Application of Severity Indices Developed for Adiabatic Compression Testing

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Abstract: Gaseous fluid impact (adiabatic compression) testing is widely used for ranking and qualifying a nonmetallic material for its sensitivity to ignition in high-pressure gaseous oxygen. This test method is also used for qualifying flow control equipment (valves, regulators, flexible hoses, etc.) for use in high-pressure gaseous oxygen. As with many ignition tests, gaseous fluid impact ignition testing is inherently probabilistic and subject to variations in results.

One common gaseous fluid impact test is ASTM G74[1]. When originally published in 1982, this standard considered a "*passing*" result to be 0 ignitions of a material out of 20 samples tested. A flow control component was considered to have passed the test by surviving 20 successive pressure surges without signs of ignition. Researchers familiar with this test method have recognized that statistical problems exist with the prescribed methodology and have reported that an analysis of the cumulative binomial probabilities for ASTM G74 produce a 36% confidence for a 20-cycle "passing" result. As a result, the lack of reliability with the historical ASTM G74 test logic could be potentially misleading or even catastrophic when results are used to qualify materials or components for oxygen service.

This paper presents a summary of research performed to specify the severity of the ASTM G74 test so that the statistical variations can be incorporated into the test methodology. The severity of the test, as compared to service conditions, was considered crucial to the specification of a suitable approach for passing a material or qualifying a component. Logically, the more severe the test approach, as compared to service conditions, the more confidence that can be placed in a passing result. This research demonstrated that the gaseous fluid impact test commonly conducted is more severe than the service conditions, but not by a large margin. Therefore, the statistical aspects of the test, based on a suitable understanding of the actual severity, are shown to be crucial to an understanding and correct application of the data obtained.

Keywords

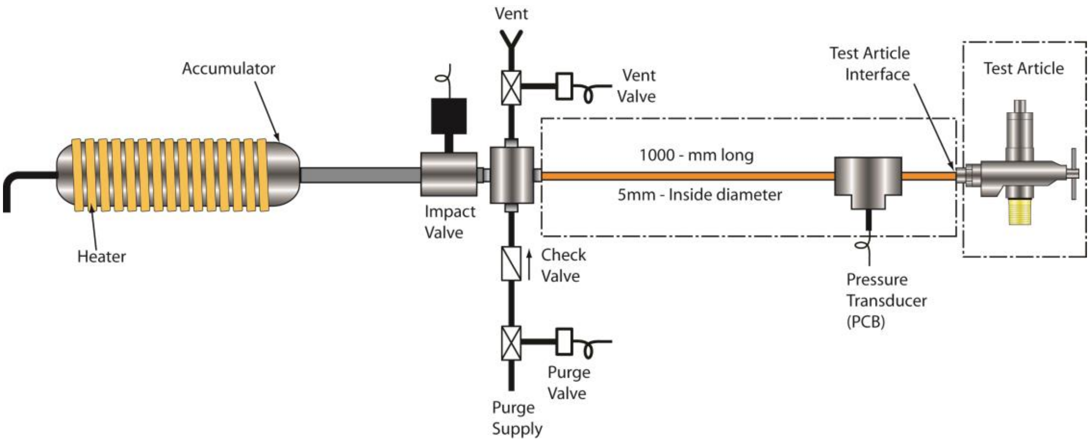
Gaseous Fluid Impact, Adiabatic Compression, Oxygen, Oxygen Compatibility, Oxygen Qualification, Test Severity, Logistic Regression, Acceleration Model, Material or Component Qualification, Material or Component Ignition, ASTM G74

1.0 INTRODUCTION

Gaseous fluid impact (adiabatic compression) testing is widely used for ranking a non-metallic material for its sensitivity to ignition in gaseous oxygen and for qualifying flow control equipment (valves, regulators, flexible hoses, etc.) for use in high-pressure gaseous oxygen. A typical test system used for this testing is illustrated in Figure 1. These systems are designed to subject a material test sample or an assembled component (valve or regulator, etc.) to rapid pressure surges of gaseous oxygen from ambient pressure to a pre-selected test pressure through a connecting tube or tubing conduit. Tubing conduits are chosen to be consistent with the system sizes common to the material or component under test. Typical tubing conduits are either 1000-mm long by 5-mm internal diameter or 750-mm long by 14-mm internal diameter.

[1]ASTM International Test Method G74, "Standard Test Method for Ignition Sensitivity of Materials to Gaseous Fluid Impact," *Annual Book of ASTM Standards, ASTM Committee G4 on Compatibility and Sensitivity of Materials in Oxygen Enriched Atmospheres Subcommittee G04.01 on Test Methods*, Vol. 14.04, ASTM International, West Conshohocken, PA., Originally published in 1982.

Figure 1 - Illustration of Typical ASTM G74 Test System



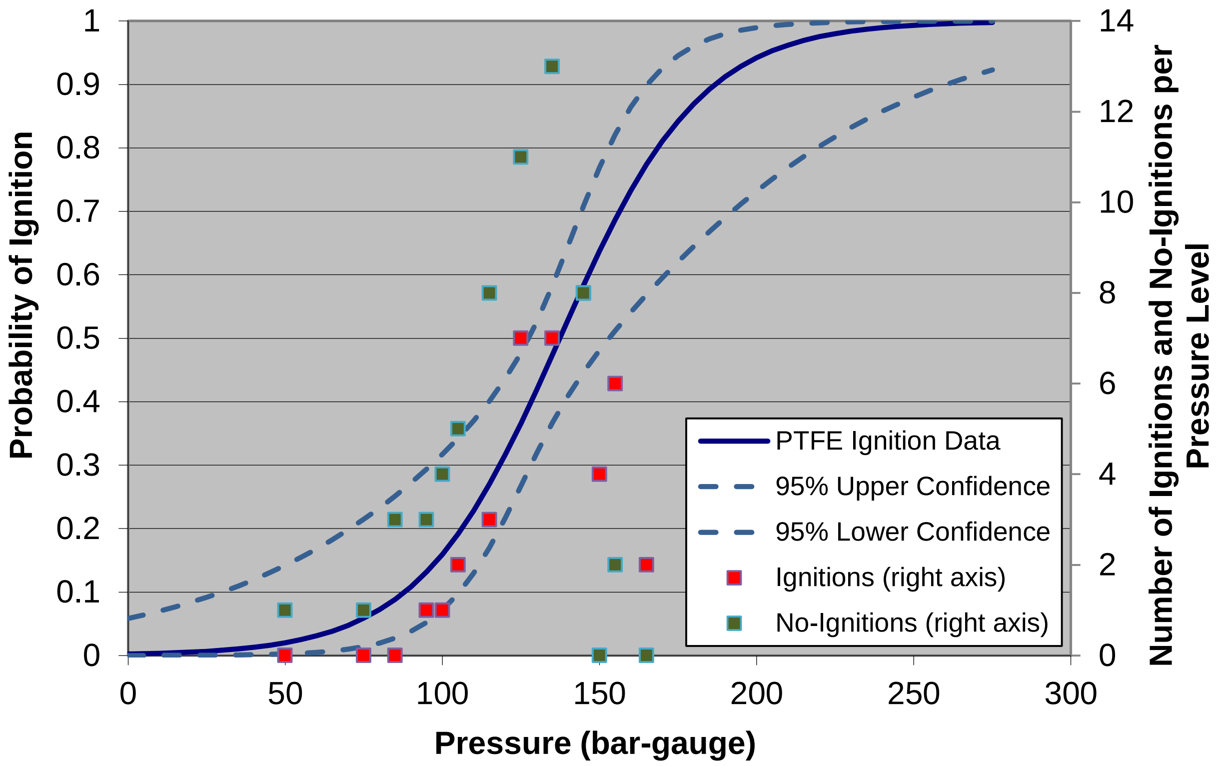
The pressure surge is usually required by international test standards to occur within 15 to 20 milliseconds, which is achieved by rapidly opening a high speed, high-flow, impact valve. The rapid pressurization causes an associated temperature rise in the gas being compressed and, if sufficiently elevated, ignition may occur on non-metallic materials (seats, seals, lubricants, etc.). The ignition thresholds provide a way of ranking non-metallic materials and qualifying assembled components for use in oxygen at certain service pressures (including appropriate factors of safety). Compression heating of oxygen in service is widely considered an efficient ignition mechanism and has been implicated in many component fires. Therefore the test described above has gained a wide subscription worldwide.

As with many ignition tests, gaseous fluid impact testing is inherently probabilistic [1] and subject to variations that are best analyzed through suitable statistical approaches. This fact has been widely discussed in the literature related to gaseous fluid impact testing [2 - 6] since approximately 1989 related to ASTM G74 [7] testing. ASTM G74 originally considered a "*passing*" result to be 0 ignitions in 20 samples tested. Hirsch summarized well the statistical problem with this approach, "*An analysis of the cumulative binomial probabilities for the ASTM G74 procedure indicated that for a probability of reaction of 0.05, assumed for a single trial, the probability of obtaining zero reactions in the 20 trials prescribed by the standard logic is about 36%. As a result, the lack of precision with the ASTM G74 test logic could be potentially misleading when results were used to qualify materials for oxygen service*" [6].

To illustrate the statistical variability associated with gaseous fluid impact testing, Figure 2 was developed after subjecting 100 sample discs of Polytetrafluoroethylene (PTFE) to ignition testing in accordance with ASTM G74. This figure shows the number of ignitions (red points) and the number of no-ignitions (green points) obtained at various pressure levels. It is noteworthy that ignitions and no-ignitions occurred at each pressure level and the results appear somewhat random. If only 20 samples had been tested, depending upon the data obtained, the results could be very difficult to interpret correctly. However, these data are well treated using an established approach for logistic regression, as has been observed and applied by several authors [1, 8].

The logistic regression approach converts the data obtained to a probability of ignition based on the number of ignitions and no-ignitions at each pressure level and then fits the data to a standard logistic sigmoidal curve having the functionality shown in Equation 1. The beta values (β_0 and β_1) are the intercept and slope values for the function and produce the shape parameters for the sigmoidal curve. These values can be obtained from any number of available statistical packages. The sigmoidal curve, for the PTFE data shown, and its 95% upper and lower confidence intervals are also depicted in Figure 2. The sigmoidal curve shown provides a convenient way to compare one material to another since a material having higher ignition likelihood would shift the curve to the left (and conversely, the curve will shift to the right for a material having lower ignition likelihood).

Figure 2 - PTFE Material Testing Results by ASTM G74 (Logistic Regression Analysis)



$$P(x) = \frac{e^{\beta_0+\beta_1 \cdot x}}{1+e^{\beta_0+\beta_1 \cdot x}} \quad (1)$$

Despite the extensive discussion of the probabilistic nature of gaseous fluid impact ignition tests in the literature, the predominant international test standards have, to-date, not incorporated rigorous statistical approaches into the test requirements and still only require 20 successive pressure surges, or less, to qualify materials and components. Only recently has ASTM G74 incorporated a statistical approach to analyzing the test results, but other relevant test standards have not. This lack of attention to the statistical implications of the methodology is due in part to a belief that the test is far more severe than actual service conditions, which this research will show is not a valid assumption.

This paper provides an approach to characterizing the actual severity of the test used as compared to typical service conditions. The authors have previously published on the background of this test method [2] and on a methodology for specifying the severity of the test [9, 10] which was utilized in the calculations discussed herein. This paper further utilizes the test severity obtained to specify an "*acceleration factor*" for the test so that the data can more easily be applied to the actual service conditions experienced.

2.0 SPECIFICATION OF TEST SEVERITY

For a gaseous-fluid-impact compression process, work is done on the fluid so as to raise its pressure and temperature. The first law of thermodynamics for conservation of energy can be written, for this process, as shown in Equation 2, in a simplified form. If potential and kinetic energy are ignored, the first law states that the change in enthalpy (ΔH) equals the sum of the heat transfer (ΣQ) minus the flow work (ΣW). As is commonly assigned, heat transfer is positive when heat is transferred into the gas and work is positive when is done on the system. This form of the first law indicates that if the heat losses in the compression process are specified by ΣQ, and the work done on the gas during the energy gain period of the pressure surge, $\int P dv$, are specified by ΣW, then the change in enthalpy provides a quantification of the property change in the gas being compressed.

$$\Delta H = \Sigma W - \Sigma Q \qquad (2)$$

Since it is common to specify the efficiency of a compression or expansion process by the ratio shown in Equation 3 [11], the derivation of an enthalpy change in the gas during the compression process provides a very convenient way to quantify the efficiency of the process; or, for the purposes of this research, the severity of the pressure surge itself. The compression efficiency, η, or severity index for the pressure surge as compared to an isentropic compression can be specified as in Equation 3 where the severity is specified by taking the ratio of the enthalpy change for an isentropic (adiabatic and reversible) pressure surge to the actual enthalpy change as measured for the system. This process permits several informative severity indices to be derived to describe the severity of a test system.

$$\eta = \frac{\Delta H_{actual}}{\Delta H_{isentropic}} \leq 1 \qquad (3)$$

The Mollier diagram shown in Figure 3 is useful to illustrate the severity index concept and the important concept of the isentropic stagnation state. As defined by van Wylen and Sonntag [11], "*The isentropic stagnation state is the state a flowing fluid would attain if it underwent a reversible adiabatic deceleration to zero velocity.*" They further point out that the actual stagnation state achieved may in reality depart from the isentropic stagnation state. They go on to say that, *"the actual stagnation state is the state achieved after an actual deceleration to zero velocity, and there may be irreversibilities associated with the deceleration process.*" As a consequence of irreversibility in the actual deceleration of a fluid, the fluid stagnation state may include an entropy increase and a shift in the actual fluid state, as compared to a true isentropic change.

The actual process achieves the same enthalpy and velocity change (Equation 4, Figure 3), and would therefore achieve the same theoretical temperature rise (in the absence of heat losses), but the real stagnation pressure may be less than the isentropic stagnation pressure, as required by the increase in entropy. Neglecting changes in potential energy, equation 4 equates the stagnation enthalpy (H_o) to the flow enthalpy (H) plus ½ times the flow velocity squared (V^2).

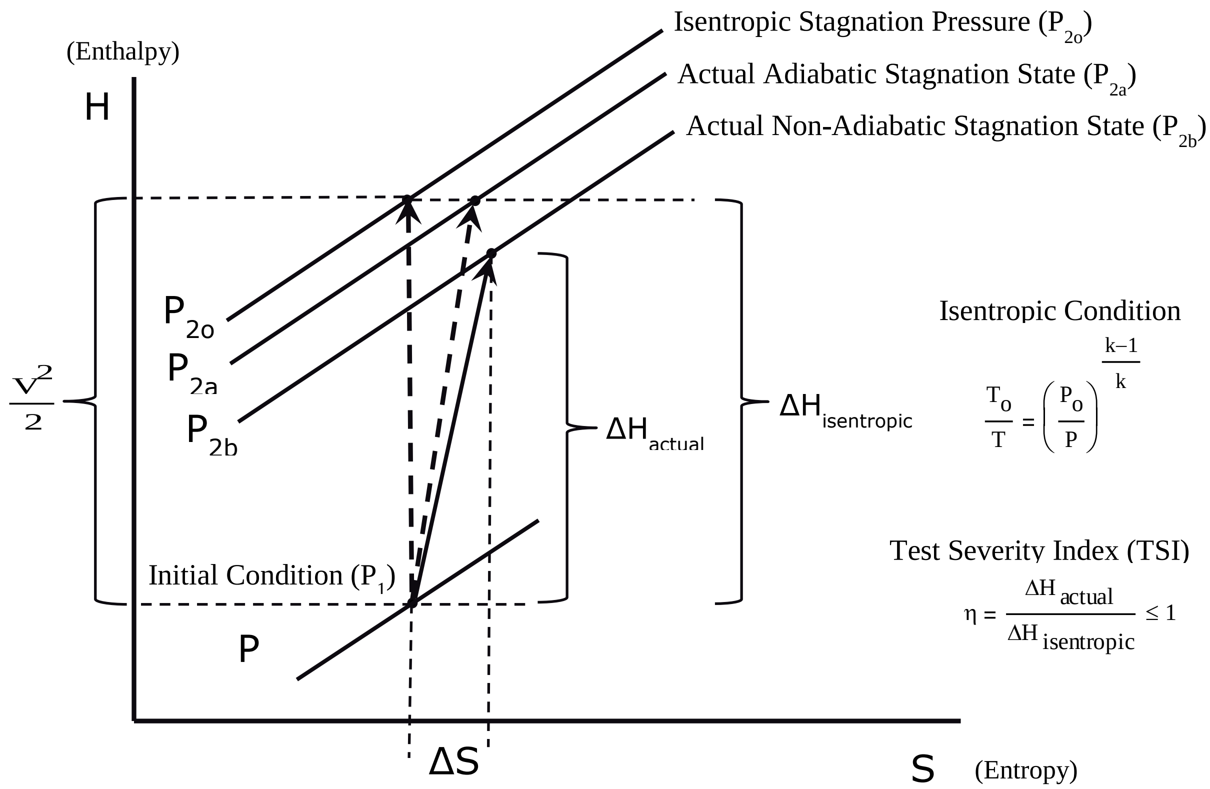
$$H_o = H + \tfrac{1}{2} V^2 \qquad (4)$$

Considering these conditions, the usual calculation for the theoretical temperature rise for an isentropic compression is given in Equation 5 (see also Figure 3). Equation 5 is an idealized, theoretical, estimate since at the high-pressure conditions of the test the oxygen behaves as a real gas and not an ideal gas; but, this correlation provides a simple idealized relationship between the initial temperature of the gas (T_i) and final temperature of the gas (T_f) for any pressure ratio ($P_{final}/P_{initial}$). The ratio of specific heats (k, 1.4 for oxygen) is specified as usual for an isentropic process.

$$T_f = T_i \left(\frac{P_f}{P_i}\right)^{\frac{k-1}{k}} \qquad (5)$$

Equation 5 provides a means of deriving the maximum temperature change possible for the isentropic stagnation state of an ideal gas and allows for an efficiency index, or a severity index, to be derived to rank the energy change for an actual pressure surge, as shown in Equation 3. We have defined this ratio as the **Test Severity Index (TSI)** based on the difference in the actual enthalpy change (produced in the actual test) compared to an ideal isentropic enthalpy change.

Figure 3 - Enthalpy-Entropy Mollier Diagram Illustrating Various Stagnation States



In reality, since gaseous oxygen behaves as a real gas and the actual fluid dynamics in a pressure surge are highly turbulent and inherently irreversible, subject to strong heat transfer effects in most actual system installations, the actual temperature achieved during a gaseous fluid impact is always less than that calculated by Equation 5. Further, heat transfer effects can be significant due to the relatively low mass (m) of the gases being compressed and their relatively low heat capacities (Cp). The high turbulence during a pressure surge also strongly increases the convective heat losses to the tubing walls. Therefore, heat losses should be expected for even a rapid pressure surge, such as in the present test (<20 ms) and, therefore, an actual real-gas stagnation state that is non-adiabatic will be realized. This will further shift the stagnation condition shown in Figure 3 to the right to indicate a further entropy increase and a reduced enthalpy gain (ΔH_{actual}).

The real-gas actual condition would be expected to produce a (correspondingly) lower gas temperature than either the isentropic or actual adiabatic stagnation states and would therefore produce a lower enthalpy rise. It is this actual (non-adiabatic or real) state change that our research sought to evaluate. Specification of the state change will naturally allow for derivation of other severity indices specific to the test. By this means several severity indices may be defined to compare test systems and test conditions to one another, as defined in the following section:

2.1 Idealized Severity Index (ISI)

The *Idealized Severity Index* is an index (ratio) calculated to compare purely adiabatic and reversible (i.e., isentropic) compression of a mass of ideal gas to the thermodynamic and fluid dynamic predictions when real-gas properties are considered (see Equation 6). This index establishes an idealized limit for the potential thermal energy produced from an isentropic pressure surge in a test system for gaseous oxygen treated as a real gas, subject to non-ideality and compressibility, rather than as an ideal gas, as it is often considered.

$$\eta_{ISI} = \frac{\Delta H_{isentropic\_real\_gas}}{\Delta H_{isentropic\_ideal\_gas}} \leq 1 \tag{6}$$

In order to evaluate the enthalpy changes for oxygen treated as a real gas, residual functions (also called departure functions [11]) were used in the manner described by Barragan et.al [10, 12]. The procedure described allows for changes in the compressibility and specific heat of the gas to be incorporated in the calculation and a real-gas temperature to be estimated for a given pressure ratio (P_f/P_i). Once a real-gas temperature was calculated, standard thermodynamic tables provided an estimate of the associated enthalpy change. At this point, no heat transfer from the gas has been considered, but an idealized efficiency factor (ISI) has been defined that can be used to specify the "*best case possible*" for a system operating with oxygen under truly adiabatic conditions. It is anticipated that the ISI can be used to compare different test systems.

2.2 Test Severity Index (TSI)

From actual temperature measurements a "*Test Severity Index*" (TSI) can also be derived for the actual thermal energy developed (i.e., actual enthalpy changes or actual temperature changes in a system). This index (Equation 7) incorporates the heat transfer losses into the severity index calculation and provides a measure of how closely a test system approaches either an isentropic condition ($\Delta H_{isentropic\_ideal}$) or alternatively a real-gas adiabatic ($\Delta H_{adiabatic\_real}$) condition. In this index, either denominator (i.e., isentropic or real-adiabatic) can be used to provide a measure of the degree to which heat transfer losses influence test system performance. By this index, a particular test system can be compared to the idealized behavior and also to other test systems that are evaluated in the same way.

$$\eta_{TSI} = \frac{\Delta H_{actual}}{\Delta H_{isentropic\_real}} \leq 1 \quad or \quad \eta_{TSI} = \frac{\Delta H_{actual}}{\Delta H_{isentropic\_ideal}} \leq 1 \tag{7}$$

The challenge with this index is clearly the great difficulty in measuring the actual temperature rise rates, including heat transfer losses, during a rapid pressure surge. A substantial effort was made to develop a means of measuring the actual temperature rise rate (i.e., the thermal profile) during a pressure surge with gaseous oxygen and has previously been discussed and reported [2, 9, and 10]. That research developed a means of measuring the temperature rise rates in the gas undergoing compression in real time, using an array of miniature thermocouples as shown in Figure 4.

Figure 4 - Thermocouple Array Used For Gas Temperature Measurements

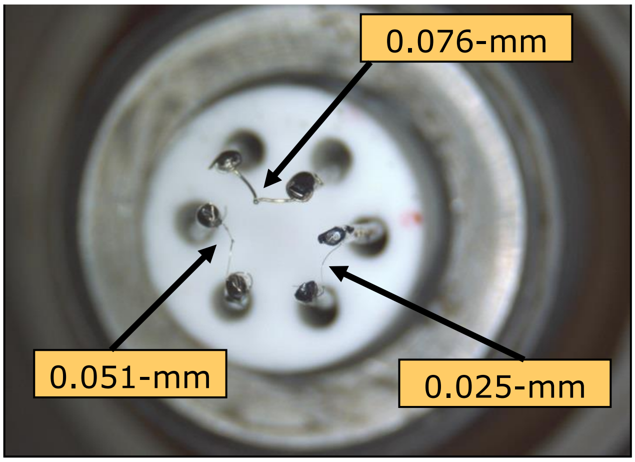
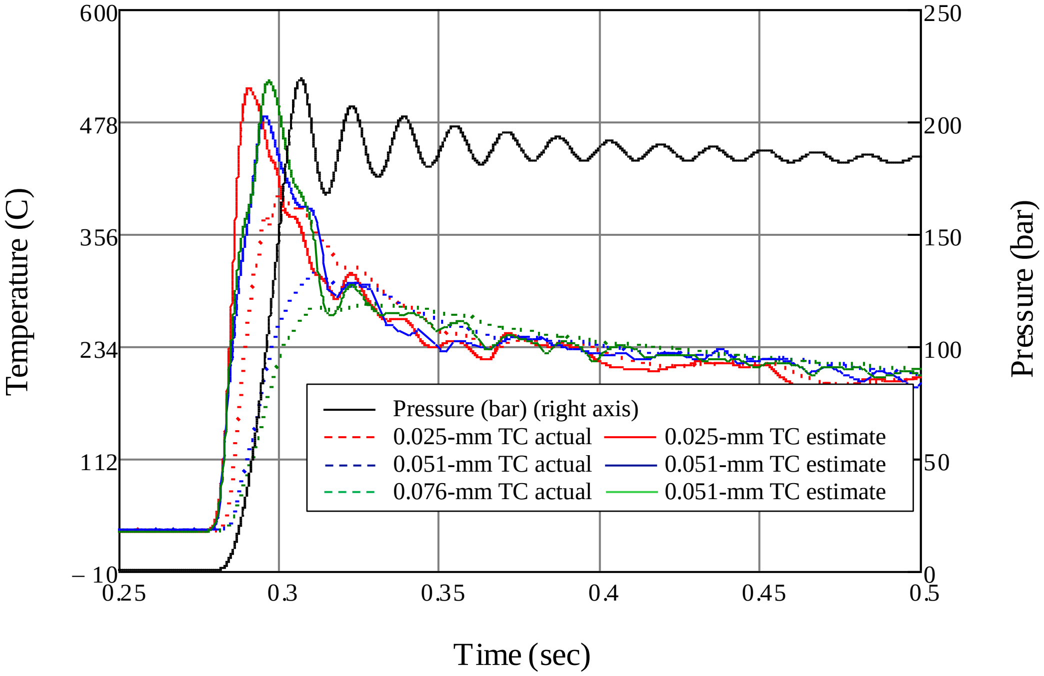


Figure 5 - Actual and Estimated Temperature Measurements on 200 bar Test
(Actual – dotted lines; Estimated – Solid Lines)



The smallest thermocouples exhibited time constants in the 3-4 millisecond range. Several fast-response thermocouple technologies were evaluated, however, the thermocouple array shown in Figure 4 provided the fastest, most reliable, response to the gas temperature measurements.

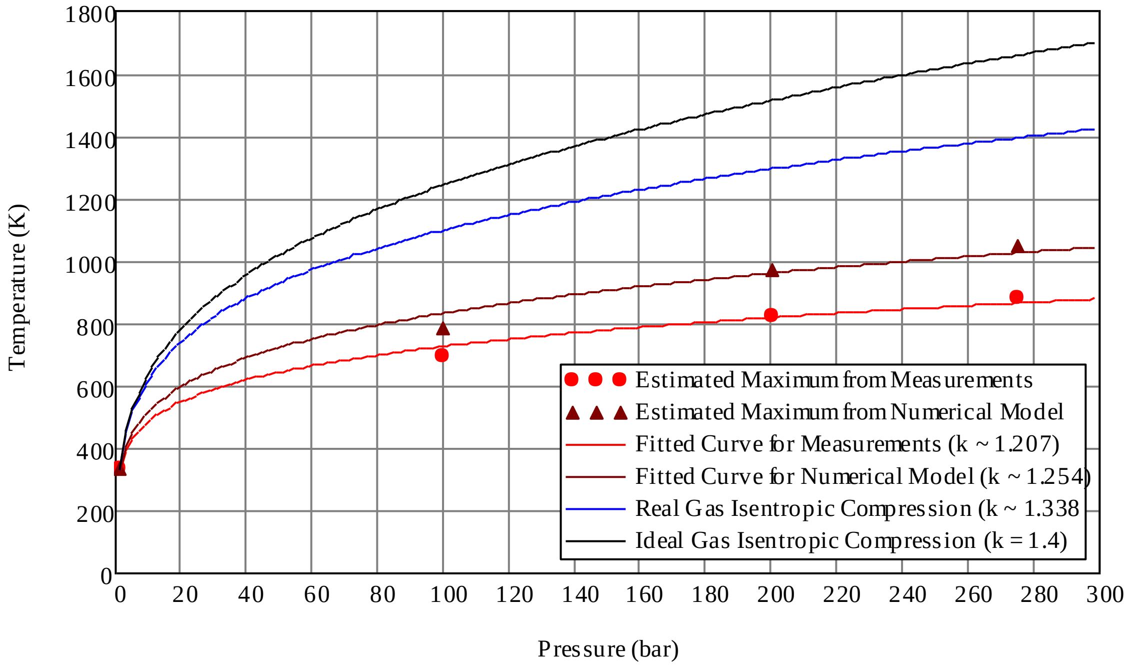
Figure 5 shows the response of a thermocouple array for a pressure surge at approximately 200 bar. In this figure the actual measurements are shown as dotted lines. Because the time constants of the thermocouples were on the same scale as the pressure rise rate (~20 ms), the true gas temperatures were not directly reproduced in the measurements. Therefore, a procedure that had been developed for combustion temperature rise measurements [13, 14] was used to estimate the true (real-time) gas temperatures from the time constants and temperature rise rates that were measured by each thermocouple. This approach took advantage of the different sized thermocouples in the array and provided an independent estimate of the true gas temperature for a specific location. Each thermocouple of the array provided a point-by-point estimate of the true temperature that could be compared to one another and overlaid in time by shifting their response curves based on their time constants.

As shown in Figure 5, the estimated temperatures for each thermocouple compared quite well (note that for ease of observation the time scales have not been shifted in the figure). The temperature estimates would converge near a similar value and an average of the estimated peak temperatures can be calculated for use in estimating the true enthalpy change. For instance, in the test cycle shown in Figure 5, and based on calibrations under known conditions, the maximum temperature estimate for the test was 506 +/- 23 ^{o}C. Many repeat tests were performed at each of several test pressures to eventually develop characteristic thermal profiles for the test system.

2.3 Service Severity Index (SSI)

A "*Service Severity Index*" (SSI) can also be developed by measuring the temperature rise and deriving the thermal profile achieved during actual service conditions, such as the opening of a cylinder valve with a regulator connected. This is the most common service condition for which the adiabatic compression testing is intended to qualify valves and regulators. The SSI for this application (Equation 8), and others, is directly useful in specifying the severity of the test as compared to actual service conditions. Once the SSI is specified for a given service configuration, then a reliability for a "passing" result can be more readily derived. Clearly, the higher the severity of

Figure 6 - Gaseous Fluid Impact Temperature Estimates



the test, the more likely that the test will result in ignition, and therefore, fewer test cycles and fewer passing results are required to achieve the desired statistical confidence in the test result.

$$\eta_{SSI} = \frac{\Delta H_{service\_conditions}}{\Delta H_{actual\_test\_conditions}} \leq 1 \tag{8}$$

3.0 TEST SYSTEM CHARACTERIZATIONS

A significant amount of testing has been performed recently to characterize the performance of several laboratories that routinely perform gaseous fluid impact testing and to develop the ISI and TSI severity indices described above [9, 10]. However, in order to illustrate the useful application of the SSI, the test system used to produce the data shown in Figure 2 was selected and the principles discussed herein applied to that system. It is noteworthy that this test system performed very well in the system characterization tests and is currently being presented to international standards organizations as suitable to represent a "*standard test performance*" for baseline comparison with other test systems.

3.1 Specification of Test System Performance

In order to calculate the severity ratios above, the expected temperature profiles for isentropic, adiabatic real-gas, and actual test conditions during a pressure surge must be calculated and measured as described above in order to estimate the enthalpy changes. Since the measurements of the temperature changes also include gas mixing between the driving gas and the driven gas, the expected temperature change was also thermodynamically modeled for oxygen as a real gas using a finite-difference model. This allowed for a thermal profile to be calculated that would include energy/heat losses to the impact tube[2] [10] but would assume to represent the best case expectations for a non-adiabatic system. Thus, four temperature profiles were derived for the standard test system as shown in Figure 6. The four temperature profiles shown in Figure 6 represent the following:

[2]All predictions herein were based on the ISO standard 5-mm ID, 1-meter long, impact tube with and initial pressure of 1-atm and an initial gas temperature of 60 ^{o}C.

1) The top curve represents the estimated temperature for isentropic compression of oxygen treated as an ideal gas. This temperature is based on the isentropic equation shown in Figure 5 and is a theoretical limit for rapid compression of an inert gas.
2) The second curve from the top represents the estimated temperature for isentropic compression of oxygen treated as a real gas. These temperature predictions still assume that the pressure surge occurs under fully adiabatic conditions, but, oxygen is treated as a real gas undergoing changes due to compressibility, specific heat, internal energy other associated thermodynamic properties.
3) The third curve from the top represents the estimated maximum temperatures for oxygen treated as a real gas but allowing heat transfer to occur (non-adiabatic) to the impact tube during the time of the rapid pressurization. These predictions were made using a numerical model which calculated the velocity and Reynolds numbers of the mass wave moving down the impact tube during the rapid pressurization process. The associated heat transfer coefficients were estimated from the appropriate Nusselt numbers for turbulent flow in a tube. These temperatures represent the temperatures that might be achieved during a gaseous fluid impact test if no irreversible processes occur except the loss of energy due to momentum changes in the mass wave and heat transfer to the impact tube.
4) The fourth curve from the top (that is, the bottom curve) represents the estimated maximum temperatures achieved during a gaseous fluid impact in the standard test system, based on actual real-time measurements[3].

These four temperature estimates provide the basis for estimating the severity indices of a gaseous fluid impact in the standard test system. Since the enthalpy change (ΔH) from an initial state to a final state provides an appropriate estimate of the heat energy available for ignition, the temperature estimates for the initial and final states were used to estimate the enthalpy change.

The estimates shown in Figure 6 are dramatically different than the view historically believed by oxygen practitioners. First, temperatures estimated by assuming isentropic conditions substantially overstate the temperatures possible in the test, which are better predicted by adiabatic real-gas assumptions. Second, heat transfer during a rapid compression event also has a substantial effect on the test conditions and even the assumption of adiabatic real-gas conditions overstates the temperatures possible in the test, which are better demonstrated by the numerical model. Third, estimates of the true gas temperatures achieved during a test indicate that additional irreversibility's exist which depress the temperatures even further. Table 1 provides an estimate of these effects and shows a calculation of the ISI and the TSI for the standard test system based on the estimated enthalpy changes on a kJ/kg basis.

Table 1: Severity Indices for Gaseous Fluid Impact at 3 Pressure Levels

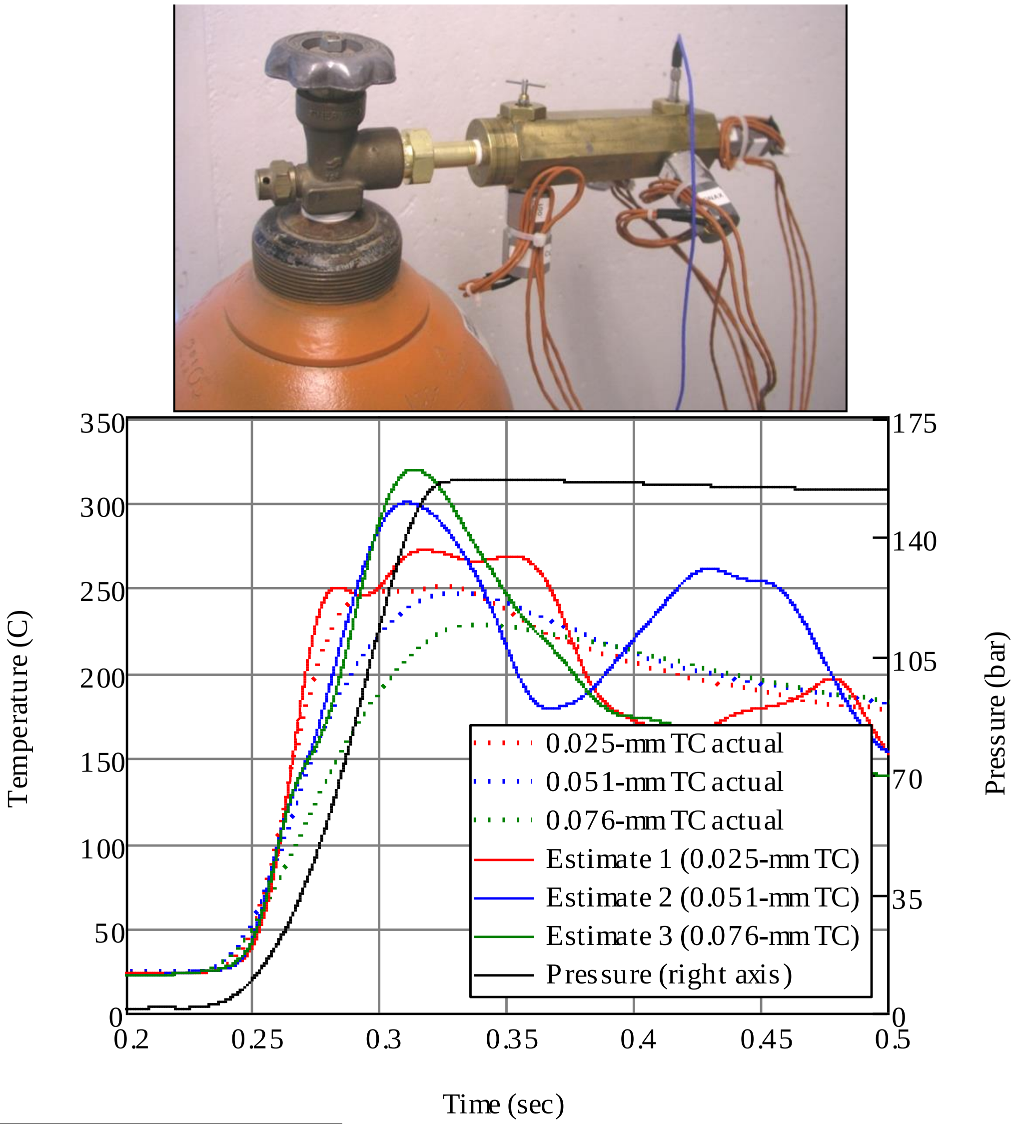
Pressure	Estimated Max Measured Temperature	ISI (ΔH_{ratio}) (real/ideal)	TSI (ΔH_{ratio}) (test/real)
10 MPa (100 bar)	453 ^{o}C	0.46	0.49
20 MPa (200 bar)	546 ^{o}C	0.50	0.48
27.5 MPa (275 bar)	592 ^{o}C	0.51	0.47

[3]It is noteworthy that the estimated gas temperature by the procedure described herein, for a 100 bar gaseous fluid impact, was 453 ^{o}C, just above the Autogenous Ignition Temperature (AIT) usually reported for PTFE in high-pressure oxygen. This was considered noteworthy since the PTFE ignition tests shown in Figure 2, which were performed on the same test system as modeled here, produced ignitions only at pressures of 95 bar (estimated temperature of 446 ^{o}C) and greater. Thus, the predicted temperatures for the pressure condition that actually began to produce ignition of PTFE samples, were near the reported AIT of the test material. Thus, the actual ignition results provide some verification of the measured and estimated maximum temperatures.

Much could be observed regarding the severity indices, but for the purposes of this discussion the ISI indicates that the enthalpy change for oxygen as a real gas rapidly compressed to the pressures indicated is estimated to produce only about 50% of the energy as would an ideal gas[4]. More importantly for this discussion of the gaseous fluid impact test, the TSI indicates that the oxygen in the actual test is estimated to develop only about 48% of the energy available for a real gas. The loss of energy is evidently due to heat losses in the tube and other irreversibilities of the rapid compression (i.e., momentum losses, flow dynamic losses, valve losses, etc.).

From this analysis it may be concluded that the actual gaseous fluid impact test does not produce conditions even close to the severity of an ideal gas undergoing isentropic compression. This may be demonstrated by multiplying the ISI by the TSI, which indicates that the gaseous fluid impact test produces just less than 24% of the energy expected from an ideal gas undergoing isentropic compression. Since the probabilistic nature of the test is inherently influenced by how severe the test

Figure 7 - Temperature Measurement Fixture and Thermal Profiles Obtained



[4]The ISI's were calculated here using the total enthalpy change for an ideal gas compared to a real gas. These calculations are not trivial. However, a more simplistic method was also derived by calculating the ISI's from the temperature derived through the isentropic equation representing an ideal gas (see Figure 3) but then reading the enthalpy change from standard thermodynamic tables for oxygen, at the temperature calculated. This condition is not really thermodynamically ideal since the compressibility and heat capacity changes are reflected in the tables. However, it is a procedure that allows direct access to standard tables. The ISI by this approach is about 80% rather than 50%, but, does not require the rigors of the calculational approach.

conditions are compared to service conditions and since the severity by this analysis is much lower than has been historically expected, then the SSI is required for an understanding of the statistical reliability of a test result.

3.2 Specification of Service Severity Index (SSI)

The SSI is a configuration dependent index, based on the actual conditions that may be encountered during service compared to the expected test conditions. The severity is based on the temperatures that develop for reasonable, but worst case, conditions and are based on actual measurements.

The SSI was evaluated using the thermocouple arrays previously described, mounted in a fixture which was equipped with several arrays, and affixed downstream of a cylinder valve as shown in Figure 7. This configuration was utilized to evaluate the severity that might develop for a gas regulator mounted on a standard oxygen cylinder when the cylinder valve was opened rapidly. The data for the dead-end thermocouple array is also shown in Figure 7, with the temperature estimates derived by the same procedure as was used for estimating the gas temperatures in the test system.

The average maximum temperature for the cylinder valve configuration (Figure 7) was 315 ^{o}C, for the cylinder pressure and pressurization rate achieved when the valve was opened by hand rapidly. When this enthalpy change is compared to the usual test conditions[5], the estimated maximum temperature expected in a gaseous fluid impact test is 532 ^{o}C. For these conditions, the severity indices are calculated in Table 2.

Table 2: Severity Indices for Rapid Opening a Cylinder Valve

Pressure	Estimated Max Temperature	SSI (service/test)	TSI • ISI	TSI • ISI • SSI
18.2 MPa (182 bar)	532 ^{o}C	0.53	0.24	0.13

The SSI for this circumstance indicated that the enthalpy change for the service condition was approximately 53% of that achieved under the test conditions. In other words, compared to the service condition considered, the gaseous fluid impact test is estimated to be only 1.9 times more severe. Clearly, different service configurations will produce different severities based on the pressures, pressurization rates, and energy loss mechanisms.

This result is even more surprising since the test has often been argued to produce temperatures near isentropic temperatures since the required pressurization rate is so rapid (<20 ms). Based on this analysis, when the SSI is multiplied by the product of the TSI times the ISI, then one might argue that the service condition is indeed 8 times less severe (13%) than the assumed isentropic condition. This would produce a very severe test if those conditions were actually being achieved. However, the enthalpy changes actually achieved during the test are less than twice (1.9 times) more severe than service conditions and the test is therefore only marginally more severe than the service conditions. If this is correct, then the statistical variation in the results should be expected and the test criteria should commonly require a high number of cycles to characterize a material or qualify a component.

4.0 CONCLUSION: TEST CONSIDERATIONS BASED ON SEVERITY FACTORS

Based on the above results, the gaseous fluid impact test is more severe than the service conditions but not by a large margin. The test does, however, provide meaningful and useful results if a sufficient number of test cycles are performed. Experience indicates that ignition testing is almost always probabilistic and where the stimulus energy is near that required for ignition (i.e., near the AIT or near the Minimum Ignition Energy (MIE)) then the probabilistic effects are more difficult to

[5]In order to provide a safety margin for testing a component like a pressure regulator, the test is usually conducted at a pressure 1.2 times the maximum service pressure and at a starting gas temperature of 60 ^{o}C.

interpret. When the stimulus energy is well above the AIT or the MIE then ignition becomes more frequent for each test trial.

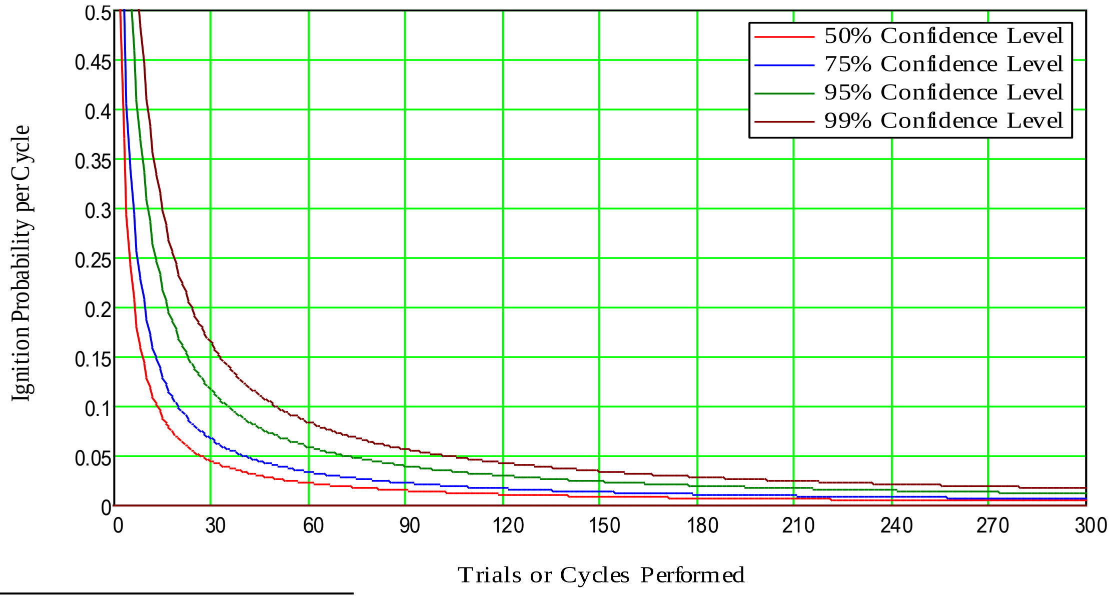
4.1 Material Tests

The conclusion that meaningful results can be accomplished with the test is illustrated for the testing shown in Figure 2. At pressures above 13.8 MPa (2002 psig), about half of the samples tested reacted. This pressure is the level where 50% of the samples exhibited ignition and is commonly referred to as the 50% reaction pressure. The estimated maximum temperature achieved in the standard test system for this pressure was 494 ^{o}C, about 40 ^{o}C above the usual AIT specified for PTFE. Above this pressure the samples reacted with greater frequency each time the pressure level was increased. By 15.5 MPa (~510 ^{o}C) three samples ignited for every one that did not ignite. By the time the pressure was raised to 16.5 MPa (~519 ^{o}C) all samples ignited[6]. For a test on an individual material, like the PTFE tests of Figure 2, then the test method used in that testing is recommended with the data analyzed by a logistic regression analysis. Experience has indicated that by utilizing a Bruceton up-down method [15] to first establish the 50% reaction pressure, and then additional samples at selected pressure intervals above and below the 50% reaction pressure, that the logistic regression curve representing the performance of the material may be established with fewer samples.

4.2 Component Tests

For a component qualification test, the component design clearly will influence the energy delivered by the test system to the non-metallic materials in the component; and, it is this design factor that is of interest. Consequently, the SSI of the test must be considered and the lower the SSI, the more cycles for a "*passing*" result that should be required for a statistically valid result. Since ignition testing by gaseous fluid impact results in only two possible outcomes (ignition or no-ignition), the binomial distribution is considered useful, as was shown by Suvorovs et.al, [8] (Figure 8). Binomial statistics require that only two possible outcomes can occur and that each trial is independent of the last trial. While slight changes in the non-metallic material may occur during successive trials (since the result desired is either ignition or no-ignition), the binomial distribution has still been shown to be applicable [6, 8]. Thus, for a component test, these statistics were plotted in Figure 8 and suggest that if a reasonably low ignition likelihood (<0.05 minimum) is desired to qualify a component, with a reasonable confidence level, then approximately 70 or 105 successful cycles (or more) must be achieved for a 95 or 99% confidence level (respectively) in a passing result.

Figure 8 - Ignition Probability Based on Binomial Distribution



[6]While this was true for the test data, only two samples were tested at this level and therefore a possibility exists that had more samples been tested some may not have ignited. However, it would still have been expected that many more samples would ignite at this pressure level than would not ignite.

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