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ABSTRACT

Two new techniques for investigating the thermal skin-burn potential of airbags are presented. A reduced-volume airbag test procedure has been developed to obtain airbag pressures that are representative of a dynamic ride-down event during a static deployment. Temperature and heat flux measurements made with this procedure can be used to predict airbag thermal burn potential. Measurements from the reduced-volume procedure are complemented by data obtained using two gas-jet simulators, called heatguns. Gas is vented in controlled bursts from a large, heated, pressurized tank of gas onto a target surface. Heat flux measurements on the target surface have been used to develop quantitative models of the relationships between gas jet characteristics and burn potential.

INTRODUCTION

Many contemporary airbag designs use rapid combustion of a solid generant to produce the inflation gas. Depending on the generant and the inflator design, the temperature of the inflation gas can reach several hundred degrees Celsius as it is vented from the airbag. With some airbag designs, the temperature and velocity of the vented gas are sufficient to cause thermal burns to skin surfaces exposed to the gas.

Field data indicate that thermal skin burns occur in a small percentage of airbag deployments (1).¹ Reinfurt et al. (2) found that approximately 7 percent of drivers who experienced a steering-wheel airbag deployment reported a thermal burn. Reed et al. (3) conducted laboratory experiments with human volunteers to develop a mathematical model of the skin burn process for application to airbag-induced skin burns. Combining the skin burn model with a gas-dynamics model of airbag deployment gave accurate prediction of burn potential for airbags in static deployments.

Recently, trends in airbag system design have led to increased interest in airbag skin-burn issues. Reductions in inflator size and changes in generant formulations have increased inflation gas temperatures in some cases, potentially increasing burn risk. Uncoated airbag fabrics raise the possibility of burns due to gas flow through fabric or via contact with heated fabric.

This paper presents recent advances in laboratory equipment and methods for studying issues relating to airbag-induced skin burns. Commercially available instrumentation has been adapted to make useful measurements of airbag exhaust gas temperature and heat transfer to a target surface. Gas jet simulators, or heatguns, have been developed to produce high-velocity, high-temperature gas jets in the laboratory. The heatguns have been used to develop new mathematical models to predict the heat flux resulting from airbag exhaust gas impinging on the skin. For testing airbag systems, a new approach that uses a reduced airbag volume to obtain dynamic-equivalent exhaust gas velocities has been developed. This method allows verification of simulation results using inexpensive static deployments.

METHODS

INSTRUMENTATION – When airbags are deployed to study thermal burn potential, three parameters are commonly measured. The airbag internal pressure provides a useful record of the internal airbag state during the deployment, and is used to verify inflator performance. Internal pressure is monitored with a transducer mounted on a tap located just in front of the airbag inflator. Thermocouples are used to measure the exhaust gas temperature, and fast-response heat-flux meters are placed in the path of the airbag exhaust gas to measure the thermal flux.

The short duration of airbag deployments makes it difficult to obtain accurate gas temperature measurements. The frequency bandwidth of thermal transducers is related to the heat capacity of the sensing elements. The frequency response of thermocouples, for example, is limited by the size of the thermocouple junction. In practice, this means that thermocouples made from extremely

1. Numbers in parentheses denote references at the end of the paper.

fine-gage wire are necessary to record temperatures that change rapidly. For airbag exhaust gases, K-type thermocouples constructed with 0.0127-mm-diameter wire have been found to be adequate. The Appendix details some calculations demonstrating that the expected time constant for these fine-wire thermocouples can be expected to be about two milliseconds when used to measure airbag exhaust gas temperature.

Although gas temperature measurements are important, the heat transfer to a surface located in the airbag exhaust gas flow is more directly related to burn potential. In recent testing at UMTRI, a commercially available thermal flux sensor has been applied to airbag exhaust gas measurements. The Vatell heat flux meter (HFM) is comprised of two thin-film thermopiles separated by a substrate with known thermal conductivity, as illustrated in Figure 1. As heat is transferred into or out of the sensor, the temperature difference across the substrate is proportional to the heat flux. A thin-film resistance temperature gage on the surface of the sensor allows the flux measurement to be corrected for variation in the conductivity of the substrate with temperature. The thin-film design allows the gage to respond very rapidly, with time constants estimated at approximately 10 microseconds. The HFM can be used to measure radiative, convective, or conductive thermal flux, and is small enough (4-mm-diameter sensing surface) to be adapted to many experimental conditions. The HFM is used in tests with gas jet simulators and in testing with airbags.

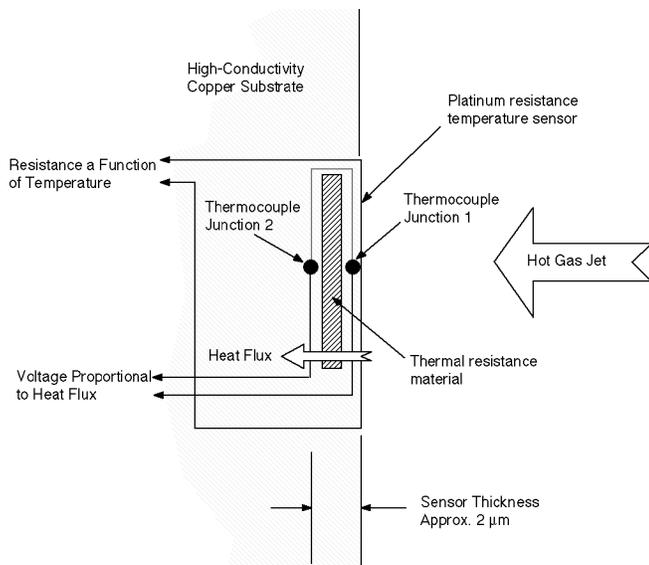


Figure 1. Schematic of Vatell HFM.

GAS JET SIMULATORS – Testing to determine the heat transfer and burn potential associated with high-temperature, high-velocity gas jets has been conducted using gas jet simulators, known as heatguns. Two systems have been developed at UMTRI for conducting testing with human volunteers and with thermal measurement instrumentation.

The room-air heatgun, shown in Figure 2, is based on a modified commercial heatgun. Air is drawn in by a blower motor and passed over two electric heaters before exiting through a straight run of constant-diameter tubing. The thick-walled tubing maintains the air temperature up to the outlet. Static and dynamic pressure ports near the outlet are used to monitor the speed of the gas jet. Testing has been conducted with outlet diameters ranging from 5 to 20 mm, gas speeds up to 50 m/s, and temperatures up to 550 °C. A fast-acting shutter assembly was developed to assess the rate of heat flux development with the onset of hot gas flow. An electric motor spins an activation cam that is manually brought into contact with a lightweight shutter. The target surface is covered by the shutter prior to the test, and exposed fully to the gas jet in about one millisecond. The room-air heatgun was used in testing with human volunteers to determine skin burn thresholds (2), and has been used more recently to develop new models to predict heat transfer as a function of gas jet geometry and properties.

The compressed-gas heatgun, shown in Figure 3, was developed to facilitate testing at higher gas jet speeds than are possible with the room-air heatgun, and to allow the testing of alternative gas formulations. The compressed-gas heatgun is comprised of a 10-ft³ stainless steel tank capable of sustained operation at 2 MPa and 550 °C. Sixteen 1200-watt electrical heater bands connected through a computer controller maintain the gas in the tank at the desired temperature. Ports are provided for monitoring the temperature and pressure in the tank, and for injecting gas or water vapor.

Figure 4 shows a schematic of the compressed-gas heatgun. A 3-mm orifice at the tank outlet restricts the flow gas from the tank. This orifice can be changed to allow testing with higher gas flow rates. The sonic flow conditions across the orifice make the mass flow from the tank independent of downstream pressure, which simplifies the adjustments required to obtain a desired gas jet speed. When a pneumatically actuated main valve is opened, gas flows through the orifice into a heated length of pipe. A bleed valve connected to the pipe allows the gas speed through the outlet orifice to be controlled by diverting some of the flow. Adjustments of the bleed valve based on tank pressure allow constant gas speeds to be maintained for a series of test exposures as the tank pressure decreases. A 450-mm run of straight, heated, thick-walled tubing precedes the outlet to allow turbulent flow to develop fully. Although this does not necessarily replicate the gas flow upstream of an airbag exhaust vent, it provides a consistent test condition.

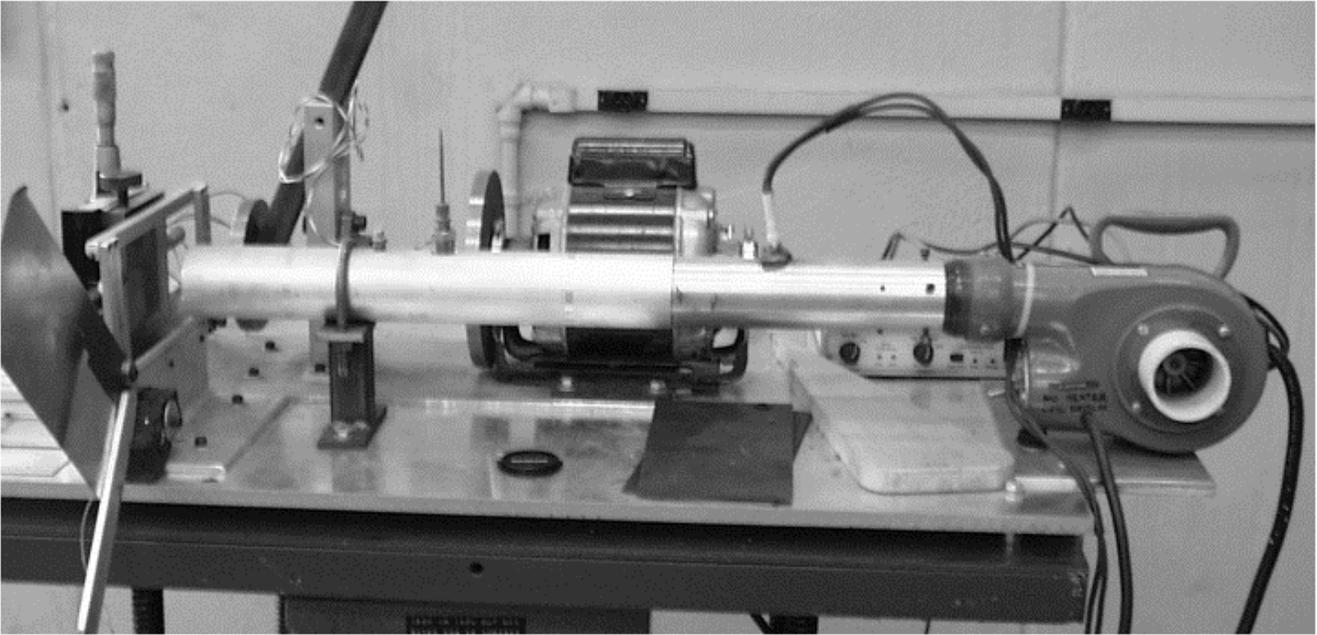


Figure 2. Room-air heatgun with fast-acting shutter system.



Figure 3. Compressed-gas heatgun.

An adjustable platform has been constructed for mounting a heat flux meter in front of the heatgun outlet. In recent testing, a large number of measurements were made to determine the influence of outlet diameter, gas speed, temperature, target distance, and off-axis spacing on the heat flux to the target. The compressed-gas heat-

gun has also been used to investigate the effects of water vapor content on heat transfer from hot gases. With an additional fitting to attach porous fabric to the outlet, the heatgun has also been used to measure the heat flux produced by gas flow through fabric.

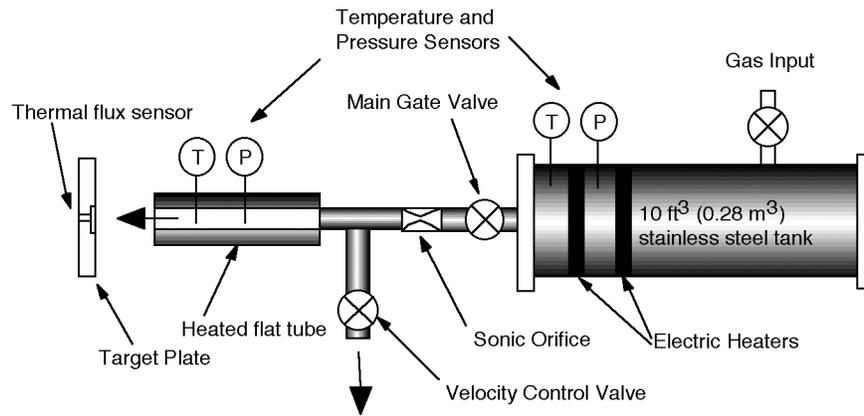


Figure 4. Schematic of compressed-gas heatgun.

REDUCED VOLUME STATIC TESTING – Static airbag deployments without occupant interaction are easier and less expensive to conduct than dynamic tests, but have important limitations for airbag-induced skin burn assessment. During a static deployment, the internal airbag pressure is typically lower than during a dynamic test with occupant ridedown. The higher pressures in a dynamic test produce higher airbag exhaust gas exit speeds and higher burn potential.

A simple procedure has been developed to obtain results that are reasonably representative of thermal exposures from dynamic tests while maintaining the simplicity of a static deployment. Testing of an airbag module is conducted using the same inflator but with a reduced-volume airbag. The airbag volume is selected, based on simulation and test results, so that the peak internal pressure is approximately the same as that expected during a dynamic test. Because of differences in the thermodynamics of the static and dynamic tests, slightly different gas temperatures are obtained even when the pressures match. However, the overall character of the thermal exposure at the exhaust vents is similar to a dynamic test. This reduced-volume procedure allows a more representative assessment of airbag burn risk using a static test than is possible with a normally configured airbag.

Figure 5 shows a schematic of the laboratory apparatus for reduced-volume testing. The airbag is configured with the discrete vents, or porous fabric venting area, located on the front surface of the airbag (opposite the inflator). This allows greater flexibility in positioning thermal instrumentation with respect to the vent holes than if the vents were in their typical position on the rear airbag panel. The airbag is constructed with 250-mm internal tethers that help to control the deployment kinematics. The airbag inflator is mounted to the top plate of the fixture, with the airbag between two plates separated by about 225 mm. No airbag module is used, but rather the airbag is bunched tightly in a columnar form and taped in place prior to the deployment. While this alters the deployment character somewhat, because no energy is absorbed in rupturing a module cover, the resulting kinematics consistently locate the vent ports relative to the thermal instrumentation.

When the inflator is triggered, the front surface of the airbag is pressed against the lower plate and held in position for the duration of the deployment. The gas flow through the vents passes through a hole in the lower plate and onto the test instrumentation. In a typical test, the exhaust gas flows past fast-response thermocouples onto copper blocks containing HFM thermal flux sensors. The vertical position of the HFM mounting blocks is adjusted to control the spacing between the airbag exhaust vents and the target surface. Data from the HFMs and thermocouples are recorded for analysis.

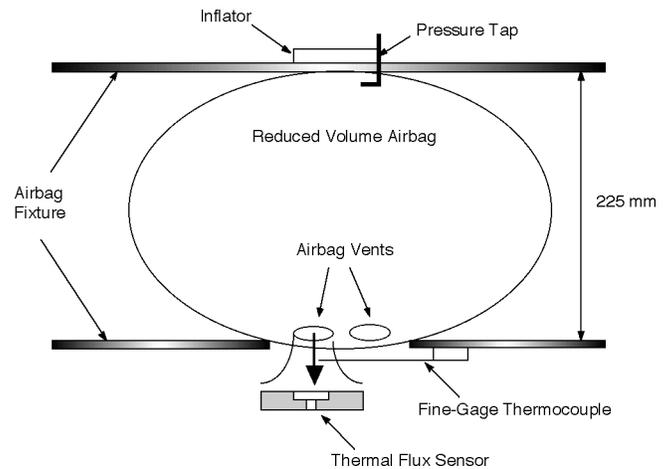


Figure 5. Schematic of laboratory apparatus for reduced-volume testing (side view).

RESULTS

The new laboratory methods described above have been used to study a variety of issues relating to thermal burns produced by airbag exhaust gases. This section presents some typical results obtained using the reduced-volume static test procedure.

Deployments were conducted comparing two different inflators. Each was fitted to an identical airbag with a volume of approximately 40 liters and two 40-mm-diameter discrete vents. A Vatel HFM thermal flux sensor was placed 80 mm (two diameters) from the vents, in a position expected to be at the center of the gas jet during deployment. Figure 6 shows thermal flux versus time for

deployments with two different airbag inflators. The two inflators produce considerably different peak fluxes and different total heat transfer to the surface. Over a 100-ms interval, the average thermal flux from the test with inflator A was 434 kW/m^2 , while the average for inflator B was 126 kW/m^2 .

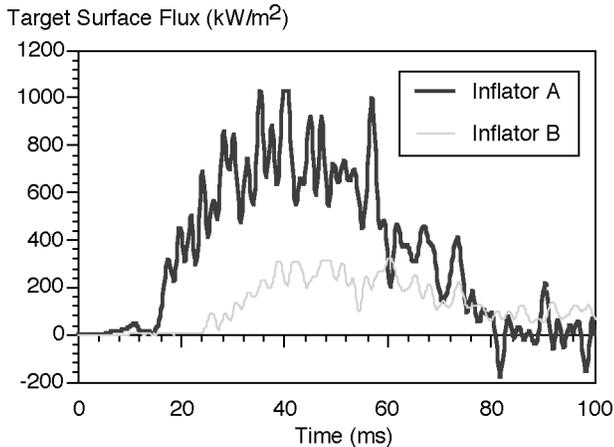


Figure 6. Thermal flux on a target surface produced by gas jets from airbags with two different inflators.

The thermal flux data were input to the UMTRI Airbag Skin Burn Model (3) to assess the potential for skin burn for the measured thermal exposures. Inflator A was predicted to produce a skin burn depth of $294 \mu\text{m}$, while inflator B, with lower thermal flux, was predicted to produce a burn depth of $40 \mu\text{m}$. In previous work (3), the minimum burn depth required to produce a second-degree burn was identified as $80 \mu\text{m}$, corresponding to the average depth of the basal epidermal layer. Using a burn depth of $80 \mu\text{m}$ as the threshold for a second-degree burn, the thermal exposure produced in these test conditions by inflator A is predicted to produce a skin burn, while the exposure produced by inflator B is below the thermal burn threshold.

Additional testing with other inflators and test conditions has suggested that it may be possible to define a quantity of energy transfer per unit area that corresponds to the threshold for burn risk under airbag deployment conditions. The exposure duration is generally a critical parameter affecting burn potential, but the maximum duration of hot gas flow is constrained under airbag deployment conditions to a maximum of approximately 100 to 150 ms, depending on the airbag system design. Preliminary analysis suggests that an average heat flux rate of 200 kW/m^2 for 100 ms, or a total heat transfer of 20 kJ/m^2 during the exposure, is a reasonable estimate of the threshold for airbag-induced second-degree burn. Use of a threshold flux criterion would eliminate the need to input the thermal flux data to the UMTRI Airbag Skin Burn Model to assess the burn risk. Further experimentation will be necessary to determine if a simple procedure for processing the flux signals is an acceptable substitute for the analysis using the burn model in this type of testing.

DISCUSSION AND CONCLUSIONS

The research methods described in this paper have been developed to study the skin burn potential of hot airbag exhaust gases. The reduced-volume test procedure provides an inexpensive way to evaluate the burn potential of an airbag system. The heatguns have been used for extensive laboratory testing of the thermal characteristics of hot gas jets. The room-air heatgun was used to investigate convection burn thresholds with human subjects (3), and the compressed-gas heatgun has recently been used to develop a new model to describe the heat transfer from a gas jet to the skin as a function of gas jet velocity, temperature, diameter, and other factors.

Data from these laboratory methods are used with the UMTRI Airbag Skin Burn Model (ASBM), a computer program that simulates heat transfer into the skin and calculates the resulting potential for burn injury. The original model was based on burn sensitivity experiments conducted with human volunteers (3). A recent update of the model allows input of surface heat flux data from the reduced-volume test procedure directly into the simulation. The model predicts the burn severity that would have resulted from the measured thermal exposure. The model can also be used with data from airbag deployment simulations to predict burn potential, as described above. Together, these laboratory and simulation tools provide the means to incorporate assessments of airbag thermal burn potential into the airbag system design process.

ACKNOWLEDGMENTS

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APPENDIX

ESTIMATING THE RESPONSE TIME FOR FINE-WIRE THERMOCOUPLES IN AN AIRBAG EXHAUST GAS STREAM

Thermocouple response time is related to the heat capacity of the thermocouple junction and the rate of heat transfer to the thermocouple. This appendix presents sample calculations estimating the time constant for a thermocouple placed in the gas jet at an airbag exhaust vent. These calculations are rough approximations, and should not be considered to be accurate for all applications. They do, however, demonstrate that it is feasible to use thermocouples to measure airbag exhaust gas temperatures.

The time constant for the response of a spherical thermocouple junction to a step change in external conditions is given by

$$\tau = \frac{\rho c D}{6h} \quad (1)$$

where τ is the thermal time constant, ρ is the density of the thermocouple junction, c is the specific heat capacity of the junction, D is the junction diameter, and h is the convective heat transfer coefficient at the surface (4). Equation 1 demonstrates that the response time of a thermocouple in a gas stream is inversely proportional to the surface heat transfer coefficient. In an airbag exhaust gas stream, high velocities (approaching the speed of sound) result in rapid heat transfer to the thermocouple. Using the Hilpert correlation function (4), the coefficient of heat transfer h to a cylinder in crossflow can be estimated by

$$h = \frac{k}{D} C Re^m Pr^{1/3} \quad (2)$$

where k is the thermal conductivity of the gas, Re is the Reynolds number, Pr is the Prandtl number, and C and m are empirically derived constants. For this example calculation, the airbag exhaust gas is assumed to be nitrogen at 400 °C flowing at a Mach number of 0.8. Using ideal gas assumptions, the velocity u is calculated to be

$$u = 0.8 (k_r R T)^{1/2} = 0.8 (1.4 (296.8) (273 + 400))^{1/2} = 423 \text{ m/s} \quad (3)$$

where k_r is the ratio of specific heats, R is the gas constant, and T is the temperature (K). For a thermocouple formed by the junction of two wires, each with a diameter of 0.0005 inches (0.0127 mm), the junction diameter is conservatively taken as twice the wire diameter, or 0.0254 mm. The Reynolds number is then

$$Re = \frac{uD}{\nu} = \frac{423(0.0254 \times 10^{-3})}{36.1 \times 10^{-6}} = 298 \quad (4)$$

Where ν is the kinematic viscosity of nitrogen. The kinematic viscosity and other gas thermal properties in these calculations are evaluated at the film temperature, which is defined to be the average of the free-stream and surface temperatures. Assuming that the thermocouple surface is initially at 293 K, the film temperature is $(293 + 673)/2 = 483$ K.

Returning to equation 2, the heat transfer coefficient is estimated to be

$$h = \frac{k}{D} C Re^m Pr^{1/3} = \frac{37.8 \times 10^{-3}}{0.0254 \times 10^{-3}} (0.989) (298^{0.33}) \left(0.702^{1/3}\right) = 8573 \text{ W/m}^2/\text{K} \quad (5)$$

From equation 1, the thermocouple time constant is estimated as

$$t = \frac{\rho c D}{6h} = \frac{8643.6(457.5)(0.0254 \times 10^{-3})}{6(8573)} = 0.00195 \text{ s} \quad (6)$$

where the values of ρ and c are estimates for K-type thermocouples (4). Using the time constant, the temperature of the thermocouple junction during the response to a step change in free-stream temperature is given by

$$\frac{T - T_\infty}{T_i - T_\infty} = e^{-t/\tau} \quad (7)$$

where T_∞ is the free-stream temperature and T_i is the initial temperature of the thermocouple. Rearranging equation 7, the time required to reach, say, 90% of a step change from 20 °C to 400 °C is

$$t = -\tau \ln\left(\frac{T - T_\infty}{T_i - T_\infty}\right) = -0.002 \ln\left(\frac{362 - 400}{20 - 400}\right) = 0.005 \text{ s} \quad (8)$$

These calculations suggest that a 0.0254-mm-diameter thermocouple junction in a 400 °C airbag exhaust gas stream would record a temperature of 362 °C within about 5 ms of the start of gas flow. Because of the considerable uncertainties in these calculations, particularly in estimating the heat transfer coefficient, these results should be interpreted as an order-of-magnitude estimate. Under typical airbag exhaust flow conditions, reasonably accurate readings can be expected when the thermocouple remains in the gas stream for 10 to 20 ms.

Experiments at UMTRI with fine-gage thermocouples have shown that the measurements are often variable, particularly because the gas flow is very dynamic. When the gas velocity at the thermocouple decreases, either because of decreases in airbag internal pressure or because the location of the thermocouple in the exhaust gas vent stream changes, the response time of the thermocouple will increase. In general, temperatures recorded in this manner can be interpreted as a lower bound on the exhaust gas temperature, since errors will always tend to underestimate the actual free-stream temperature. The procedure at UMTRI is to use thermocouple measurements in combination with the results of gas dynamics calculations based on inflator tank-test data, airbag geometry, and measured airbag internal pressure.