   qh   power law exponent
c   xtype = 3: Johnson-Cook flow stress:
c   3   qs   initial yield strength
c   4   qh   hardening coefficient
c   5   qn   hardening exponent
c   6   qc   strain rate coefficient
c   7   qm   thermal softening coefficient
c   10  cp   specific heat
c   11  tmelt melting temperature
c   12  tref reference temperature
c   13  eref reference strain rate
c   xtype = 4: cubic polynomial fit
c   3   qaa   initial yield strength
c   4   qbb   linear hardening term (B)
c   5   qcc   quadratic hardening term (C)
c   6   qdd   cubic hardening term (D)
c   7   qee   strain rate sensitivity
c   8   qer   reference strain rate
c   xtype = 5: Zerilli-Armstrong FCC flow stress:
c   3   phi0  initial yield strength
c   4   phi2  hardening coefficient
c   5   phi3  thermal softening coefficient
c   6   phi4  strain rate coefficient
c   10  cp   specific heat
c   13  eref reference strain rate
c   33  t0    initial temperature (absolute)
c   xtype = 6: Zerilli-Armstrong BCC flow stress:
c   3   phi0  initial yield strength
c   4   phi1  pre-exponential coefficient

```

```

c   5   phi3   thermal softening coefficient
c   6   phi4   strain rate coefficient
c   7   phi5   hardening coefficient
c   8   q      hardening exponent
c  10   cp      specific heat
c  13   eref    reference strain rate
c  24   phi6    grain size effect  $k/l^{0.5}$ 
c  33   t0      initial temperature (absolute)
c  38   Gamma   amount of work converted into heat
c  40   ddsig   increase in flow stress due to twinning
c  45   dsigG   solute and the original dislocation density
c  48   sigt    twinning stress
c      xtype = 7: Goldthorpes modified Zerilli-Armstrong BCC flow stress:
c   3   phi0    initial yield strength
c   4   phi2    pre-exponential coefficient
c   5   phi3    thermal softening coefficient
c   6   phi4    strain rate coefficient
c   7   phi5    hardening coefficient
c   8   q      hardening exponent
c  10   cp      specific heat
c  13   eref    reference strain rate
c  24   phi6    shock hardening coefficient
c  33   t0      initial temperature (absolute)
c  38   Gamma   amount of work converted into heat
c  40   ddsig   increase in flow stress due to twinning
c  45   dsigG   solute and the original dislocation density
c  48   sigt    twinning stress
c -----
c
c      real ninv,ninvl
c      common/gur38/d4(128)
c      common/gur39/ pold(128)
c      common/gurs1/
c      1 pnw(128),cc(128),scale(128),aj2(128),ak(128),davg(128),
c      2 sj2(128)
c
c      debug:
c
c      common/bk52/xm(128)
c
c      common/gurmau/g0,xtype,qs,qh,qn,qc,qm,xjnk1,
c      1      bk,cp,tmelt,tref,eref,rho0,E,poi,q1,q2,q3,
c      2      toler,xiter,phimax,fail,phi6,enucl,fnucl,snucl,t0,
c      3      fc,ff,visc,sin,t0,cint,s1,s2,s3,gamma,gamma1,
c      4      ddsig,xlhist,delt,refrate,refstr,dsigG,critrate,
c      5      critshear,sigt
c      equivalence (qs,qaa),(qh,qbb),(qn,qcc),(qc,qdd),(qm,qee),
c      1      (xjnk1,qcr)
c      equivalence (qs,phi0),(qh,phi1),(qh,phi2),(qn,phi3),(qc,phi4),
c      1      (qm,phi5),(xjnk1,q),(s2,Gslope),(s3,Gint)
c
c      common/gurson/soft(128),hydro(128),phi(128),eval(128),
c      1      evali(128),qlf(128),q3f2(128),alpha(128),
c      2      gs(128),bkas(128),xa(128),xb(128),xc(128),
c      3      xe(128),dlamb(128),dpdl(128),dla3(128),
c      4      dp1(128),dp2(128),dp3(128),dp4(128),
c      5      dep(128),dfnucl(128),
c      6      tmp(128),tmpf(128),effs(128),
c      7      d1d(128),d2d(128),d3d(128),epsf(128),
c      8      a(128),b(128),hard(128),g3qh(128),rv(128),
c      9      rv2(128),rv3(128),rvi(128)
c

```

```

dimension hisv2(*)
c
c compute flow stress (ak)
c
itype=nint(xtype)
tmp(i)=gamma*hisv2(4)/cp +t0
c linear hardening
if(itype.eq.1)then
  g3qh(i)=g3+qh
  31 ak(i)=qh*hisv2(1)+qs
  else if(itype.eq.2)then
c power law hardening
  hard(i)=g3/(ninvt*(hisv2(6)/qs)**ninvt-1.)
  g3qh(i)=g3+hard(i)
  32 ak(i)=hisv2(6)
  else if(itype.eq.3)then
c Johnson-Cook
  hisv2(1)=hisv2(1)+0.0001
  tnum=amax1(0.0001,(tmp(i)-tref))
c tnum=tmp(i)-tref
  tmpf(i)=1.-(tnum/(tmelt-tref))**qm
  tmpf(2)=1.-(0.0001/(tmelt-tref))**qm
  effs(i)=(2.*(d1d(i)**2+d2d(i)**2+d3d(i)**2)+
  1 d4(i)**2)/3.
  effs(i)=amax1(.0020,sqrt(effs(i))/eref)
  hisv2(17)=effs(i)
  effs(i)=alog(effs(i))
  epsf(i)=tmpf(i)*(1.+qc*effs(i))
  epsf(2)=tmpf(2)*(1.+qc*effs(i))
  hard(i)=qh*qn*hisv2(1)**(qn-1.)*epsf(i)
  g3qh(i)=g3+hard(i)
  ak(i)=(qs+qh*hisv2(1)**qn)*epsf(i)
  ak(2)=(qs+qh*hisv2(1)**qn)*epsf(2)
  hisv2(1)=hisv2(1)-0.0001
  else if(itype.eq.4)then
c polynomial fit
  effs(i)=(2.*(d1d(i)**2+d2d(i)**2+d3d(i)**2)+
  1 d4(i)**2)/3.
  effs(i)=amax1(.0020,sqrt(effs(i))/qer)
  effs(i)=alog(effs(i))
  epsf(i)=1.+qec*effs(i)
  hard(i)=(qbb+hisv2(1)*(2.*qcc+3.*qdd*hisv2(1)))*epsf(i)
  g3qh(i)=g3+hard(i)
  34 ak(i)=(qaa+hisv2(1)*(qbb+hisv2(1)*(qcc+qdd*hisv2(1)))*epsf(i)
  else if(itype.eq.5)then
c Zerilli-Armstrong FCC
  hisv2(1)=hisv2(1)+0.0001
  tmpf(i)=phi3*tmp(i)
  effs(i)=(2.*(d1d(i)**2+d2d(i)**2+d3d(i)**2)+
  1 d4(i)**2)/3.
  effs(i)=amax1(.0020,sqrt(effs(i))/eref)
  effs(i)=phi4*alog(effs(i))
  epsf(i)=phi2*exp(tmpf(i)+effs(i))
  ak(i)=phi0+sqrt(hisv2(1))*epsf(i)
  hard(i)=0.5*epsf(i)/sqrt(hisv2(1))
  g3qh(i)=g3+hard(i)
  hisv2(1)=hisv2(1)-0.0001
  else if(itype.eq.6)then
c Zerilli-Armstrong BCC
  hisv2(1)=hisv2(1)+0.0001
  tmpf(i)=phi3*(tmp(i)+273.0)
  tmpf(2)=phi3*(tref+273.0)

```

```

    effs(i)=(2.*(d1d(i)**2+d2d(i)**2+d3d(i)**2)+
    | d4(i)**2)/3.
    effs(i)=amax1(.0020,sqrt(effs(i))/eref)
    hisv2(17)=effs(i)
    effs(i)=phi4*log(effs(i))
    epsf(i)=phi1*exp(tmpf(i)+(tmp(i)+273)*effs(i))
    epsf(2)=phi1*exp(tmpf(2)+(tref+273)*effs(i))
    ak(i)=dsigG+epsf(i)+phi5*(hisv2(1))**q+phi6
    ak(2)=dsigG+epsf(2)+phi5*(hisv2(1))**q+phi6
    hard(i)=q*phi5*(hisv2(1))**(q-1.)
    g3qh(i)=g3+hard(i)
    hisv2(1)=hisv2(1)-0.0001
    else if(itype.eq.7)then
c Goldthorpe modifies Zerilli-Armstrong BCC
    hisv2(1)=hisv2(1)+0.0001
    tmpf(i)=phi3*(tmp(i)+273.0)
    tmpf(2)=phi3*(tref+273.0)
    effs(i)=(2.*(d1d(i)**2+d2d(i)**2+d3d(i)**2)+
    | d4(i)**2)/3.
    effs(i)=amax1(.0020,sqrt(effs(i))/eref)
    hisv2(17)=effs(i)
    effs(i)=phi4*log(effs(i))
    epsf(i)=phi2*exp(tmpf(i)+(tmp(i)+273.0)*effs(i))
    epsf(2)=phi2*exp(tmpf(2)+(tref+273.0)*effs(i))
    ak(i)=phi0+epsf(i)+(phi5*(hisv2(1))**q+phi6)*
    | (Gint+Gslope*(tmp(i)+273.0))
    ak(2)=phi0+epsf(2)+(phi5*(hisv2(1))**q+phi6)*
    | (Gint+Gslope*(tref+273.0))
    hard(i)=q*phi5*(hisv2(1))**(q-1.)*(Gint+Gslope*(tmp(i)+273.0))
    g3qh(i)=g3+hard(i)
    hisv2(1)=hisv2(1)-0.0001
    end if
c
    return
end

```

Appendix D
Nominal Shear Stress-Strain Calculation


```

character*20 name1
character*20 crap
character*72 junk
integer i,j,ps
real time(1000),topst(1000,100),bottomst(1000,100),topr(1000,100)
real bottomr(1000,100),topf(1000,100),bottomf(1000,100),topt,bottomt
real stress(1000),forceavg(1000),strain(1000),l, pts,delta(1000)
real totstress(1000),avgstress(1000)
parameter fact=31415926.0, pi=3.14159265359

c Number of plot states
print *, 'Enter the number of plot states'
read *,ps

c Type of test
print *, 'Is this a compression(1) or shear(2) test?'
read *,typ

if (typ.eq.1) then

    telem=30.0
    mode=31.0
    belem=30.0
    bnode=31.0

    print *, 'Is this a standard test, t1=0.1250, d1=0.2500:'
    print *, '(Yes=1 or No=2)'
    read *,cst

    if (sst.eq.2) then
        print *, 'Enter gauge length (inches):'
        read *,t1

        print *, 'Enter specimen diameter (inches):'
        read *,d1
    else
        t1=0.1250
        d1=0.250
    end if
    aspec=d1**2*pi/4*25.4**2
    l=t1*0.0254

else

    telem=14.0
    mode=15.0
    belem=20.0
    bnode=21.0

    print *, 'Is this a standard test, d1=0.338, d2=0.328, t3=0.05:'
    print *, '(Yes=1 or No=2)'
    read *,sst

    if (sst.eq.2) then
        print *, 'Enter inner ring diameter (inches): '
        read *,d1

        print *, 'Enter punch diameter (inches): '
        read *,d2

        print *, 'Enter specimen thickness (inches): '

```

```

        read *, t3

    else
        d1=0.338
        d2=0.328
        t3=0.050
    end if
    aspec=(d1+d2)*25.4**2*pi*t3/2
    l=(d1-d2)*0.0254
end if

print *, 'Number of points of average:'
read *, pts
pts2=pts/2 + 0.5
pts3=pts2-1

open (unit=100,file='all',status='old')
open (unit=300,file='topf.dat',status='new')
open (unit=301,file='bottomf.dat',status='new')
open (unit=302,file='forceavg.dat',status='new')
open (unit=303,file='stress.dat',status='new')
open (unit=304,file='stress.vs.strain.dat',status='new')
open (unit=305,file='avgstress.vs.strain.dat',status='new')

do 10 i=1,9
    read (100,fmt=900) junk
10  continue

c    read (100,fmt=930) ps

    do 20 i=1,ps
        read (100,fmt=910) time(i),topst(i,1)
        totstress(i)=0.0
20  continue

    do 50 i=2,telem
        do 30 j=1,9
            read (100,fmt=900) junk
30      continue
        do 40 j=1,ps
            read (100,fmt=910) crap,topst(j,i)
40      continue
50      continue

        do 80 i=1,belem
            do 60 j=1,9
                read (100,fmt=900) junk
60          continue
            do 70 j=1,ps
                read (100,fmt=910) crap,bottomst(j,i)
70          continue
80          continue

            do 110 i=1,tnode
                do 90 j=1,9
                    read (100,fmt=900) junk
90          continue
                do 100 j=1,ps
                    read (100,fmt=910) crap,topr(j,i)
100         continue
110        continue

```

```

do 140 i=1,bnode
  do 120 j=1,9
    read (100,fmt=900) junk
120   continue
    do 130 j=1,ps
      read (100,fmt=910) crap,bottomr(j,i)
130   continue
140   continue

    do 150 j=1,6
      read (100,fmt=900) junk
150   continue
    do 160 j=1,ps
      read (100,fmt=910) crap,delta(j)
160   continue

    do 180 i=1,ps
      do 170 j=1,telem
        topf(i,j)=topst(i,j)*fact*(topr(i,j+1)**2-topr(i,j)**2)
        topt=topt+topf(i,j)
170   continue
      topf(i,tnode)=topt*-1
      topt=0.0
      write (300,fmt=910) time(i),topf(i,tnode)
180   continue

    do 200 i=1,ps
      do 190 j=1,belem
        bottomf(i,j)=bottomst(i,j)*fact
190   *(bottomr(i,j+1)**2-bottomr(i,j)**2)
        bottomt=bottomt+bottomf(i,j)
      bottomf(i,bnode)=bottomt*-1
      bottomt=0.0
      write (301,fmt=910) time(i),bottomf(i,bnode)
200   continue

    do 210 i=1,ps
      forceavg(i)=(topf(i,tnode)+bottomf(i,bnode))/2
      stress(i)=forceavg(i)/aspec
      strain(i)=delta(i)/(-1*100)
      write (302,fmt=910) time(i),forceavg(i)
      write (303,fmt=910) time(i),stress(i)
      write (304,fmt=910) strain(i),stress(i)
210   continue

    do 230 i=pts2,ps-pts3
      do 220 j=i-pts3,i+pts3
        totstress(i)=totstress(i)+stress(j)
220   continue
      avgstress(i)=totstress(i)/pts
      write (305,fmt=910) strain(i),avgstress(i)
230   continue

900   format (a)
910   format (e20.11,e20.11)
920   format (f20.10,f20.10)
930   format (f5.0)
end

```

Appendix E
Velocity Calculation from Incident Pulse

```

Character*20 name0
Character*10 crap
Character*72 junk
Integer I,J,count,bridge,type
Real inc(2,20000),refl(2,20000),tran(2,20000),gauge(2,10),excit
Real Fact1,Fact2,Fact3,ET,TFact1,a,QA,QB,QC,incvel,reflvel,tranvel
Real closvel
Parameter Young=2.0685E+05,Abarc=126.6769,mass=0.304522,dt=2.0E-07

```

```
count=0.0
```

c Opening files

```

open (unit=301,file='velinfo.dat',status='new')
open (unit=302,file='inc.dat',status='new')
open (unit=303,file='refl.dat',status='new')
open (unit=304,file='trans.dat',status='new')

```

c Prompting for the input data

```

print *, 'Enter the name of the file with the bridge data:'
read (*,900), name0
print *, 'Enter the excitation voltage:'
read *,excit

open (unit=101,file=Name0,status='old')
do 10 I=1,5
  read (101,fmt=900) junk
10  continue

do 30 I=1,2
  do 20 J=1,6
    read (101,fmt=910) crap,gauge(I,J)
20  continue
30  continue

```

c Opening files with sectioned incident, reflected, and transmitted pulses

```

open (unit=100,file='inc.exp',status='old')
open (unit=101,file='refl.exp',status='old')
open (unit=102,file='tran.exp',status='old')

```

c Reading incident, transmitted, and reflected pulse data into arrays

```

do 40 I=1,20000
  read (100,*,end=50) inc(1,I),inc(2,I)
  read (101,*,end=50) refl(1,I),refl(2,I)
  read (102,*,end=50) tran(1,I),tran(2,I)
  count=count+1
40  continue

do 60 I=1,count
  inc(2,I)=inc(2,I)*1000
  refl(2,I)=refl(2,I)*1000
  tran(2,I)=tran(2,I)*1000
60  continue

```

c Calculating constants

```

a=(gauge(1,6)/gauge(1,2)+gauge(1,1)/gauge(1,5))/2
Fact2=Young*dt*abarc/(mass*vel)
Fact3=Young*dt*abarc/mass

```

c Calculating incident strain from strain gauge data

```

do 70 I=1,count
  QA=(inc(2,I)/excit-1)*gauge(1,3)*gauge(1,4)
  QB=(inc(2,I)*(1+a)/(excit*a)*(gauge(1,4)+a*gauge(1,3))
  I -gauge(1,4)-gauge(1,3)
  QC=inc(2,I)/(excit*a)*(1+a)**2
  E=(-QB-sqrt(QB**2-4*QA*QC))/(2*QA)
  write (302,*) inc(1,I),E
  ET=ET+E
70 continue

```

c Calculating incident velocity

```

incvel=Fact3*ET
ET=0.0

```

c Calculating reflected strain from strain gauge data

```

do 80 I=1,count
  QA=(refl(2,I)/excit-1)*gauge(1,3)*gauge(1,4)
  QB=(refl(2,I)*(1+a)/(excit*a)*(gauge(1,4)+a*gauge(1,3))
  I -gauge(1,4)-gauge(1,3)
  QC=refl(2,I)/(excit*a)*(1+a)**2
  E=(-QB-sqrt(QB**2-4*QA*QC))/(2*QA)
  write (303,*) refl(1,I),E
  ET=ET+E
80 continue

```

c Calculating reflected velocity

```

reflvel=Fact3*ET
ET=0.0

```

c Calculating transmitted strain from strain gauge data

```

do 90 I=1,count
  QA=(tran(2,I)/excit-1)*gauge(1,3)*gauge(1,4)
  QB=(tran(2,I)*(1+a)/(excit*a)*(gauge(1,4)+a*gauge(1,3))
  I -gauge(1,4)-gauge(1,3)
  QC=tran(2,I)/(excit*a)*(1+a)**2
  E=(-QB-sqrt(QB**2-4*QA*QC))/(2*QA)
  write (304,*) tran(1,I),E
  ET=ET+E
90 continue

```

c Calculating transmitted velocity

```

tranvel=Fact3*ET

closvel=incvel-reflvel-tranvel

```

c Writing to files

```

write (301,*) 'Incident velocity: ',incvel
write (301,*) 'Reflected velocity: ',reflvel
write (301,*) 'Transmitted velocity: ',tranvel
write (301,*) 'Closure velocity: ',closvel

```

```

900 format (A)
910 format (A10,F10.5)
920 format (I2)
930 format (F10.5)

```

```

end

```