Full-Scale Turbine Missile Casing Tests

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Full-Scale Turbine-Missile-Casing Tests

NP-2741
Research Project 399-1

Final Report, January 1983

Prepared by

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Prepared by
Sandia National Laboratories
Albuquerque, New Mexico

EPRI PERSPECTIVE

PROJECT DESCRIPTION

The effects of turbine missiles must be addressed for the safe design of nuclear plants. Such missiles could conceivably be produced by the rare failure of shrunk-on steel disks that retain the rotating blades in the low-pressure sections of large, 1800-rpm steam turbines. A high-energy fragment from a failed disk might be contained within the steel casing surrounding the turbine or might be slowed down as it passes through the casing. Such turbine missile effects are examined by full-scale tests described in this final report for RP399-1. (Quick-look results were published in an EPRI preliminary report in October 1978). These tests are a key feature of EPRI's wide-scope turbine missile research program. Other projects include (1) scale model tests and analysis of casing impact (RP399), (2) impact tests and analysis of turbine missiles striking concrete structures that protect safety-related equipment (RP399, RP393), and (3) a study of the overall risk from turbine missile effects (RP1549).

PROJECT OBJECTIVE

The objective was to provide benchmark data on the energy absorbed by turbine casings in stopping or slowing down a fragment produced by the postulated failure of a disk. Extensive instrumentation recorded the response of two full-scale, simplified models of a casing, each struck by a turbine missile launched by a rocket sled. Tests were completed in June 1978. The data can be used in assessing present methods for calculating the exit conditions of turbine missiles and for validating improved design and analysis methods.

PROJECT RESULTS

The tests showed that, depending on the orientation of the fragment as it strikes, turbine casings can either contain the fragment or absorb a substantial portion of the fragment's energy by deforming during the breakthrough process. However, additional testing and analysis are needed to examine the effects of missile spin and blade crush on energy absorption (RP399-5).

The results in this report would be useful to utility engineers responsible for turbine missile requirements in plant licensing, to architect-engineers responsible for estimating turbine missile risk, and to turbine manufacturers responsible for providing design basis exit conditions for turbine missiles. A 16-mm sound documentary film showing slow-motion views of the full-scale rocket sled tests is available for loan or purchase upon written request of the EPRI Project Manager.

George Sliter, Project Manager
Nuclear Power Division

ABSTRACT

Results are presented of two full-scale tests simulating the impact of turbine disk fragments on simple ring and shell structures that represent the internal stator blade ring and the outer housing of an 1800-rpm steam turbine casing. The objective was to provide benchmark data on both the energy-absorbing mechanisms of the impact process and, if breakthrough occurred, the exit conditions of the turbine missile. A rocket sled was used to accelerate a 1527-kg (3366-lb) segment of a turbine disk, which impacted a steel ring 12.7 cm (5 in) thick and a steel shell 3.2 cm (1.25 in) thick. The impact velocity of about 150 m/s (492 ft/s) gave a missile kinetic energy corresponding to the energy of a fragment from a postulated failure at the design overspeed (120% of operating speed). Depending on the orientation of the missile at impact, the steel test structure either slowed the missile to 60% of its initial translational velocity or brought it almost to rest (an energy reduction of 65 and 100%, respectively). The report includes structural and finite element analysis and data interpretation, estimates of energy during impact, missile displacement and velocity histories, and selected strain gage data.

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SUMMARY

Two full-scale rocket sled tests have been conducted to provide benchmark data for making more realistic assessments of turbine missile effects in nuclear plant design. The tests simulated the impact of a fragment from a failed shrunk-on turbine disk on the internal stator blade ring and the outer housing of an 1800-rpm steam turbine low-pressure casing. High-speed photography and strain gages were used to measure data on both the energy-absorbing mechanisms of the impact process and, if breakthrough occurred, the exit velocity and rotation of the fragment. This information can lead to improvements in the methods currently used to estimate whether external missiles are produced by the postulated failure of a rotor and, if they are, the initial conditions of missile trajectories which may intersect the containment or other plant buildings. A subsequent full-scale test program at Sandia National Laboratories is examining the impact resistance of the reinforced concrete walls and roofs that protect nuclear plant buildings.

In both casing tests the target structure was a simplified representation of a casing (Figure S-1). Only the two main semicircular casing components that were expected to absorb most of the energy in a postulated failure were modeled: the internal blade ring or diaphragm, which supports the stationary turbine blades and is represented by a steel ring 12.7 cm (5 in) thick; and the outer casing cover, represented by a steel shell 3.2 cm (1.25 in) thick.

The ring and the shell were bolted to a massive reinforced-concrete backup structure. Various types of connections are used to join the upper and lower rings and casing sections in actual steam turbines. The strengths of the bolted connections used in the tests are within the range of the horizontal joint strengths in an actual turbine.

The missile in both tests was a 120° segment of a last-stage, shrunk-on rotor weighing 1527 kg (3366 lb) as shown in Figure S-1. The segment had no blades; it was assumed that the blades would be broken off or crushed during exit. Although a segment from a failed rotor would be spinning as it was thrown from the shaft by centrifugal force, the test segment had no spin. It was mounted on a carriage

Figure S-1. Target Structure, Missile, and Impact Orientations

sled which was propelled to the test velocity by a detachable rocket sled. After rocket burnout, the rocket sled was decelerated by a water braking system and slowed to a stop. Prior to impact, the test segment was detached from the carriage sled with explosive bolts, and the carriage sled was deflected away from the impact area by a large steel plate. The test segment then translated unconstrained and without rotation to strike the target structure.

The nominal impact velocity in both tests was 146 m/s (480 ft/s), which gives the same total kinetic energy as a segment leaving a shaft spinning at 120% of operating speed (the design overspeed condition). The rotational energy of a 120° segment at this speed is about 37% of the total energy. Since rotational motion is not as effective for impact penetration as translational motion, the test condition gives an upper estimate of impact response.

Since the orientation of a turbine segment at impact with the inner ring cannot be specified with certainty and since orientation has a great influence on absorbed energy, the missiles in the two tests were given "bounding" orientations (see Figure S-1). In one test, a piercing orientation, with sharp corner impact and minimum projected area, gave a low estimate on absorbed energy. In the other test, a blunt orientation, with curved edge impact and maximum projected area, gave a high estimate on absorbed energy.

In the test with a piercing orientation (Figures S-2a, S-2b, and S-2c), the missile penetrated about 10 cm (4 in) into the ring, broke through both the ring and the shell, and exited with a translational velocity of 86.9 m/s (285 ft/s). After failure of the ring at the impact point, both ends of the ring broke at the edge of the welded gusset plates that connected the ring to its base plate. The bolts connecting the base plate to the backup structure did not fail. The missile was only slightly deformed by the impact; the sharp corner was blunted about 2.5 cm (1 in).

After exiting the simulated casing, the segment was rotating at 115 rpm. The rotational energy imparted to the missile was negligible (0.2%) compared with the total exit energy.

In slowing the missile to 58% of its initial speed, the structure absorbed two-thirds of the missile's energy; 85% of the absorbed energy was dissipated by ring impact and only 15% by shell impact.

Figure S-2a. ($V = 149 \text{ m/s}$, $\omega = 0$)

Figure S-3a. ($V = 151 \text{ m/s}$, $\omega = 0$)

Figure S-2b. ($V = 99 \text{ m/s}$)

Figure S-3b. ($V = 32 \text{ m/s}$)

Figure S-2c. ($V = 87 \text{ m/s}$, $\omega = 115 \text{ rpm}$)

Figure S-3c. ($V = 3 \text{ m/s}$, $\omega = 0$)

Overhead photographs of
Piercing Impact

Overhead photographs of
Blunt Impact

In the test with a blunt orientation (Figures S-3a, S-3b, and S-3c), the missile did not perforate either the ring or the shell. Instead, the momentum transferred to the structures failed all of the support bolts, and the net energy dissipated brought the missile almost to rest. About 95% of the missile's initial energy was dissipated as the ring impact slowed the missile to one-fifth of its initial speed. The missile velocity remained constant until impact with the shell, which absorbed virtually all of the remaining 5% of its energy.

A series of structural analyses were used to assist in:

- selection of the test impact conditions
- design of the test support fixtures
- selection of instrumentation location and settings
- interpretation of test results

Prior to the tests, simple momentum energy techniques were used to predict the exit velocities for the test segments in each configuration. These techniques produce predictions which are somewhat biased by the analyst's judgment because they depend greatly on the failure modes assumed. A comparison of the exit velocities calculated by two techniques indicated that it is difficult to accurately predict exit velocity by momentum analysis techniques alone and that the measured exit velocities for both tests were within the limits set by the assumptions used in each method.

Lumped parameter (spring mass) modeling in combination with momentum analysis techniques was used to model the response of the steel test targets and concrete backup structure to assist in the engineering design and to check the adequacy of the hardware under dynamic loading. For both tests, the lumped parameter analysis indicated that the maximum reaction load at the connections would occur much later than the maximum impact load. Also, the magnitude of the loads in the connections would be less for the piercing test than for the blunt test. Thus, the piercing test would have less influence on the target design than the blunt test.

Subsequent to the tests, a simple ring (beam) type of two-dimensional finite element modeling was used to calculate impact behavior and to assist in the understanding of the test data and the interpretation of the results. For both tests, the comparison of predicted and measured displacement-time and velocity-time histories for the missile indicated good correlation; however, the comparison of measured strain gage data and calculated strains showed less agreement. The

finite element method has the potential advantage of being extended to calculate three-dimensional response from both off-axis impact and rotary missile impact.

The types of detailed information in the report include estimates of missile energy and absorbed energy due to target interaction, missile displacement and velocity, measured deformations in the test structure, selected strain gage data for the ring and shell targets during impact, and measured material properties of the missile and test structures.

Section 1

INTRODUCTION

Nuclear power plant designers are required to provide safety-related components with adequate protection against hypothetical turbine-missile impacts. The missiles could be produced by the unlikely failure of shrunk-on rotors that support the rotating blades in the low-pressure sections of large 1800-rpm steam turbines.

In plants with a "peninsula" arrangement, protection is provided by installing the turbine axis radially from the reactor building, so that most potential missile trajectories are not in line with the plant (Figure 1-1). Only missiles that are thrown upward ("high-trajectory missiles") and drop onto plant structures contribute to turbine missile risk. In plants with a "nonpeninsula" arrangement (turbine axis perpendicular to a radius), the trajectories of a significant portion of "low-trajectory missiles" contribute to turbine missile risk (Figure 1-1). Therefore designers rely on the low probability of a missile generation and strike and on the protection provided by reinforced concrete walls to demonstrate an adequate level of protection as specified in USNRC Regulatory Guide 1.115 (1).

One of the critical first steps in demonstrating adequacy is determination of the energy and spin of the turbine segments as they exit the turbine casing. The Electric Power Research Institute sponsored two full-scale turbine-missile casing tests which were conducted by Sandia National Laboratories at their rocket-sled facility in Albuquerque, New Mexico. The objective of this program was to provide benchmark data on both the energy-absorbing mechanisms of the impact process and, if breakthrough occurred, the exit conditions of the fragment. The data can be used in assessing present methods for calculating casing exit velocities and in validating improved design and analysis methods. The next phase of the program employs additional full-scale tests to quantify the resistance of reinforced concrete structures to turbine missile impact. Information from both the casing and concrete impact tests will be used as input in making improved estimates of the risk from turbine missile effects.

Figure 1-1. Typical Peninsula and Non Peninsula Turbine Arrangements

The key element in both phases of full scale impact testing is the rocket-sled facility in Albuquerque, New Mexico. The missile launching technique is similar to that that used for the tornado missile tests at Sandia's Tonopah, Nevada, site (2).

Section 2 provides a description of the test conditions, procedure, and hardware. Section 3 describes the structural analysis used to assist in selecting the impact conditions, in designing the test fixtures, and in interpreting the results. Section 4 presents the results of the tests, data interpretation, estimates of energy dissipated during impact, missile displacement and velocity histories, and selected strain gage data. These sections are supplemented as necessary with additional results in the Appendices.

REFERENCES

1. Nuclear Regulatory Commission. Regulatory Guide 1.115 Protection Against Low-Trajectory Turbine Missiles, Revision 1, July 1977.
2. A. E. Stephenson, Full-Scale Tornado Missile Impact Tests, EPRI Final Report NP-440, prepared by Sandia Laboratories, July 1977.

Section 2

TEST DESCRIPTION

In this section, the test hardware (missile and simulated turbine structure), instrumentation, and test conditions (velocities and orientations) are described.

2.1 TEST MISSILE

Although many of the fragments produced by a turbine failure would be light enough to be contained within the casing (see Figure 2-1), the test missile was selected to be in the moderately heavier weight class of turbine segments that, under certain conditions, are expected to be ejected from the casings. The turbine missile weights considered in design range from 454 to 3720 Kg (1000 to 8200 lb) (1).

Missiles with 120° angles are typically used for turbine missile impact design because this segment angle gives close to the maximum amount of initial translational energy as shown in Figure 2-2 (2). The 134° segment gives the greatest amount of translational energy but test samples were not available for the weight range considered for the ring and shell structure to be modeled. The 180° segment has more total energy but most of its energy is rotational as indicated by the energy fraction as shown in Figure 2-2. One manufacturer specifies 90° segments for design on the basis that, because of its smaller impact area, exit energy is predicted to be greater than for 120° segments. The 90° segment was not selected because the 120° segment angle is more typical of those specified for design purposes. (General Electric specifies missiles with 120° and 60° angles and Allis Chalmers specifies 134° missiles.)

A 120°, 1527 Kg (3366 lb) segment was provided by Westinghouse Electric Corporation (W) for the casing tests. The dimensions and mass properties of the segment are given in Figure 2-3. Note that the segment has no turbine blades; the blades are assumed to be broken off or crushed during exit from the turbine casing. It is assumed that the additional energy of impact associated with blades is dissipated by their deformation during exit. This assumption is being examined in another EPRI-sponsored test program.

Figure 2-1. Cutaway of a Typical Low-Pressure Steam Turbine in a Nuclear Power Plant Showing the Rotating Shaft, Rotors and Blades, Stationary Stator Rings and Blades, and Outer Structure

Figure 2-2. Fragment Energy and Fragment Energy Fraction as a Function of Segment Angle (2)

Figure 2-3. Dimensions and Mass Properties of the Westinghouse 120° Test Segment

2.2 TARGET STRUCTURE

A cutaway view of typical low-pressure sections of large 1800-rpm turbine-generator units in nuclear power plants is shown in Figure 2-1. The turbine in each section consists of a central shaft supported on end bearings with shrunk-on rotors spaced symmetrically about the centerline (Figure 2-4). The rotating turbine blades are attached to the rotors. A series of rings surrounding the rotating blades supports the stator blades. An outer steel cover encloses the upper half of the casing and contains the steam exiting the low pressure turbine stage.

For the tests, the turbine was simplified to provide a generic first-order representation of the major members in the structure. Superficial parts not expected to play a significant role in the breakthrough process were not included.

The target structure in both tests had the same configuration (Figures 2-5 and 2-6). The inner structure, representing a last-stage stationary-blade support ring, was 12.7 cm (5 in) thick, 50.8 cm (20 in) wide, and 431.8 cm (170 in) in diameter. The outer shell, representing a casing cover, was 3.2 cm (1.25 in) thick, 182.9 cm (72 in) wide, and 635 cm (250 in) in diameter. These two turbine components are the main energy-absorbing members in a postulated failure.

Figure 2-4. Section View of a Typical Dual-Flow Low-Pressure Steam Turbine Showing the Rotating Shaft with Rotors and Blades Attached, Fixed Stators and Blades, and Outer Structure

Figure 2-5. Top View of Target Structure

Figure 2-6. Side View of Target Structure

In a hypothetical accident, fragments (assuming that the rotor fractures in thirds) may impact at arbitrary points spaced 120° apart along the periphery of the stator blade ring (see Figure 2-7). The tests conducted evaluated the vertical flight of only one of the fragments striking the ring and outer casing structure. Other possible impact locations should be examined in future work.

A simple generic case of a non-rotating missile impacting at the middle of the ring's height was considered for the tests (see Figure 2-6). An impact closer to the top or bottom edge of the ring which may occur under certain conditions should be examined in future work.

The ring and the shell were fabricated from ASTM A515 Grade 65 cold-rolled steel with a minimum specified tensile yield of 241 MPa (35.0 ksi), tensile ultimate of 448 MPa (68.0 ksi), and elongation of 26% at room temperature. Actual measured values of the ring material are described in Appendix A.

The ring was maintained at a temperature of about 38°C (100°F) during both tests, which is typical of the temperature in the last-stage region of an operating turbine. This temperature was selected on the basis of information from steam turbine manufacturers. The ring was heated by means of electric heating blankets placed in contact with the surface of the ring and was covered with thermal insulation to prevent heat loss. Since it was necessary to strip the heat blankets and insulation prior to the test, the ring was initially heated to a temperature of approximately 3°C (5°F) greater than the test temperature. The ring cooled at a rate of about 3°C per 60 minutes, assuming an 18°C (65°F) ambient temperature.

$T = 0 \text{ ms}$

$T = 2 \text{ ms}$

$T = 4 \text{ ms}$

$T = 6 \text{ ms}$

$T = 8 \text{ ms}$

$T = 10 \text{ ms}$

Figure 2-7. Hypothetical Trajectories of Three Fragmented 120° Rotor Segments in the Low Pressure Stage of a Steam Turbine at the 120% Overspeed Condition (Blades are not shown)

Since the thermal conductivity of the steel ring was high, the cross-sectional temperature variation was not expected to exceed 0.6°C (1°F). The ring surface temperature was monitored with thermocouples placed near the impact zone.

The simulated casing was installed with its centerline coincident with the rocket-sled track centerline. The target ring and shell were bolted to gusset and base plates mounted to a massive 1.6×10^9 kg (1800 ton) backup structure of reinforced concrete and soil overburden. The design details and construction photographs of the target structure are included as Appendix B.

The outer shell is connected to the support structure at each end with fourteen 2.5-cm (1-in) dia. horizontal bolts with an active length of 15.2 cm (6 in). These bolts have an area per unit length of outer shell of 0.042 cm^2 per cm (0.20 in^2 per ft), or 16% of shell cross-section, which is typical of the bolts connecting the upper and lower halves of the outer casing in a turbine.

The inner ring is connected to the support structure at each end with twelve 3.8-cm (1.5-in) dia horizontal bolts with an active length of 25.4 cm (10 in). The area of these bolts is 142 cm^2 (22.0 in^2) or 22% of the ring cross-section. Because of the complex nature and wide range of design detail in actual turbine stationary ring joints, the prototypicality of the ring connection in the test structure is not as clear as it is for the shell connection. Although joint design varies widely depending on manufacturer and in some cases is considered proprietary information, the basic designs consist primarily of thick flanges welded to the ends of the upper and lower rings with the flanges connected by bolts or studs (Figure 2-8). The thick flanges have an irregular shape in the plan view, as they follow more or less the contours of the different diameter stationary stator blade rings. The flanges in the vicinity of the end ring in a typical turbine are connected with two 5.1-cm (2-in) dia studs and one 3.8-cm (1.5-in) dia stud. The area is 51.6 cm^2 (8.0 in^2). Actually, however, circumferential membrane loads induced at the ends of a ring under missile impact are transferred by shear to adjoining rings and their corresponding flange/bolt regions. Therefore the single ring and flange in the test was provided with about three times the bolt area of that in the vicinity of an actual turbine end ring. (This amount of bolt strength also served as a load limiter for design of the backup structure). Clearly, the end connection of the test ring is only a crude simulation of the complex end condition in an actual stationary blade ring.

Figure 2-8. Plan View of a Typical Horizontal Joint in a Stator Blade Ring
(Note complex pattern of bolts joining top and bottom halves.)

2-3 MISSILE CONDITIONS AT IMPACT

To select the test segment orientation at impact, the trajectory of the segment from the time it would leave the turbine shaft to the time it would impact the first substantial barrier was examined. To calculate this trajectory it is customary to neglect the resistance of the turbine blades (both rotating and stationary) as they are deformed by the segment. Figure 2-9 shows the segment in its original position on the shaft, an intermediate position in flight, and its position upon contact with the ring. It is assumed that during breakup the failure planes are formed instantaneously and that the energy dissipated during their formation is negligible. This assumption is certainly valid for a brittle fracture, but may be questionable for a ductile fracture. With the assumption of instantaneous separation, consideration of conservation of energy and momentum shows that the segment leaves the shaft with a rotational velocity, ω , which is the rotational speed of the turbine at failure, and a translational velocity, $V_t = \omega r$, where r is the radial distance to the center

of gravity (CG) of the segment. For the 120% overspeed condition pictured in Figure 2-9, $\omega = 226 \text{ rad/s}$ and $V_t = 116 \text{ m/s}$ (380 ft/s). Further analysis indicates that the impact orientation is the same regardless of shaft speed but is dependent upon the radial location of the impacted structure. However, it should be noted that the orientation of the segment is one of the many parameters of casing breakthrough that is subject to wide variation. The shape of the segment and actual changes in trajectory due to blade crushing or secondary impacts within the casing are factors which affect impact orientation. In addition, energy absorbed during breakthrough could be highly dependent upon missile orientation. Therefore, of the many parameters whose variation could have been studied in the two tests, missile orientation was selected as the one that would be most informative to vary. Since it is questionable whether the trajectory shown in Figure 2-9 would actually be taken by a turbine segment, two extremes, shown in Figure 2-10, were selected for the tests. The blunt orientation was estimated to give near the maximum absorption because the projected area of the segment on the ring is maximum. The piercing orientation was estimated to give the minimum energy absorption because the projected area is a minimum and the segment will pierce the ring with little tendency to rotate.

Figure 2-9. Hypothetical Unconstrained Trajectory of Turbine Segment

Turbine missile risk analyses are required by US Nuclear Regulatory Commission Regulatory Guide 1.115 to consider failures at both "design overspeed" (120 to 130% of operating speed) and "destructive overspeed" (180 to 190% of operating speed). These initial tests concentrated on the design overspeed condition. Failures of destructive overspeed are unlikely because their occurrence presupposes failure of redundant overspeed protection systems. Furthermore, it is not clear that high overspeeds can be reached before mechanical failures of bearings and blades limit the overspeed of the rotors.

Because the test technique made it extremely difficult to impart initial rotation the test segment had only an initial translational velocity. To be conservative, it was assumed that the rotational energy of the missile be included in the translational energy. The translational velocity of the missile at the moment of release is 115.8 m/s (380 ft/s) as shown in Figure 2-9. With rotational energy included, the translational velocity of the test missile was calculated to be 146 m/s (480 ft/s).

Figure 2-10. Impact Orientation Selected for Tests

2-4 TEST FACILITY

The test facility included a missile launcher, a sled separation device, and a back-up structure to support the test ring and shell.

Missile Launcher

The missile was mounted on a light-weight sled with the missile center of gravity displaced 51.3 cm (20.2 in) laterally from the track centerline (Figure 2-11). This simulated the trajectory of a turbine segment which, as it leaves the shaft, translates tangentially from a circle through the segment's center of gravity (See Figure 2-9). The 15.2-cm (6-in) wide impact edge of the missile was positioned to strike at the center of the 50.8-cm (20-in) wide ring. Deformation of the missile was expected to be small enough to permit its use in both tests.

The missile was propelled up to speed with 15 Zuni Rockets mounted on a pusher rocket sled. Each Zuni provided a thrust of about 30246 N (6800 lb) for 1.3 seconds.

Figure 2-11. Missile Trajectory Along the Rocket Sled Track

All rockets were ignited simultaneously. After rocket burnout, the pusher sled was slowed to a stop with a water braking system. The missile and support sled continued into the impact site. Figure 2-12 shows the missile and rocket sled in the piercing and blunt orientation prior to testing.

Sled Separation Device

The missile support sled was not permitted to strike the ring or shell during the test so that the response of the ring and shell structure would not be influenced by impact with the support sled. Prior to impact, the support sled was decoupled from the turbine missile by explosive bolts. The explosive bolts were triggered from foil switches placed alongside of the track. To increase reliability, a redundant ignition system was used to trigger the explosive bolts. The sled was deflected downwards by slanted guide rails in the last 3 m (10 ft) of track before striking a steel catcher plate, as shown in Figure 2-13. The plate was designed to prevent the sled from impacting the simulated turbine shell assembly. A calibration test was performed to ensure that missile sled capture occurred and that correct missile trajectory was obtained. After exiting the simulated casing the missile was by an earth embankment.

Backup Structure

The fixed backup structure (See Figure 2-14) resisted the impact loads of the ring and shell by means of both its mass (concrete with massive soil overburden) and frictional forces generated by contact with the ground. The concrete pedestal support structure to which the test hardware was attached was designed to withstand 26.7 MN (6 million lb) of static force (13.3 MN per side). Each side of the ring connector was designed for 11.1 MN (2.5 million lb), providing a total of 22.2 MN, and each side of the shell connector was designed for 6.7 MN (1.5 million lb), providing a total of 13.3 MN (3 million lb). Note that in the tests, the loads at ring and shell connections do not occur simultaneously. To prevent failure of the pedestal support structure, the backup structure was designed to slide at 17.8 MN (4 million lb) static force.

The loads from the ring and shell were transmitted into the backup structure by means of steel support brackets. The support brackets were fabricated from welded steel base plates and gusset plates. These connections were designed with two safety releases. The horizontal bolts were designed to fail at 83% of the maximum design

Piercing Orientation

Blunt Orientation

Figure 2-12. Missile, Support Sled, and Rocket Pusher

Figure 2-13. Sled Deflector Assembly

Figure 2-14. Backup Structure

load, and the vertical bolts were designed to fail at 100% of the design load. Additional design details are included in Appendix B.

2-5 INSTRUMENTATION

High-speed photometric and strain gage instrumentation were used to observe and record the response of the ring and shell structure. These systems are described in this section. The data obtained along with error estimates are included in Section 4 and Appendix D.

Photometrics

High-speed photometric cameras were positioned to observe missile, ring, and shell motion. Primary coverage for data reduction was provided by overhead cameras. The camera plan is shown in Figure 2-15.

Two Hycam rotating prism cameras (shown by o's in Figure 2-15) were positioned overhead to record ring motion; these cameras had lenses which provided a narrow field of view. One Hycam and one Milliken (pin registered) camera (shown by X's in Figure 2-15) was positioned behind the turbine shell to record trajectory and rotation of the missile subsequent to impact. Approximately two missile lengths of free space were provided behind the shell to observe missile motion. Overall overhead coverage was provided by Photosonics 4M, Milliken, and Hycam cameras.

Secondary coverage of the event included cameras positioned at both sides and at 45° angles to the impact surface. Fastaxs (rotating prism cameras) positioned at 45° to the track centerline were used to record missile motion in front of the target structure prior to and during impact. Millikens positioned at 45° to the track centerline behind the structure and 90° to the track centerline recorded pre- and post-impact missile motion.

The high-speed cameras operated at a speed of 400 frames per second for Millikens to approximately 1000 to 3000 frames per second for the Fastaxs and Hycams.

Deflection histories of various points on the ring may be obtained by the marker positions on the simulated turbine casing and missile shown in Figure 2-16. The photometric data and error analyses are presented in Appendix D.

Figure 2-15. Photometric Camera Plan

Active Instrumentation

The missile impact velocity was measured by break rods positioned on the track in front of the target structure. Additional measurements were obtained by an image motion camera and by high-speed framing cameras.

Strain gages were used to record the structural response near the supports of the ring and shell and near the impact point of the ring. Typical strain gage placement on the ring and shell structure is shown in Figure 2-17. Actual gage locations for each test are shown in Appendix D. Both bonded and welded gages were used for the tests. The strain gage data and error analysis are presented in Appendix D.

Figure 2-16. Ring and Shell Marker Layout

Figure 2-17. Typical Strain Gage Instrumentation Placement for Simulated Turbine Shell and Ring (All Dimensions in cm)

REFERENCE

1. Report of the ASCE Committee on Impactive and Impulsive Loads, Second ASCE Conference on Civil Engineering and Nuclear Power, Vol V, Appendix B, September 1980.
2. A. C. Hagg and G. O. Sankey, "The Containment of Disk Burst Fragments by Cylindrical Shells," Transactions of the ASME, April 1974.

Section 3

STRUCTURAL ANALYSIS

Three general types of analyses were performed in support of the test program. The first method, based on simple hand calculations, was used to assess the ability to predict whether the missiles would be contained or what exit velocities would be observed. The second type of analysis used simple lumped mass spring modeling of the steel target structures and massive back-up structure to check the adequacy of the system under dynamic loading. The third used a finite element model of the ring impacted by a solid mass. If adequate correlation with test results are obtained, these analyses could be used to extend test results to other impact conditions of interest.

3-1 PREDICTIONS OF EXIT VELOCITIES

A variety of simple hand calculation techniques are available to estimate the exit velocity. Most of these techniques are based on energy-momentum methods in combination with empirical methods based on test data. For this study, three such methods were used to predict the exit velocities in the two casing tests. In the momentum-energy approach, the impact is viewed first as a process of momentum transfer between the missile and target, and then as an energy loss required to fracture or penetrate the target. This approach requires engineering judgment for the selection of appropriate values for effective masses in the momentum calculations and for coefficients in the energy calculations.

The first two methods used are taken directly from techniques developed and referenced in the open literature. The first approach is a method developed by Hagg and Sankey (1). This method is based on observations from tests in which spinning fragments from a rotor burst (about one-fifth the scale of a nuclear power plant turbine) impact an integral 360° ring. Therefore, its application here to tests in which a single non-rotating fragment strikes a 180° end bolted ring is questionable. Test observations are used to estimate effective masses and energy coefficients. The assumption is made that the rotational energy of the fragments is completely dissipated by friction during impact. The method is used by Westinghouse to calculate design exit velocities, but the details of the calculations are proprietary.

The second method used to estimate exit velocity was the SRI penetration formula (2). Because the formula is based on impact tests with several parameters outside the turbine test range and because an "equivalent diameter" of the complex turbine missile shape must be assumed, estimates from the SRI formula are even less reliable than others.

The third technique used to estimate exit velocities was a simple energy momentum calculation made at Sandia. It was felt that good engineering judgment could provide as good an estimate of exit velocities as the more rigid approaches discussed earlier. The technique is:

1. Assume an effective mass of the ring near the impact point for momentum transfer. For the geometries of this problem, this is not a crucial assumption.
2. Calculate the velocity of the missile after impact with this effective mass of the ring. The energy of the missile can then be determined.
3. Calculate the energy losses due to complete penetration based on the product of the average impact force applied to the missile times the total displacement which occurs during penetration. The forces can be obtained from the force to shear a plug of material from the ring, the force to mechanically deform the missile, or the force to locally fail the ring in tension. The allowable displacement before penetration or failure is related to the allowable strain in the ring.
4. If the above calculated energy loss (3) is larger than the impact energy (2) of the missile, the missile will be stopped. If the calculated energy is less than the impact energy, the missile will penetrate. The final or exit energy is given by this difference. The missile velocity can be obtained from this final energy.

Pretest calculations to determine energy losses during impact were made based on the above technique. The effective impacted mass was defined as the mass of a portion of ring having an area equal in radius to the radius of the impacting missile and the ring thickness. The penetration force was taken to be related to shear of the entire ring section.

During the pretest calculations of energy losses, it was difficult to make precise estimates of the allowable deformation to be used in the energy calculation. Two values were used (two and three times the ring thickness) which provide the two solutions shown in Tables 3-1 and 3-2.

Figure 3-1 gives the results from all three methods for the piercing missile orientation (See Figure 2-10). The reduction in velocity shown includes breakthrough of both the ring and the shell. The wide variation in results demonstrates that all simple methods depend heavily on the failure modes assumed and points out the need for test data.

Figure 3-2 gives the results for the blunt missile orientation (see Figure 2-10). The Sandia estimate compares closely with the upper Hagg and Sankey estimate because both assume that the ring shears only at the ends of the missile impact region. If, on the other hand, it is assumed that the ring shears also circumferentially along both edges of the 15.2-cm (6-in)-wide impact face of the missile, the lower Hagg and Sankey estimate in Figure 3-2 results. Assumption of this mode leads to the conclusion that the test missile would be contained in either the blunt or piercing orientation.

The target ring in the test is bolted to reinforced concrete backup structure in much the same way as the upper ring in a turbine is bolted to the lower ring. The strength of the target joint is greater than that of actual turbine joints (target bolt area is about 20% of ring cross-sectional area). It is predicted that the joint will fail by bolt fracture at initial missile velocities at the lower end of the curves in Figures 3-1 and 3-2. At higher velocities, the mass of the ring itself serves to react the missile penetration and the bolts are not expected to fail. It is important to note that shear breakthrough occurs in about the first milli-second whereas bolt failure occurs later. Therefore, containment with respect to penetration and containment with respect to joint failure will be uncoupled and can be interpreted separately.

3.2 LUMPED MASS MODELING

Simple lumped mass-spring modeling was used to find the magnitude of the impact loads. This modeling was used specifically to obtain design loads for the test fixtures. The masses of the projectile, the ring, and the reaction mass were considered. Due to the nonlinear impact behavior, nonlinear springs were used. This analysis provides a time history of loads, as well as the displacement, at various

Table 3-1

EXIT VELOCITY, PIERCING ORIENTATION
 1527 kg 120° TURBINE SEGMENT
 SANDIA ESTIMATE

Initial Velocity		Exit Velocity	
m/s	(ft/sec)	m/s	(ft/sec)
254	833	222 to 209	729 to 685
210	690	174 to 156	570 to 513
152	500	94 to 57	310 to 187
146	480	81 to 27	266 to 89
127	417	0 to 3	0 to 10

Table 3-2

EXIT VELOCITY, BLUNT ORIENTATION
 1527 kg 120° TURBINE SEGMENT
 SANDIA ESTIMATE

Initial Velocity		Exit Velocity	
m/s	(ft/sec)	m/s	(ft/sec)
254	833	201 to 186	658 to 610
210	690	155 to 136	508 to 446
152	500	64 to 0	210 to 0
146	480	51 to 0	168 to 0
127	417	0	0

Figure 3-1. Estimates of Exit Velocity for Piercing Orientation

Figure 3-2. Estimates of Exit Velocity for Blunt Orientation

points in the structure. The forces obtained in this analysis for the first loading condition are:

1. Maximum impact force, 44.5 MN (10×10^6 lb) at 0.0016 seconds,
2. Maximum reaction force, 15.1 MN (3.4×10^6 lb) at 0.012 seconds,
3. Maximum soil-structure force, 24.0 MN (5.4×10^6 lb) at 0.012 seconds.

This analysis shows that for the first test condition, the maximum reaction load occurs much later than the maximum impact load. The projectile penetration is unaffected by the size or stiffness of the reaction mass. The maximum reaction mass motion is less than 0.05 cm (0.02 in).

3.3 FINITE ELEMENT ANALYSIS

The third type of analysis done in conjunction with the turbine missile testing is based on the finite element method. This technique is at present very popular and is extensively used for structural analysis. A large number of finite element programs are available with a wide range of discretization complexity.

The finite element model used in conjunction with this test program basically fills two needs. First, this analysis will aid in understanding the test data. Second, after correlation with the test data the analysis might be used to predict the behavior of similar impacts under other more realistic impact conditions.

A two-dimensional beam finite element model was used for post-test structural analysis of the tests. It provided enough detail to satisfy all the requirements but yet was simple enough that most large engineering firms have the capability to make similar calculations. As discussed in Appendix C, the inner test ring was modeled as a series of 60 elastic-plastic beam elements. The impacting turbine missile was basically modeled as a series of rigid elements. Nonlinear load connections between the impacting mass and the ring elements were provided to simulate the local ring penetration and missile deformation. The number, location, and nonlinear load capabilities of these connecting elements were dictated by the initial impact geometry. In the piercing orientation, the interaction between the missile and the ring occurs basically at a single point, hence, only one connecting spring is required. In the blunt orientation, the interaction can occur along the entire breadth of the missile. Since this breadth is equal to the distance between 5 beam elements, 5 spring connections are needed to transfer the load from the missile to the beam. Since the curvature of the beam and the missile are different, the initial impact

is only at one location. Beam deformation will then permit the other springs to accept load. Mathematically this is handled with non-linear spring elements. A flexible connection was provided for the two beam-end reactions to account for the flexibility of the attachment bolts.

This assemblage of flexible beam elements, rigid beam elements, and nonlinear connection elements was analyzed using the ADINA computer program (3). This program is capable of treating large deformation, nonlinear dynamic response. A complete discussion of the analysis and results is given in Appendix C. Correlation of these results with the test data indicated that this modeling was sufficient to provide a reasonably good prediction of displacements and velocity changes. Prediction of strain was not as good.

Although this modeling was two-dimensional, the beam element approach (ADINA) can be used to predict the three-dimensional behavior that would result from off-axis impact. However, further validation with test data would be needed before the beam element approach could be used confidently to analyze turbine missile impacts of casing structures.

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Section 4

TEST RESULTS

4.1 INTRODUCTION

This section presents selected results from the piercing and blunt impact tests. The results include post test photographs of the test hardware, photometric data and strain gage data reduction, and an interpretation of the test data. Further information may be found in the Appendices.

4.2 PIERCING TEST

The missile impacted in the piercing mode at 149 m/s (488 ft/s) in the first test as measured by an image motion camera placed adjacent to the sled track. The accuracy for this measurement based on image motion cameras is approximately +1% (+1.5 m/s).

The residual velocity after ring breakthrough was about 99 m/s (325 ft/s) and about 87 m/s (285 ft/s) after shell perforation. These measurements (see Figure 4-1 and Appendix D) were obtained from data from overhead high speed photometric cameras. The estimated accuracy of the photometric velocity data reduction is +4.6 m/s (+15 ft/s). Selected overhead photographs of the piercing impact test are presented in Figure 4-2.

The test sequence as deduced from optical and strain data is as follows: The missile-ring impact duration was 2.1 to 2.5 ms, at which time the ring broke near the impact point. The missile continued until it hit the shell at about 9.5 ms. This is the approximate time of the third photograph shown in Figure 4-2. The right portion of the ring broke off at the top of the welded gusset plates at about 2.8 to 3.3 ms due to combined bending and tensile loads. This broken section was carried aft and rotated into and through the shell structure. Based on strain data taken near the left support, this portion of the ring was broken at the gusset plates at about 8.0 to 9.5 ms. The photographs of the broken ring and shell are shown in Figures 4-3 and 4-4. The photographs of the fractured supports are shown in Figure 4-5. The deformation of the missile and ring in the impact is reviewed

Figure 4-1. Displacement History of Missile in Test With Piercing Orientation

next. The ring deformation consisted of a local penetration of about 9.4 cm (3.7 in) through the depth, coupled with bending and shear deformation across the 50.8 cm (20 in) width of the ring (See Figure 4-6). The missile nose was also blunted about 2.5 cm (1 in). The deformations of the ring and the missile are shown in Figures 4-7 and 4-8.

As stated before, the ring broke at the right reaction at about 2.8 to 3.3 ms. The maximum dynamic tensile load carried near this failure region was estimated from ring strain data at 14.7 MN (3.3×10^6 lb) as shown in Figure 4-9. The static axial

$T = -6.0 \text{ ms}$

$T = 15.4 \text{ ms}$

$T = 1.1 \text{ ms}$

$T = 22.6 \text{ ms}$

$T = 8.3 \text{ ms}$

$T = 29.7 \text{ ms}$

Figure 4-2. Overhead Photographs of Piercing Impact

Figure 4-3. Post-Impact View of Exterior Surface of Simulated Turbine Casing (Piercing Orientation)

Figure 4-4. Post-Impact View of Interior Surface of Simulated Turbine Casing (Piercing Orientation)

Right Support

Left Support

Figure 4-5. Post-Test View of the Fractured Ring Supports

Figure 4-6. Cross-Section of Ring at Impact Zone

Figure 4-7. Deformation of the Ring After Impact in the Piercing Test

Figure 4-8. Deformation of the Missile After Impact in the Piercing Test

Figure 4-9. Upper Surface Membrane Strain Record (Gage No. 11) Near Ring Support, Piercing Orientation (Negative Sign Denotes Tensile Strain)

load capacity of the bolted joint is about 11.6 MN (2.6×10^6 lb). The dynamic peak occurred at 2 ms as indicated in Figure 4-9. It should be noted that all recorded strains in this region were less than the yield strength of the material. Unfortunately, the strain gage which should have measured the highest tensile strain in this region did not record data. Bending loads at failure could not be accurately deduced from strain data. The axial load at the left reaction peaked at about 10.7 MN (2.4×10^6 lb) at 2 ms.

The motion of the missile was determined from optical data as shown in Appendix D. The resulting velocities and kinetic energies of the missiles are shown in Table 4-1. Of the total energy loss, about 85% resulted from ring impact and only about 15% from the shell penetration. Regarding the amount of rotational energy in the missile after penetration, estimates of rotational velocity were used to calculate a rotational energy of about 34 kN·m (0.025×10^6 ft·lb) or about .2% of the total, a negligibly small amount.

Table 4-1

MEASURED MISSILE VELOCITY AND ENERGY HISTORY - PIERCING TEST

Sequence	Time (ms)	Missile Velocity		Missile Kinetic Energy		Energy Change		Cumulative Energy Dissipation %
		m/s	(ft/s)	MN·m	(10^6 ft·lb)	MN·m	(10^6 ft·lb)	
Before Impact	0	149	(488)	17.0	(12.7)			0
Ring Impact	2.5					9.6	(7.1)	
Free Flight	2.5 to 8.0	99	(325)	7.5	(5.6)			56
Shell Impact	8.0					1.7	(1.3)	
After Shell Impact	20	87	(285)	5.7	(4.2)			66

The energy loss during ring impact, which was $9.6 \text{ MN}\cdot\text{m}$ ($7.1 \times 10^6 \text{ ft}\cdot\text{lb}$), was absorbed in four basic components:

- 1) local plastic deformation of the ring,
- 2) local plastic deformation of the missile,
- 3) kinetic energy in the ring pieces, and
- 4) plastic strain energy in the ring pieces due to overall plastic deformation.

The energy absorbed due to local plastic deformation of the ring was obtained by taking the product of the impact force times the plastic deformation. Based on the photometric data of the missile, an average impact force of 40 MN ($9 \times 10^6 \text{ lb}$) acting over the measured distance of 12.7 cm (5 in) was used to obtain an estimated $5.1 \text{ MN}\cdot\text{m}$ ($3.75 \times 10^6 \text{ ft}\cdot\text{lb}$) energy absorption. The local distortion of the missile was about 2.54 cm (1.0 in), which resulted in an absorbed energy of $1.0 \text{ MN}\cdot\text{m}$ ($0.75 \times 10^6 \text{ ft}\cdot\text{lb}$) in the missile.

The kinetic energy of the ring segments was calculated from the measured velocities after ring failure. The total translational and rotational energy of the ring was estimated to be 0.4 to $1.1 \text{ MN}\cdot\text{m}$ (0.3 to $0.83 \times 10^6 \text{ ft}\cdot\text{lb}$). The energy absorbed by the ring was not obtained directly but was calculated by subtracting the known energy losses from the total. An energy of $9.6 \text{ MN}\cdot\text{m}$ less the first three energy losses resulted in an absorbed energy of 2.4 to $3.1 \text{ MN}\cdot\text{m}$ (1.8 to $2.3 \times 10^6 \text{ ft}\cdot\text{lb}$) due to permanent distortion of the ring sections.

4.3 BLUNT TEST

The missile impacted in the blunt orientation at 151 m/s (496 ft/s) in the second test as determined by an image motion camera positioned adjacent to the sled track. The accuracy for this measurement is approximately $\pm 1\%$. As indicated in Fig. 4-10, the residual velocity of the missile was about 7.6 m/s (25 ft/s) after shell impact. This measurement (see Appendix D) was obtained from data from overhead high speed photometric cameras. The accuracy of the photometric velocity data reduction was estimated to be $\pm 4.6 \text{ m/s}$ ($\pm 15 \text{ ft/s}$). Selected overhead photographs of the blunt impact test are presented in Figure 4-11.

The test sequence as deduced from optical and strain data is as follows: The missile-ring impact began on a point near the center of the leading right-hand

Figure 4-10. Displacement History of Missile in Test with Blunt Orientation

$T = -3.5 \text{ ms}$

$T = 16.4 \text{ ms}$

$T = 3.2 \text{ ms}$

$T = 25.1 \text{ ms}$

$T = 9.8 \text{ ms}$

$T = 29.7 \text{ ms}$

Figure 4-11. Overhead Photographs of Blunt Impact

half of the missile. Within 3 to 5 ms, the ring deformed locally and the missile rotated such that the entire front face of the missile was in contact with the ring (see Figure 4-11). At 2 ms the right reaction bolts began to break primarily due to tensile loading. The left reaction bolts broke later at 8.5 to 9.5 ms due to combined tension and bending loading. The missile and deformed ring were then free to move aft into the shell structure. The ring hit the shell structure at about 22 ms. Figures 4-12 and 4-13 show the distorted ring and shell after test.

The deformation of the missile and ring in the impact zone is reviewed next. There was little penetration of the missile into the ring, as occurred on the first test. The flange of the missile deformed the ring approximately 1.3 cm (0.5 in) at the impact point. A slight cracking on both sides of the missile near the juncture of the flange and web near the impact area was noted (see Figure 4-14). This appeared to be slight and probably did not affect the test results.

As stated before, the bolts broke at the right reaction starting at 2 ms. The maximum dynamic tensile load carried near this failure region was estimated from strain data at 19.1 MN (4.3×10^6 lb) (see Appendix D). The static axial load capacity of the bolted joint was about 11.6 MN (2.6×10^6 lb). The load in the ring 0.5 m from the plane of the bolts shows a peak at 2 ms, a drop, and then a rise to a second peak near 7 ms as indicated in Figure 4-15. Bending loads at failure caused severe yielding at the extreme fibers. Measured fiber strains near the supports were above 6000 microstrain.

The failure was, of course, due to failure of the bolts. Most of the bolts broke in a ductile manner in tension as expected; however, some failed by thread shear in the nuts. Photographs of some broken bolts are shown in Figure 4-16. There appeared to be little elongation through the length of the bolts, and thus little energy was absorbed by bolt elongation. Tensile tests were performed after the blunt test on several samples of Grade 8 bolts and nuts to determine their breaking strengths. Table 4-2 shows the results of tensile tests with a bolt/nut combination and bolt/special threaded fastener combination as compared with the rated loads. Other tests performed on the bolts and nuts are reported in Appendix A. No incipient failure of the ring or welded gusset area could be seen. The left reaction force peaked at near 8.9 MN (2×10^6 lb). The bending component of load was sufficient to cause bolt failure as derived from strain gage data on the ring (see Figure 4-17). This is less than the 11.1 MN (2.5×10^6 lb) design load value for the joint. The difference may be due to the failure of the bolts in the region at less than specified bolt capacity or to the contribution of bending at the support.

Figure 4-12. Post-Impacted Shape of the Ring and Shell Structure

Figure 4-13. Post-Impact Orientation of Missile and Ring Structure

Figure 4-14. Rotor Cracks Near Junction of Web and Flange After Blunt Impact Test

Figure 4-15. Right Ring Reaction Load for Blunt Test as Derived from Filtered Strain Gage Data (Gage No. 13)

Table 4-2

TENSILE TESTS ON GRADE 8 BOLTS FOR CASING TESTS

<u>Item</u>	<u>Failure Load</u>		<u>Failure Mode*</u>
	kN	(10 ³ lb)	
1-in-diam - 8 A490 Bolt			
Bolt with nut	209.1	(47.0)	1
Bolt with special threaded holder	400.5	(90.0)	2
Rated load (1070 ft-lb torque)	404.5	(90.9)	
1-1/2-in-diam - 6 A490 Bolt			
Bolt with nut	698.7	(157.0)	1
Bolt with special threaded holder	1179.3	(265.0)	2
Rated load (3700 ft-lb torque)	937.8	(210.8)	

- * 1. Stripped threads in nut.
 2. Rupture through threaded cross section
 (1 in = 2.54 cm; 1 ft·lb = 1.355 N·M)

Attachment Hardware

2.5 cm Shell and 3.8 cm Ring Bolts

Figure 4-16. Condition of Ring and Shell Bolts After the Blunt Impact Test

The motion of the missile was determined from optical data. (See Appendix D). The measured velocities and resulting kinetic energies are shown in Table 4-3 for several times after impact. As seen from this table, about 93% of the energy was lost as a result of the ring impact. The first portion of the ring impact caused some conversion of translational energy to rotational energy (less than 1% of the original value). Most of the energy loss during ring impact was due to beam behavior. Based on the product of the impact force times the deformation, 0.3 to 0.7 MN·m (0.25 to 0.5×10^6 ft·lb) of energy was lost during the local impact behavior. About 0.1 to 0.5 MN·m (0.08 to 0.33×10^6 ft·lb) of energy was converted into kinetic energy of the ring. The remainder of the energy was absorbed in plastic distortion of the ring and connecting bolts.

Selected results from both tests are compared with finite element computer analysis in Appendix C.

Photometric and strain gage data from both tests along with a description of the methods used are included in Appendix D. An error analysis of the strain gage and photometric data reduction is provided.

Figure 4-17. Load in Ring 0.5 m from Left End for Blunt Test as Determined by Strain Gage Data (Gage No. 12) on the Ring.

Table 4-3

MEASURED MISSILE VELOCITY AND ENERGY HISTORY - BLUNT TEST

Time	Translational Velocity		Rotational Velocity	Translational [*] Energy		Rotational ^{**} Energy		Total Energy	
	m/s	(ft/s)		MN·m	(10 ⁶ ft·lb)	kN·m	(10 ³ ft·lb)	MN·m	(10 ⁶ ft·lb)
0	51	(496)	0	17.17	(12.68)	0	(0)	17.17	(12.68)
5	59	(195)	29.7	2.72	(2.01)	105	(77.5)	2.82	(2.08)
10	38	(125)	22.7	1.11	(0.83)	62.1	(45.8)	1.18	(0.88)
20	24	(80)	13.1	0.45	(0.33)	20.3	(15.0)	0.47	(0.35)
30	8	(25)	--	0.03	(0.02)	--	--	0.03	(0.02)

$$* E_T = 1/2 M V^2 = 4.40 V^2 \text{ (MN·m)}$$

$$** E_R = 1/2 I \omega^2 = 1059 \omega^2$$

Appendix A

MATERIAL PROPERTY EVALUATION

This Appendix presents the results of a metallurgical evaluation and mechanical property measurements of the various structural components involved in the turbine impact test.

The test hardware was fabricated from cold-rolled plates of certified ASTM A515 Grade 65 steel. Both ring sections were flame cut from a single plate and were formed into a semi-circle by rolling and rerolling in a rolling mill. Channel stiffeners were used to hold the dimensions of the ends of the formed semicircular ring to ± 16 mm (± 0.06 in). Weld preparation to the ends of the rings was performed prior to base attachment. The ring bases were obtained from an unformed plate and were attached to the ends of the rings by welding. Gusset plates were added to provide additional reinforcement. The test specimens described in this section were obtained from flat plate stock. As a final step, both rings and test specimens were stress relieved in a furnace. The shells were fabricated and stress relieved in a similar manner. Additional ring and shell information is presented in Appendix B.

TURBINE RING BASEPLATE

Quantitative analysis showed that the 140-mm-thick ring baseplate meets the chemistry requirements for ASTM A515 Grade 65 (Table A-1). Metallographic examination indicated that the baseplate was fine-grained, ASTM grain size No. 7 (Figure A-1). This unformed material is from the same lot as that used in the 127-mm-thick rolled ring test sections.

Figure A-2 shows a typical tensile stress-strain curve for the turbine baseplate material. Tensile loading of this alloy resulted in discontinuous yielding and the development of a Luders band region at low strains. Tensile-compressive cycling experiments indicated that any mechanical modeling of this material would require the use of combined kinematic-isotropic hardening rules (Figure A-3). While there was little influence of sample orientation on the tensile properties (Table A-2), increasing strain rate did tend to increase the tensile yield and ultimate strengths, although it did not affect the strain-hardening behavior of the A515 Grade 65 baseplate (Table A-3).

Table A-1

CHEMICAL COMPOSITION OF TURBINE RING
BASEPLATE ELEMENT (wt %)

	<u>C</u>	<u>Mn</u>	<u>P</u>	<u>S</u>	<u>Si</u>
Specification (A515 Grade 65)	0.33 (max)	0.9 (max)	0.035 (max)	0.04 (max)	0.13-0.33
Actual	0.34	0.5	0.006	0.034	0.23

Figure A-1. Typical Microstructure of Turbine Ring
Support Plate (magnification: 100X*)

* Please note that the illustration(s) on this page
has been reduced 10% in printing.

A-4

Figure A-2. Typical Tensile Stress-Strain Curve for Turbine Baseplate Alloy

Figure A-3. Cyclic Stress-Strain Response of Turbine Baseplate Alloy

Table A-2

INFLUENCE OF ORIENTATION ON THE ROOM-TEMPERATURE
TENSILE PROPERTIES OF TURBINE BASEPLATE MATERIAL*

Orientation	0.2% Yield Strength (MPa)	Ultimate Tensile Strength (MPa)	Uniform Elong. (%)	Total Elong. (% in 2.54 cm)	Red. Area (%)
Longitudinal	259	464	19.4	31.8	52.7
Long Transverse	264	466	20.9	34.5	53.3
Specification (ASTM 515 Grade 65)	241(min)	448-586	--	23(min)	--

*Strain rate: $1.6 \times 10^{-4} \text{ s}^{-1}$; results average of duplicate tests

Table A-3

INFLUENCE OF STRAIN RATE ON THE ROOM-TEMPERATURE
TENSILE PROPERTIES OF TURBINE BASEPLATE MATERIALS*

Strain Rate (s^{-1})	0.2% Yield Strength (MPa)	Ultimate Tensile Strength (MPa)	Uniform Elong. (%)	Total Elong. (% in 2.54 cm)	Red. Area (%)
1×10^{-4}	267	467	19	33	55.9
1×10^{-3}	273	475	18	31.9	53.6
1×10^{-2}	289	483	19	32.6	53.8
1×10^{-1}	286	489	18	30.4	54.6
1×10^0	316	513	20	31.6	53.2

*Orientation: Long Transverse; results average of duplicate tests

TURBINE RINGS

The 127-mm-thick rings were fabricated from cold-rolled A515 Grade 65 carbon steel. Charpy samples taken from the L-S grain direction per ASTM E 399-72 were obtained from the plate prior to forming. Figure A-4 shows that the 20-joule absorbed Charpy impact energy transition temperature for this material was approximately 290 K, i.e., slightly below room temperature.

TIEDOWN NUTS AND BOLTS

Two groups of tiedown nuts and bolts, nominally 25.4-mm- and 38.1-mm-dia., were originally purchased under Federal Specifications FF-N-836D and FF-S-85C for Grade 8 nuts and bolts, respectively. Figure A-5 illustrates the types of failure modes that were observed when the bolts were examined following the simulated blunt impact test. All 25.4-mm-dia. bolts, Nos. 1 through 5 in Figure A-5, failed in a relatively ductile manner as a result of overstressing. Two of the 38.1-mm-dia. bolts, Nos. 8 and 10, failed in a similar fashion. However, bolts Nos. 6, 7 and 9 were simply stripped from their nut mountings.

Federal Specification FF-S-85C states that steels to be used for Grade 8 bolts must have a carbon content between 0.28 and 0.55 wt%, a maximum of 0.04% phosphorous and a maximum of 0.05% sulphur. Federal Specification FF-N-836D states that steels to be used for Grade 8 nuts have a maximum of 0.04% phosphorous and a maximum of 0.05% sulphur. In addition, the chemical composition of the bolts must conform to Federal Standard No. 66 for alloy steel. Table A-4 shows the results of a chemical analysis of selected nuts and bolts used for the turbine impact tests. All bolts meet the federal specification for Grade 8 alloy steel. The tiedown nuts also meet the Grade 8 specification.

The mechanical properties specified by Federal Specifications FF-N-836D and FF-S-85C for Grade 8 nuts and bolts are presented in Table A-5. The bolt hardness measurements are required to be made at the center of the threaded section, one diameter from the end of the bolts. Bolt tensile results, on the other hand, are to be obtained from samples removed from the shank portion of the bolt. Figure A-6 shows a typical stress-strain curve for the bolts examined. The tensile and hardness measurements performed on Grade 8 nuts and bolts used for the turbine impact test are summarized in Table A-6. Comparison of these results with those specified in Table A-5 showed that both size bolts meet the tensile property

Figure A-4. Influence of Test Temperature on Charpy Impact Energy of Inner Ring

Table A-4

CHEMICAL COMPOSITION OF TIEDOWN NUTS AND BOLTS

Designation	Element (wt%)												ASI
	C	Mn	P	S	Si	Cu	Ni	Cr	Mo	V	Ti	W	Equivalent
Bolts													
No. 1	0.39	0.87	0.015	0.031	0.30	0.08	0.07	1.0	0.20	<0.01	0.02	<0.01	4137
No. 2	0.38	0.86	0.013	0.033	0.27	0.20	0.15	0.98	0.21	<0.01	0.02	<0.01	4137
No. 8	0.37	0.86	0.014	0.032	0.26	0.20	0.15	0.98	0.21	<0.01	0.02	<0.01	4137
Nuts													
25.4 mm	0.44	0.72	0.029	0.020	0.23	0.01	0.02	0.10	0.01	<0.01	0.01	<0.01	1040
38.1 mm	0.30	0.71	0.023	0.023	0.20	0.01	0.02	0.13	0.01	<0.01	0.01	<0.01	1030

Table A-5

MECHANICAL PROPERTIES SPECIFIED PER FEDERAL SPECIFICATION
FF-N-836D AND FF-S-85C FOR GRADE 8 NUTS AND BOLTS

	Nominal Size (mm)	Proof Load (MPa)	Tensile Strength (MPa) min	Hardness, R_C	
				min	max
Bolt	6.35-38.1	827	1,034	32	38
Nut	6.35-38.1	1,034	--	24	32

requirements. However, bolt No. 7, which was stripped from its mounting nut, did not meet the hardness specification. In addition, the hardness of the 38.1-mm-dia. nut also did not meet the minimum hardness specification. These results imply that those nuts/bolts which were stripped were not Grade 8. Those bolts that failed in a ductile mode did meet Grade 8 specifications.

Figure A-5. Photo of Selected Tiedown Bolts
After Turbine Impact Test

Figure A-6. Typical Stress-Strain Curve for Grade 8 Bolt

Table A-6

MEASURED MECHANICAL PROPERTIES FOR GRADE 8 NUTS AND BOLTS
FOR TURBINE IMPACT TEST

	Nominal Size <u>Dia. (mm)</u>	Yield Strength <u>(MPa)</u>	Tensile Strength <u>(MPa)</u>	Hardness <u>(R_C)</u>
Bolt				
No. 1	25.4	1,058	1,131	36
No. 7	38.1	1,005	1,086	26
Nut	38.1	--	--	21

Appendix B

DRAWINGS OF TEST STRUCTURES AND DESIGN OF THE REACTION MASS FOR THE TURBINE CASING TESTS

In the test, the casing is represented by a half ring made of ASTM A515, Grade 65 steel, 12.7 cm (5 in) thick, 50.8 cm (20 in) wide, and with a radius of 215.9 cm (85 in) (Figure B-1). Behind the half ring is a shell of the same steel, 3.2 cm (1.25 in) thick, 182.9 cm (72 in) wide, and with a radius of 317.5 cm (125 in) (Figure B-2). The ring and shell are arranged in such a way that the shell encloses the ring concentrically, with a separation of 101.6 cm (40 in) between them (Figure B-3). Both are anchored firmly with base plates to the top of a "reaction mass" that absorbs the energy of the impact from the hub segment (Figures B-4, B-5, and B-6).

The reaction mass (Figures B-7 through B-9) is made of concrete with a compressive strength of 20.7 MPa (3000 lb/in²) and weighs 9.08×10^5 kg (1000 tons). It is reinforced with steel longitudinally, vertically, and laterally; the last merely protects against heat expansion and shrinking. Lying in a depression in the ground, the concrete mass is flat-bottomed and has a pedestal 0.9 m (3 ft) high and 1.5 m (5 ft) wide at the front, or impact, end. Not including the pedestal, it measures 2.1 m (7 ft) high at the front end and slopes down toward the back where it measures 0.9 m (3 ft) high. Including the 1.5 m (5 ft) of pedestal, it is 22.9 m (75 ft) long and 9.8 m (32.1 ft) wide. On top of this mass is piled 7.4×10^5 kg (810 tons) of earth, which contributes to the reaction mass and stops the missile in the event of complete perforation of the target structure. From the bottom of the reaction mass to the top of the earth overburden is 5.2 m (17 ft) (Figure B-7).

If the forces acting against it are large enough to overcome its inertia and soil friction and pressures, the mass is designed to slide rather than overturn. The factor of safety (FS) against it overturning was calculated to be 3.6.

Figure B-1. Test Ring Section, Dwg. No. T51337

Figure B-2. Test Shell Section, Dwg. No. T52444

B-4

Figure B-3. Ring and Shell Assembly, Dwg. No. T52863

Figure B-7. Reaction Mass for 3000' Test Track, Dwg. No. 95042 A-1

Figure B-8. Reaction Mass for 3000' Test Track, Dwg. No. 95042 A-2

Resisting the movement of the reaction mass would be its own inertia and the passive pressure, inertia, and friction of the soil (Figure B-10). To calculate total inertia, the weight (W) of the ring and shell, of the concrete [2400 kg/m³ (150 lb/ft³)], and of the soil [1920 kg/m³ (120 lb/ft³)] must be included. Passive pressure (P_p) of the soil against the vertical sides of the mass, as well as the resistive forces (R_F) against compression of the soil at the 0.9 m (3 ft) high back face of the mass must also be calculated. For frictional forces of the soil, a coefficient of 0.5 for coarse-grain soil with some silt--typical of soil at the test site--was assumed.

Should the hub segment impart more than 17.8 MN (4 million lb), 8.9 MN per side, to the reaction mass, it will tend to slide, as can be shown by adding the horizontal forces, as follows (see Figure B-10):

$$\begin{aligned}
 F &= W[f_1 + f_3 + f_2 + f_4 + (f_6 + f_7 + f_8) \cos (3.81^\circ) + P_{p5} \\
 &\quad + P_{p4} + P_{p6} + R_F] \\
 &+ 32[11.25 + 6 + 9.23 + 7.2 + 24 + 37.2 \cos (3.81^\circ) + 2.39 \\
 &\quad + 2.88 + 9.58 + 16.74] = 4000 \text{ kips or } 17.8 \text{ MN}
 \end{aligned}$$

Each of the two base plates of the ring is attached to the top of the concrete pedestal with 12 horizontal and 50 vertical A490 1-1/2"-6 bolts. Each support is calculated to resist a design load of 13.3 MN (3 million lb), for a total of 26.7 MN (6 million lb). The shell is likewise attached with two supports, each with 14 horizontal A490 1"-8 bolts and 28 vertical A490 1-1/2"-6 bolts. Each of these supports is calculated to resist a design load of 6.7 MN (1.5 million lb), for a total of 13.3 MN (3 million lb).

To protect the integrity of the pedestals, the horizontal bolts in the supports are designed to fail at 83% and the vertical at 100% of the design load of the pedestal. Since the reinforcement steel in the pedestal can withstand 27.6 MN (6 million lb) parallel to the impact of the missile, it will suffer no damage and can be reused. As indicated in Section 3, impact analysis of the entire structural system showed that the short duration during which soil-structure force

Figure B-10. Force Diagram For Reaction Mass

tends to exceed the sliding capacity of the reaction mass gives rise to a minute reaction mass displacement (less than 0.05 cm, or 0.02 in).

Photographs of construction of the test site are shown in Figures B-11 through B-15.

Figure B-11. Installation of Reinforcing Structure for Pedestal Support

Figure B-12. Reinforcing Steel for Pedestal Support (rear) and Concrete Reaction Mass (foreground)

Figure B-13. Pouring of Concrete in Reaction Mass

Figure B-14. Curing of Concrete with Plastic Blankets

Figure B-15. Removal of Forms from Concrete Pedestals

Appendix C

STRUCTURAL ANALYSIS

The two-dimensional finite element analysis used to model the behavior of the missile and turbine structure in both tests is described in this appendix.

C-1 FINITE ELEMENT MODEL

The model used for post-test structural evaluation was based on beam finite elements in combination with a rigid impacting missile and spring connecting elements.

Figure C-1 shows a diagram of the structural model. The inner test ring was modeled as a series of 60 beam elements. The impacting turbine missile was basically modeled as a rigid mass weighing 1527 kg (3366 lb). Several rigid beam elements were used for the mass. Connections between the impacting mass and the ring elements were provided to simulate the local ring penetration and missile deformation. For the test with piercing orientation, only one nonlinear connection was used. For the test with blunt orientation, a series of connections was required due to the large impact area of the blunt test. A flexible connection was provided for the two beam-end reactions. This spring was sized to account for the flexibility of the attachment bolts. During the first test, this joint had very little motion during the times of test interest. During the second test, the failure occurred in the joint itself. The mass and inertia of the end flange assemblies were not modeled.

This assemblage of flexible beam elements, rigid beam elements, and flexible connection elements was analyzed using the ADINA computer program (1). This program is capable of treating large deformation, nonlinear dynamic response. The test ring was modeled with 60 beam elements. The material was modeled as elastic-plastic as shown by the static stress-strain curve in Figure C-2. The material near the impact region experienced high strain rates, 20 to 100 cm per cm per second. At high strain rates, steel material response is characterized mainly by an increase in the yield strength. An estimate of the stress-strain behavior of this material at high strain rates is shown in Figure C-2. A nominal increase in the yield stress of the material from 310 MPa (45,000 lb/in²) to 586 MPa (85,000 lb/in²) was used in this analysis for the material in the regions of high strain rates.

Figure C-1. Finite Element Model for Piercing Test

The impacting mass was modeled by using very rigid beam elements. This effectively provides for a rigid missile motion. The analyses treated a non-rotating impacting mass as in the tests, although the rotation of the mass could be included if desired.

The ends of the model ring were fastened to a rigid base by a spring representing the tiedown bolts. Twelve 3.8-cm (1-1/2-in)-dia bolts restrained each end. The axial load behavior of these bolts could be linear or nonlinear in the model. The effect of these bolts was not important during the piercing test in which the ring failed very quickly near the impact point, but was very important during the blunt test. For the correlation analysis, the load deformation characteristic of the bolts was taken as linear for two reasons. First, the nonlinear characteristics of the bolts under dynamic loads were not accurately known. Second, the linear assumption should provide a reasonable estimate of the general ring and missile behavior. The equivalent linear spring constant was estimated to be 10.1 MN/m (54×10^6 lb/in).

Figure C-2. Stress Strain Curve Used in Analysis Showing Assumed Strain Rate Effects for Ring Material

The last elements of the structural model involved the connection between the impacting mass and the ring. This connection represented the local ring and missile deformation which was not accounted for in the beam elements; i.e., distortion of the cross-section and flow of material from beneath the concentrated impact load were not included in a beam type element. A connection or spring must be included to represent this nonlinear force-displacement characteristic, whose stiffness should represent compressive behavior. In the model used for the correlation study for the piercing test, only one nonlinear spring was used to connect the mass to the ring since the impact was fairly concentrated over a small area. In the blunt

orientation the interaction can occur along the entire breadth of the missile. Since this breadth is equal to the distance between 5 beam elements, 5 spring connections were used to transfer the load from the missile to the beam. Since the curvatures of the beam and the missile are different, the initial impact occurs only at one location. Beam deformations will then permit the other springs to accept load. Mathematically this was handled with non-linear spring elements.

The spring values used for analyses of the first test are shown in Figure C-3. The nominal curve represents an estimate based on the following rationale:

1. The maximum penetration force is limited by the force to shear a plug out of the ring material. Based on a shear strength of 0.65

Figure C-3. Impact Spring Force Between Missile and Ring (Piercing Test)

times the tensile strength and a shear area of 1316 cm^2 (204 in^2) (Figure C-4), the static yield force is 26.7 MN ($6.0 \times 10^6 \text{ lb}$) and the static ultimate capacity is 38.3 MN ($8.6 \times 10^6 \text{ lb}$). These are based only on plug shear and represent upper bounds.

2. Due to the shape of the stress-strain curve of the material, the load deflection curve should rise steeply and then flatten out. Unloading will be nearly parallel to the loading curve.
3. The test results indicated a large local ring deformation of about 9.4 cm (3.7 in) as shown in Figure C-5. Deformation of the missile was about 2.5 cm (1 in).
4. Analyses of an axisymmetric steel penetrator impacting a steel plate were made using a dynamic finite element solution. These solutions showed a stiffness similar to the nominal curve in Figure C-3.

Figure C-4. Shearing Geometry for Piercing Test

It will be shown later that the structural response of both the missile and ring were not highly dependent upon the exact shape or magnitude assumed for this spring curve in the piercing test.

The impact spring connections used for analysis of the blunt test were somewhat different. (See Figure C-6.) The impact region was about 152 cm (60 in) along the ring length compared with 7.6 to 10 cm (3 to 4 in) in the piercing test. Five effective springs were used to couple the mass to the ring in the blunt impact model. The spring stiffnesses were adjusted to account for the initial free distance between the ring and the missile. Since the rotation of the mass was not modeled, all springs were attached to the center of the impacting mass.

As before, spring constants were estimated by simple means. Various spring values were used to study the effects of this parameter. These results will be discussed later.

The most difficult parameter to assign or interpret was the failure criteria. In the case of this model, several failure parameters were required. Only two failure parameters were included in this model although others could be used if desired. The first failure mode was failure of the ring due to total strain. Failure was assumed to occur when the strain at any location exceeded the uniaxial static ultimate tensile strain of 26%. The second failure mode related to the tensile strength of the bolted end joints. Tensile failure load (static) was assumed as 11.6 kN (2.6×10^6 lb).

Figure C-5. Cross-Section at Impact Zone, Piercing Test

Figure C-6. Finite Element Model for Blunt Test

C-2 TYPICAL RESULTS FROM THE FINITE ELEMENT ANALYSIS

Calculated results for both the piercing and blunt tests are presented and discussed in this section.

Piercing Test

During the piercing test, the initial missile impact caused impact spring compression, motion and strain in the ring. At 2.1 ms the ring exceeded the allowable 26% strain, and ring failure resulted. The ring element at the impact point was then eliminated from the structural stiffness matrix and the analysis of the broken ring segments continued. No further failure criteria for the support was included for the first test condition since ring behavior after missile penetration was of little importance.

Typical displacement plots for the piercing test are given in Figures C-7 and C-8. The missile displacement is given in Figure C-7. Ring displacement at two locations is shown in Figure C-8. The slopes of these curves are the velocities. The missile velocity curve is shown in Figure C-9. After 2.1 ms no further load was applied to the missile. Therefore, the missile velocity was constant.

The impact force between the missile and ring is plotted in Figure C-10. This force came directly from the impact spring force and was directly related to the spring characteristics. The axial load at the end of the ring is plotted in Figure C-11. The load drop-off at 2.1 ms was due to ring failure at the impact point. The surface strains in the ring near the impact point and near the right support are given in Figures C-12 and C-13.

Figure C-7. Calculated Missile Displacement for Piercing Test

Figure C-8. Calculated Ring Displacement for Piercing Test

Figure C-9. Calculated Missile Velocity for Piercing Test

Figure C-10. Calculated Missile Impact Force for Piercing Test

Figure C-11. Calculated Right Reaction Load for Piercing Test

Figure C-12. Calculated Surface Strain Near Impact Point for Piercing Test

Figure C-13. Surface Strain near Right Support for Piercing Test

Blunt Test

The calculated results for the blunt test are discussed next. The missile impact caused impact forces and motion similar to the first test. The strain at the impact point was fairly large but did not exceed the critical value. Therefore, the ring did not break and large axial loads were carried into the supports. The analysis was terminated upon reaching a critical force in the support bolts.

The missile displacement is given in Figure C-14. The ring displacement at nodal point 36 is given in Figure C-15. The missile velocity is plotted in Figure C-16. The impact force is the total of the forces in all the springs which join the missile and ring. The simplified spring representation between the missile and

Figure C-14. Calculated Missile Displacement for Blunt Test

Figure C-15. Calculated Ring Displacement at Nodal Point 36 for Blunt Test

Figure C-16. Calculated Missile Velocity for Blunt Test

ring includes the capability for both tensile and compressive loads, although tensile loads, of course, cannot occur. The calculated missile impact force is shown in Figure C-17. The tensile loads in the spring coupling probably created the large oscillations in force shown at late times in Figure C-17.

The calculated reaction load at the right support is shown in Figure C-18. The spring characteristics used to obtain this solution were not realistic. The reaction forces were, therefore, too large. Calculated surface strains in the ring are plotted in Figures C-19 and C-20. The critical strain region of the ring was close to the region of load discontinuity at the end of the missile. As stated before, the strain did not reach a critical value of 26%. Hence ring failure near the impact zone was not predicted.

C-3 PARAMETER STUDIES - Piercing and Blunt Tests

Parametric studies were made to determine the effects of some of the critical and uncertain model characteristics. These studies provided a basis for evaluating the errors associated with some of the assumptions. Early analysis indicated that the effects of strain rate on the ring material behavior were important. Strain rates near 50 cm/cm/sec can elevate the yield stress by 1-1/2 to 2-1/2 for this type of

Figure C-17. Calculated Missile Impact Force for Blunt Test

Figure C-18. Calculated Right Reaction Load for Blunt Test

Figure C-19. Calculated Strain on Outer Surface of Ring Near Impact Point for Blunt Test

Figure C-20. Calculated Strain in Ring Near Right Reaction for Blunt Test

material. The increase in stress indicated by the dashed curve in Figure C-2 was assumed to govern the high-strain-rate behavior of the material in the vicinity of missile impact. The effect of this increased yield strength for this ring in the piercing test is shown in Table C-1. The higher strength caused a slight reduction in missile velocity, and later failure times were predicted. The strains in the ring were changed as expected. Later failure times were predicted.

The effect of varying the impact spring characteristics as indicated in Figure C-3 is given in Table C-2 for the piercing test. The maximum impact force was altered

Table C-1
EFFECTS OF YIELD STRENGTH (PIERCING TEST)

Yield Strength*	Maximum Impact Force	Ring Displacement at 2 ms		Missile Velocity at 2 ms		Strain Near Impact Point at 2 ms	Predicted Failure Time
MPa (lb/in ² x 10 ⁶)	MN (lb x 10 ⁶)	cm	(in)	cm/sec	(in/sec)	cm/cm	ms
310 (45)	32 (7.2)	19.3	(7.6)	10719	(4220)	.29	1.4
586 (85)	32 (7.2)	16.0	(6.3)	10668	(4200)	.26	2.0
586 ¹ (85) ¹	32 (7.2)	16.3	(6.4)	10592	(4170)	.255	2.1

* Yield strength elevated only in elements 21 to 30.

Table C-2
EFFECTS OF IMPACT SPRING (PIERCING STRENGTH)

Impact Spring	Maximum Impact Force	Ring Displacement at 2 ms		Missile Velocity at 2 ms		Strain Near Impact Point at 2 ms	Time When Strain is 0.26
	(MN) (lb x 10 ⁶)	cm	(in)	cm/sec	(in/sec)	cm/cm	sec
Nominal	32 (7.2)	15.5	(6.1)	10744	(4230)	0.249	2.15
Curve A	32 (7.2)	16.3	(6.4)	10592	(4170)	0.255	2.10
Curve B	41 (9.2)	13.5	(5.3)	10897	(4290)	0.267	1.95

considerably; however, the missile velocity at penetration was not changed by more than 3%. Since the missile penetration characteristics are of prime concern, the nominal spring characteristics seem quite applicable.

The effects of the impact spring characteristics for the blunt test are more complex due to the fact that a delay in the loading of the outer springs is needed to account for the initial gap between the missile and the beam. The actual load displacement characteristics of these outer springs is characterized by an initial load free displacement followed by a very stiff spring constant after impact. The unloading curve should follow the same path. Since the ADINA program, as presently written, is limited in its ability to handle non-linear springs, a series of spring approximations were studied. As with the piercing configuration, these springs changed the impact loads considerably although the missile behavior was not changed as much. Further analytical study should include better representation of these impact spring connections.

The effects of the rigidity of the support connection bolts was also studied. The spring stiffnesses shown in Table C-3 were used in a parametric study. As expected the impact load was unchanged, however, the reaction load changed by about 12 percent. Since the test failed in bolted connection, further analytical study should include better characterization of the bolted connection.

Table C-3
EFFECTS OF REACTION SPRING (BLUNT TEST)

Bolt Connection	Maximum Impact Force		Missile Velocity at 5 ms		Maximum Reaction Load	
	MN	(lb x 10 ⁶)	cm/sec	(in/sec)	Mn	(lb x 10 ⁶)
Rigid*	64	(14.4)	4623	(1820)	32.5	(7.3)
Elastic**	64	(14.4)	4648	(1830)	32.0	(7.3)
Elastic-Plastic [†]	64	(14.4)	5309	(2090)	28.5	(6.4)

* Rigid reaction support.

** Reaction spring constant, $k = 9457 \text{ MN/m}$ ($54 \times 10^6 \text{ lb/in}$)

[†] Reaction spring constant, $k_0 = 9457 \text{ MN/m}$ ($54 \times 10^6 \text{ lb/in}$), Yield at 8.0 MN ($1.8 \times 10^6 \text{ lb}$), $k_1 = 0$

C-4 CORRELATION WITH TEST DATA

Comparisons of the calculated results obtained from this model and experimental data taken from both tests (see Appendix D) are given in this section. Good correlation resulted for the piercing test. The correlation with test data from the blunt test was not as good. Errors in impact spring constants and beam reaction spring constants were the suspected reason for this. Adjustment of the sensitive model parameters could be made to enhance this correlation. It was felt that such adjustments were not necessary for this post-test evaluation.

Figures C-21 and C-22 show a comparison of the calculated and experimental displacements for both the missile and the ring during the piercing test. Comparisons of the surface strains on the ring are shown in Figures C-23 and C-24. As expected, considerably more error resulted in the strain correlation than for displacement. The calculated missile velocity after ring penetration was 107 m/s (350 ft/s) or about 8% higher than the measured value. Considering the possible experimental error in velocity measurements, this seems quite acceptable.

Comparisons of calculated and experimental displacements obtained from the blunt test are shown in Figure C-25. As expected, the correlation was fairly good. The comparison of the velocity of the missile is shown in Figure C-26.

C-5 CONCLUSIONS

A fairly simple structural model has been presented which provides an aid for understanding the test data. With further development this model is capable of predicting behavior for impact conditions which are more prototypical than the simplified conditions in the tests. Other parameters which could be included in further work are: missile weight, missile rotation, ring size, and impact locations off the circumferential centerline of the ring.

REFERENCES

1. K. Bathe, "ADINA - A Finite Element Program for Automatic Dynamic Incremental Nonlinear Analysis," Report 82448-1, Massachusetts Institute of Technology, September 1975.

Figure C-21. Correlation of Missile Motion (Piercing Orientation)

Figure C-22. Correlation of Ring Motion (Piercing Orientation)

Figure C-23. Correlation of Strain (Strain Gage 4) on Outside of Ring (Piercing Orientation)

Figure C-24. Correlation of Strain (Strain Gage 7) on Inside of Ring (Piercing Orientation)

Figure C-25. Correlation of Missile Displacement (Blunt Orientation)

Figure C-26. Correlation of Missile Velocity (Blunt Orientation)

Appendix D

DETAILED TEST DATA

D-1 INTRODUCTION

The three basic types of test data recorded during these tests are given in this appendix. The first, displacement and velocity data, was obtained from high speed camera coverage (Section D-2). The second, strain data, was obtained from strain gages mounted on both the ring and shell (Section D-3). The third, posttest measurements of the deformed rotor segment and ring, was obtained from mechanical measurements (Section D-4).

D-2 PHOTOMETRICS

High-speed photometric cameras were positioned to observe missile, ring, and shell motion. Primary coverage (for data analysis) was overhead. The camera plan is shown in Figure D2-1.

Overhead coverage of the entire ring and shell structure was provided by Photo-sonics 4M, Milliken, and Hycam cameras. Other overhead cameras were positioned to observe specific areas of the target. Two Hycam rotating prism cameras were positioned directly overhead to observe ring motion (marked by o's in Figure D2-1); these cameras had lenses which provided a narrow field of view. One Hycam and one Milliken (pin registered) camera was positioned behind the turbine shell (marked by X's in Figure D2-1) to observe trajectory and rotation of the missile subsequent to impact. At least two missile lengths were provided behind the shell to observe missile motion.

The locations of marker positions on the missile and simulated turbine casing and ring are shown in Figure D2-2. For both tests, the motion of the c.g. of the missile parallel to the flight path is given in the displacement and velocity plots. The raw photometric data for the ring was not reduced but is available from EPRI.

Figure D2-1. Camera Plan for Turbine Missile Casing Test

Figure D2-2. Ring and Shell Marker Layout (1 in = 2.54 cm)

Estimates of Displacement and Velocity Error on Turbine Piercing and Blunt Tests

Optical trajectory data from a fixed single camera is obtained by measuring the distance the object is displaced on the x-axis and the y-axis from some fixed reference on the camera's field of view. The x and y displacements are measured in film reader machine counts and are converted to distance by using a calibration of "known" machine counts per unit of distance. This calibration is obtained by measuring some known distance that lies on the same plane as the trajectory.

There are several factors that affect the accuracy of the trajectory data:

1. The film image quality introduces error if the image is blurred (fuzzy) or distorted. This introduces error both in the calibration measurement and in the measurement of the object's displacement.
2. An assumption is made that the object trajectory is on the same level plane throughout the test because parallax errors cannot be easily corrected.
3. On high speed rotating prism cameras, the image is distorted as it approaches the top or bottom of the film frame. No correction is made for this effect.
4. The measuring error of the film reading machine is approximately 10 machine counts on films with a good image. This error increases as the image quality deteriorates. For each data point the machine is zeroed at a fixed reference point and measured to the object. Machine error is introduced at the fixed reference point and at the object being measured. Machine error is also introduced into the displacement calibration measurement, but this error is minimized by taking several calibration measurements and using the average.

The percentage of displacement measuring error depends on the size of the image on the film. The fewer the number of machine counts representing a certain distance on the film, the greater the percentage of displacement error.

The velocity was initially calculated using the three point velocity method:

$$V(t_i) = \frac{S(t_{i-1}) - S(t_{i+1})}{\Delta t}$$

where:

- $V(t_i)$ = velocity at time t_i
- $S(t_{i-1})$ = displacement at time t_{i-1}
- $S(t_{i+1})$ = displacement at time t_{i+1}
- $\Delta t = t_{i-1} - t_{i+1}$ = time increment over three data points

The potential sources of error in the estimation of $V(t_i)$ are in the measurement of the displacements $S(t_{i-1})$ and $S(t_{i+1})$ and the time increment Δt . There are several potential sources of error in the measurement of Δt . There may be an error ($\epsilon_{\Delta t}$) to a changing frame rate of the high-speed camera during the critical part of the test. This is normally neglected since it is usually small and cannot be corrected easily. Timing error can also be introduced by a timing generator that is not properly adjusted to generate the correct cycle, but in most cases this error is considered negligible. If the generator is adjusted properly, the error could be as little as one part in 10^8 . Thus, it can be assumed that the error in the time increment is very small as compared to the potential reading errors and may be neglected. The three point velocity method proved unsatisfactory because of its high sensitivity to displacement errors.

A second reduction of the data was performed using a numerical method. In all cases the data timing was chosen so that impact occurred at $t = 0$. Due to the discreteness of the data, impact at the time t_i was chosen closest to $t = 0$. Examination of the data indicated three typical regions:

1. At $t < 0$ the unit was in free flight with virtually no deceleration component.
2. At impact and for some time after, the unit experienced a severe deceleration.
3. Finally, the unit returned to free flight, but typically with little deceleration.

The velocity profiles were obtained by fitting the position data with a constrained, least-squares, C2, cubic spline. The whole region t_1, t_N was divided by a set of points called knots. Let $T_k, k = 1, \dots, K$ be the knots, with $T_{k+1} > T_k$ and $4 \leq K < N$. The end points were selected as follows, $T_1 = t_1$ and $T_K = t_N$. A cubic spline was used to fit the data with functions that are piecewise cubic polynomials joined at the knots. The C2 property means that the cubics are continuous up to and including the second derivative (acceleration) at the knots. Because the unit was always in free flight or impacting, it was never accelerating. Thus the spline was constrained so that its second derivative was nonpositive at the knots (nonpositivity everywhere). The spline was forced to satisfy these constraints and the continuity condition while fitting the position data in the least-squares sense.

Because of the smooth flight before impact, a knot was placed at impact time. Define t_c to be the value of t_i associated with impact, then

$$T_2 = t_c.$$

Thus the first region is T_1, T_2 . Because of the discontinuity in deceleration at impact, the restriction that the spline second derivative be continuous at $t = T_2$ may be relaxed.

Division of the second and third regions was arbitrary. A time $t_c < T_D < t_N$ and two integers m and n ($m, n \geq 0$) was selected. The knots in Region 2 are

$$T_k = T_2 + (k - 2) (T_D - T_2) / (m + 1) ;$$

$$k = 3, \dots, 3 + m .$$

Note that $T_{3+m} = T_D$. The knots in Region 3 are

$$T_k = T_D + (k - m - 3) (T_K - T_D) / (n + 1) ;$$

$$k = m + 4, \dots, m + 4 + n .$$

Thus $K = m + 4 + n$. In general, the more knots, the less smooth the result.

Displacement and Velocity Errors

The magnitude of the reading or displacement errors were derived by reading the trajectory of the missile five independent times and taking the average of ten sets of differences between these five trajectory readings. The absolute values of the differences were used to obtain the absolute reading errors. The average displacement error in machine counts was converted to average displacement error in the units specified by dividing by the displacement calibration. The displacement calibration was the average of ten reading measurements of some known distance on the missile film image. The velocity error for the missile was estimated from the least squares analysis.

Piercing Test

Table D2-1 presents results of the photometric displacement error analysis for the piercing test. The error estimates are specified for only two intervals since the missile was partially obscured during impact. Data from two cameras which represented different imaging techniques were compared. A Milliken, which is a pin-registered camera with a framing rate of 398 frames/s, was selected for the slower speed analysis, and a Fastax, which is a rotating prism camera with a framing rate of 979 frames/s, was selected for the higher speed analysis.

Figures D2-3, D2-4, and D2-5 present line-of-flight displacement/time and velocity/time (using constrained cubic spline) graphs of the motion of the center of gravity (CG) of the missile.

Table D2-1

PHOTOMETRIC ERROR ANALYSIS OF MISSILE IN PIERCING TEST

<u>Camera</u>	Framing Rate (frames/s)	Time Interval* (ms)	Average Transverse Displacement Error (ft)	Average Line-of-Flight Displacement Error (ft)
			<u> </u>	<u> </u>
Milliken	398	-26.2 to 13.8	<u>+0.023</u>	<u>+0.021</u>
Milliken	398	31.3 to 51.3	<u>+0.030</u>	<u>+0.019</u>
Fastax-2	979	-16.3 to 14.3	<u>+0.020</u>	<u>+0.021</u>
Fastax-2	979	30.7 to 45.0	<u>+0.021</u>	<u>+0.021</u>

* Missile partially obscured during impact.

1 ft = 0.3048 m

The velocity error was estimated using the least squares analysis to be +4.6 m/s (+15 ft/s).

Figure D2-3. Line-of-Flight Displacement vs. Time for CG of Missile in Piercing Test, Milliken Camera

Figure D2-4. Line-of-Flight Displacement vs. Time for CG of Missile in Piercing Test, Fastax Camera

Figure D2-5. Line-of-Flight Velocity vs. Time (using constrained cubic spline) for CG of Missile in Piercing Test, Hycam Camera

Blunt Test

Table D2-2 presents the results of the photometric error analysis for the blunt test. It includes a tabulation of average estimated errors for transverse and line-of-flight data. Data from one Hycam rotating-prism camera operating at a speed of 2917 frames/s were selected for the error analysis because the images from this camera were the clearest.

Figures D2-6, D2-7, and D2-8 present line-of-flight displacement/time and velocity/time (using the three point velocity method and constrained cubic splines) graphs of the motion of the center of gravity (CG) of the missile.

Table D2-2

PHOTOMETRIC DISPLACEMENT ERROR ANALYSIS OF MISSILE IN BLUNT TEST

<u>Camera</u>	<u>Framing Rate</u> (frames/s)	<u>Average Transverse Displacement Error</u> (ft)	<u>Average Line-of-Flight Displacement Error</u> (ft)
Hycam	2917	<u>+0.009</u>	<u>+0.007</u>

1 ft = 0.3048 m

The velocity error was estimated using the least squares analysis to be +4.6 m/s (+15 ft/s).

Figure D2-6. Line-of-Flight Displacement vs. Time for CG of Missile in Blunt Test, Hycam Camera

Figure D2-7. Line-of-Flight Velocity vs. Time (using three point velocity method) for CG of Missile in Blunt Test, Hycam Camera

Figure D2-8. Line-of-Flight Velocity vs. Time (using constrained cubic splines) for CG of Missile in Blunt Test, Hycam Camera

D-3 STRAIN GAGE DATA

Strain gages were used to record structural response near the supports of the ring and shell and near the impact point of the ring.

The strain gages were mounted on the ring and shell structure as shown in Figure D3-1 for the piercing test and in Figure D3-2 for the blunt test. Strain gages were placed on the outer and inner surfaces of the ring to measure bending and at the top and bottom surfaces to measure axial loads (see Section A-A, Figure D3-1 and D3-2). Gages near the ring and shell supports were positioned 45.7 cm (18 in) from the base plates. Gages were placed similarly near the impact area to measure structural response. Although a goal of the program was to measure large strains produced by inelastic deformation, the unavailability of gages in time for the test schedule led to only partial fulfillment of the goal. Tables D3-1 and D3-2 list the strain gages used in each test. In the piercing test, moderately-high-elongation bonded gages (1.5%) were installed on the ring near the impact region and on the shell near the supports. Other gages were welded and had lower-elongation (0.5%). In the blunt test, all gages were the low-elongation welded type. These are foil gages mounted on a thin sheet metal tab, which is spot welded to the test structure. In compression, these gages can measure strains confidently only up to 0.5% before becoming susceptible to nonlinearities from buckling. In tension, the gages are calibrated for accurate measurement up to 0.5%, but should be able to read higher strains with acceptable accuracy. To determine the accuracy of the gages beyond their specified tensile range of 0.5%, special tensile tests were performed. These tests, described below, demonstrated that the gages in the blunt test measure strains up to 1.5% to within engineering accuracy ($\pm 6.7\%$). The measured strains for both tests are presented uninterpreted in data plots D3P-1 through D3P-25 for the piercing test and D3B-1 through D3B-26 for the blunt test; the locations are identified by referring to Figure D3-1 or D3-2.

Calibration ranges for the strain gages for the piercing test were estimated from the results of the pretest analyses. Calibration ranges for the blunt test were estimated from the results of the first test and the pretest analyses.

ALL STRAIN GAGES LOCATED
ON CENTERLINE UNLESS
OTHERWISE NOTED.

Figure D3-1. Strain Gage Locations for the Piercing Test
(All dimensions in cm)

ALL STRAIN GAGES LOCATED
ON CENTERLINE UNLESS
OTHERWISE NOTED.

Figure D3-2. Strain Gage Locations for the Blunt Test
(All dimensions in cm)

Table D3-1

STRAIN GAGES IN PIERCING IMPACT TEST

<u>Gage Type</u>	Rated Gage Capacity <u>Micro Strain</u>	<u>Positions</u>
Micromasurement (Bondable) WK-06-250AF-350 Option W	<u>+15,000</u>	3, 7, 8, 14, 15, 16, 17, 18, 20, 21, 24, 25
Micromasurement (Weldable) CEA-06-W250A-120	<u>+ 5,000</u>	1, 2, 4, 5, 6, 9, 10, 11, 12, 13, 19, 22, 23, 26, 27

Table D3-2

STRAIN GAGES IN BLUNT IMPACT TEST

<u>Gage Type</u>	Rated Gage Capacity <u>Micro Strain</u>	<u>Positions</u>
HiTec Corporation (weldable) HBW-35-250-6-10	<u>+5,000</u>	1-27
Extended Calibration	18,000 tension	

Extended Strain Calibration of Weldable Strain Gages

Tests were performed to determine the validity of tension strains greater than 5000 microstrain on Hitec weldable strain gages. The reference and test strain gages were mounted on an A515 Grade 65 steel tensile test specimen shown in Figure D3-3. One Hitec type HBW-35-125-6-12EP test strain gage was spotwelded to each of the two 0.95-cm- (0.375-in-) wide faces of the center section of the tensile specimen. One BLH type FAB-12-12S6 reference strain gage was bonded to each of the two 0.64-cm- (0.250-in-) wide sides of the center section of the tensile specimen. Epon 907 adhesive was used as the adhesive for the bonded gages. An extensometer with a 2.54-cm (1-in) gage length was mounted on the 0.64 cm- (0.250-in-) wide sides, straddling the bonded gages. The extensometer failed early during testing and was not used. The tensile specimen loading was made in various increments until gage failure was indicated on all gages.

Table D3-3 shows the load and strain data acquired during the test. Examination of the test data indicated that the weldable and bonded gage readings differed by no more than 6.7% up to 15000 microstrain. One Hitec gage remained operational up to a strain level of 18746 microstrain while the other gage remained operational to a strain level of 21854 microstrain. Both gages failed at spot welds at the ends of the gage. From these tests it may be concluded the Hitec gages give engineering accuracy up to 1.5% strain.

Figure D3-3. Tensile Test Specimen (All Dimensions in cm)

Table D3-3
LOAD AND STRAIN

<u>Load N</u>	<u>Hitec No. 1 Strain</u>	<u>Hitec No. 2 Strain</u>	<u>Bonded No. 1 Strain</u>	<u>Bonded No. 2 Strain</u>
0	0	0	0	0
4000	309	287	307	309
8000	617	590	626	628
12000	928	900	953	948
16000	1251	1223	1294	1278
18000	1429	1399	1483	1459
19000	1532	1498	1595	1561
20000	1681	1624	1751	1705
20500	6730	6420	6436	7422
21500	11007	10734	10200	11498
22000	13039	12914	12434	13792
22700	14849	14885	14490	15913
23300	16984	17161	17065	18630
23800	18772	18746	19450	21305
24200	20524	18979	21765	23433
24500	21854	18860	24007	25940
24700	19196	17419		

Error Analysis of Strain Gage Data

The analysis is based on the procedure given in Reference 1.

In brief terms, if a parameter P is a function of variables $v_1, v_2, \dots, v_n$ each with associated errors $\pm v_1, \pm v_2, \dots, \pm v_n$:

$$P \pm E_p = f(v_1 \pm v_1, v_2 \pm v_2, \dots, v_n \pm v_n) ,$$

then the error in P , namely E_p , is:

$$E_p = \left| \Delta v_1 \frac{f}{\partial v_1} \right| + \left| \Delta v_2 \frac{f}{\partial v_2} \right| + \dots + \left| \Delta v_n \frac{f}{\partial v_n} \right| ,$$

where the partial derivatives are evaluated at the nominal values of the factors.

The equation relating strain to voltage output of a single-active arm Wheatstone bridge circuit is:

$$\epsilon = \frac{4e_0}{F(E - 2e_0)}$$

where:

ϵ is the value of strain in in/in.

e_0 is the output voltage of the Wheatstone bridge in volts.

F is the gage factor of the strain gage.

E is the Wheatstone bridge excitation voltage in volts.

The circuit diagram of the actual test setup is shown below.

where:

R_L is the lead wire resistance between the trailer and the bridge completion unit.

R_l is the lead wire resistance between the bridge completion unit and the strain gage.

R_D is the bridge completion resistance.

R_G is the active strain gage.

The gage factor of the strain gage supplied by the manufacturer is modified by the presence of lead wire resistance in series with the strain gage. The effective gage factor is:

$$F = F_m \frac{R_G}{R_G + R},$$

where:

F is the effective gage factor.

F_m is the gage factor supplied by the manufacturer.

R_G is the strain gage resistance.

R_l is the lead wire resistance between the bridge completion unit and the strain gage.

Substituting Eq. (2) into Eq. (1), Eq. (1) becomes:

$$\epsilon = \frac{4e_o}{\frac{F_m R_G (E - 2e_o)}{R_G + R_L}} = \frac{4e_o (R_G + R_L)}{F_m R_G (E - 2e_o)} ,$$

$$\frac{\partial \epsilon}{\partial e_o} = \frac{4(R_G + R_L)(E - 2e_o)F_m R_G - 4e_o(R_G + R_L)(-2F_m R_G)}{[F_m R_G (E - 2e_o)]^2}$$

$$= \frac{4EF_m R_G (R_G + R_L)}{[F_m R_G (E - 2e_o)]^2} .$$

$$\frac{\partial \epsilon}{\partial R_G} = \frac{F_m R_G (E - 2e_o)4e_o - 4e_o(R_G + R_L)F_m (E - 2e_o)}{[F_m R_G (E - 2e_o)]^2}$$

$$= \frac{4e_o F_m R_L (E - 2e_o)}{[F_m R_G (E - 2e_o)]^2}$$

$$\frac{\partial \epsilon}{\partial R_L} = \frac{4e_o}{F_m R_G (E - 2e_o)} .$$

$$\frac{\partial \epsilon}{\partial F_m} = - \frac{4e_o (R_G + R_L) R_G (E - 2e_o)}{[F_m R_G (E - 2e_o)]^2} .$$

$$\frac{\partial \epsilon}{\partial E} = - \frac{4e_o (R_G + R_L) F_m R_G}{[F_m R_G (E - 2e_o)]^2} .$$

Table D3-4 lists the results of evaluating those partial differentials at the nominal values.

Table D3-4
RANDOM ERRORS

	TEST 1		TEST 2
	Gages 3,7,8,14,15,16,17, 18,20,21,24,25	Gages 1,2,4,5,6,9,10,11, 12,13,22,23,26,27	
R_G	$350 \pm 0.4 \%$	$120 \pm 0.4 \%$	$350 \pm 0.4 \%$
R_L	$0.69 \pm 10 \%$	$0.69 \pm 10 \%$	$1.2565 \pm 10 \%$
F_m	$2.01 \pm 1 \%$ + 0 - 0.14 % } misalignment	$2.04 \pm 1 \%$ + 0 - 0.14 % } misalignment	$2.04 \pm 2 \%$ + 0 - 0.14 % } misalignment
e_o	0.023124 ± (0.05 % of reading + 0.02 % of 0.1 V)	0.021756 ± (0.05 % of reading + 0.02 % of 0.1 V)	0.023472 ± (0.05 % of reading + 0.02 % of 0.1 V)
E	$12 \pm (0.1 \%$ of reading + 0.1 % of 19.999)		
$\partial c / \partial R_G$	.0000000217	.000000171	.0000000395
$\partial c / \partial R_L$	.00001099	.000029723	.000011011
$\partial c / \partial t_m$	-.0019190307	-.001759	-.0018942
$\partial c / \partial e_o$	.16745	.16554	.20693
$\partial c / \partial E$	-.0003226813	-.00030012	-.0003233
ΔR_G	1.4	0.48	1.4
ΔR_L	0.069	0.069	0.12565
ΔF_m	.022914	.023256	.043656
Δe_o	.000031562	.000030878	.000031736
ΔE	.031999	.031999	.031999
Δc	.0000603719	.000057554	.0001010124
% error in c	1.51 %	1.44 %	2.52 %

Piercing Test

<u>Figure</u>	<u>Strain Gage</u>
D3P-1	SG1
D3P-2	SG2
D3P-3	SG3
D3P-4	SG4
D3P-5	SG5
D3P-6	SG6
D3P-7	SG7
D3P-8	SG8
D3P-9	SG9
D3P-10	SG10
D3P-11	SG11
D3P-12	SG12
D3P-13	SG13
D3P-14	SG15
D3P-15	SG16
D3P-16	SG17
D3P-17	SG18
D3P-18	SG20
D3P-19	SG21
D3P-20	SG22
D3P-21	SG23
D3P-22	SG24
D3P-23	SG25
D3P-24	SG26
D3P-25	SG27

Figure D3P-1. Strain Gage Data SG-1

Figure D3P-2. Strain Gage Data SG-2

PIERCING TEST

Note: Negative strain is tension

Figure D3P-3. Strain Gage Data SG-3

Figure D3P-4. Strain Gage Data SG-4

PIERCING TEST

Note: Negative strain is tension

Figure D3P-5. Strain Gage Data SG-5

Figure D3P-6. Strain Gage Data SG-6

PIERCING TEST

Note: Negative strain is tension

Figure D3P-7. Strain Gage Data SG-7

Figure D3P-8. Strain Gage Data SG-8

PIERCING TEST

Note: Negative strain is tension

Figure D3P-9. Strain Gage Data SG-9

Figure D3P-10. Strain Gage Data SG-10

PIERCING TEST

Note: Negative strain is tension

Figure D3P-11. Strain Gage Data SG-11

Figure D3P-12. Strain Gage Data SG-12

PIERCING TEST

Note: Negative strain is tension

Figure D3P-13. Strain Gage Data SG-13

Figure D3P-14. Strain Gage Data SG-15

PIERCING TEST

Note: Negative strain is tension

Figure D3P-15. Strain Gage Data SG-16

Figure D3P-16. Strain Gage Data SG-17

PIERCING TEST

Note: Negative strain is tension

Figure D3P-17. Strain Gage Data SG-18

Figure D3P-18. Strain Gage Data SG-20

PIERCING TEST

Note: Negative strain is tension

Figure D3P-19. Strain Gage Data SG-21

Figure D3P-20. Strain Gage Data SG-22

PIERCING TEST

Note: Negative strain is tension

Figure D3P-21. Strain Gage Data SG-23

Figure D3P-22. Strain Gage Data SG-24

PIERCING TEST

Note: Negative strain is tension

Figure D3P-23. Strain Gage Data SG-25

Figure D3P-24. Strain Gage Data SG-26

PIERCING TEST

Note: Negative strain is tension

Figure D3P-25. Strain Gage Data SG-27

PIERCING TEST.

Note: Negative strain is tension

Blunt Test

<u>Figure</u>	<u>Strain Gage</u>
D3B-1	SG-1
D3B-2	SG-2
D3B-3	SG-3
D3B-4	SG-4
D3B-5	SG-5
D3B-6	SG-6
D3B-7	SG-7
D3B-8	SG-8
D3B-9	SG-9
D3B-10	SG-10
D3B-11	SG-11
D3B-12	SG-12
D3B-13	SG-13
D3B-14	SG-14
D3B-15	SG-15
D3B-16	SG-16
D3B-17	SG-17
D3B-18	SG-18
D3B-19	SG-19
D3B-20	SG-20
D3B-21	SG-21
D3B-22	SG-22
D3B-23	SG-23
D3B-24	SG-24
D3B-25	SG-26
D3B-26	SG-27

Figure D3B-1. Strain Gage Data SG-1

Figure D3B-2. Strain Gage Data SG-2

BLUNT TEST

Note: Negative strain is tension

Figure D3B-3. Strain Gage Data SG-3.

Figure D3B-4. Strain Gage Data SG-4.

BLUNT TEST

Note: Negative strain is tension

Figure D3B 5. Strain Gage Data SG-5.

Figure D3B-6. Strain Gage Data SG-6

BLUNT TEST

Note: Negative strain is tension

Figure D3B-7. Strain Gage Data SG-7.

Figure D3B-8. Strain Gage Data SG-8

BLUNT TEST

Note: Negative strain is tension

Figure D3B-9. Strain Gage Data SG-9

Figure D3B-10. Strain Gage Data SG-10

BLUNT TEST

Note: Negative strain is tension

Figure D3B-11. Strain Gage Data SG-11

Figure D3B-12. Strain Gage Data SG-12

BLUNT TEST

Note: Negative strain is tension

Figure D3B-13. Strain Gage Data SG-13

Figure D3B-14. Strain Gage Data SG-14

BLUNT TEST

Note: Negative strain is tension

Figure D3B-15. Strain Gage Data SG-15

Figure D3B-16. Strain Gage Data SG-16

BLUNT TEST

Note: Negative strain is tension

Figure D3B-17. Strain Gage Data SG-17

Figure D3B-18. Strain Gage Data SG-18

BLUNT TEST

Note: Negative strain is tension

Figure D3B-19. Strain Gage Data SG-19

Figure D3B-20. Strain Gage Data SG-20

BLUNT TEST

Note: Negative strain is tension

Figure D3B-21. Strain Gage Data SG-21

Figure D3B-22. Strain Gage Data SG-22

BLUNT TEST

Note: Negative strain is tension

Figure D3B-23. Strain Gage Data SG-23

Figure D3B-24. Strain Gage Data SG-24

BLUNT TEST

Note: Negative strain is tension

Figure D3B-25. Strain Gage Data SG-26

Figure D3B-26. Strain Gage Data SG-27

BLUNT TEST

Note: Negative strain is tension

D-4 DEFORMATION MEASUREMENTS

Measurements of the deformed rings of the piercing and blunt tests are described in this section. Other measurements included shell deformation of the piercing impact. Photographs of the deformed missile after testing are also presented in this section.

Piercing Impact Test

Deformation measurements for the two broken ring segments are presented in Tables D4-1 and D4-2. The reference axis for the measurements was defined as the line connecting the midpoint of the broken ends of segment as illustrated in the diagrams. Estimates of ring deformation were provided by measurements perpendicular to the reference axis.

The segment orientations are as follows. The left segment is defined as the ring piece located to the left of the missile impact point and the right segment is the ring piece located to the right of the missile impact point as shown in Figure D4-1. Table D4-1 presents the deformation measurements for the left half of the ring and Table D4-2 for the right half.

Local ring deformation measurements near the impact point for the piercing test are presented in Table D4-3. The reference axes for the measurements are defined in the diagrams.

Measurements for the shell are presented in Table D4-4. The reference axis for the deformation measurements was defined as the line joining the two opposite attachment pads as shown in the diagram.

Table D4-1

RING DEFORMATION MEASUREMENTS FOR PIERCING TEST
(Left Side)

<u>Station No.</u> (ft)	<u>Displacement</u> (in)	<u>Station No.</u> (ft)	<u>Displacement</u> (in)
0.0	0.0	6.6	5.94
0.5	2.88	7.0	4.81
1.0	5.19	7.5	3.94
1.5	7.06	8.0	3.19
2.0	8.42	8.5	2.62
2.5	9.42	9.0	2.12
3.0	10.06	9.6	1.06
3.5	10.19	10.0	1.25
4.0	9.94	10.5	0.94
4.5	9.38	11.0	0.81
5.0	8.50	11.5	0.69
5.5	7.56	12.0	0.25
6.00	6.62	12.14	0.0

1 in = 2.54 cm

Table D4-2

RING DEFORMATION MEASUREMENTS FOR PIERCING TEST
(Right Side)

<u>Station No.</u> (ft)	<u>Displacement</u> (in)	<u>Station No.</u> (ft)	<u>Displacement</u> (in)
0.0	0.0	6.0	-3.69
0.5	1.25	6.5	-2.88
1.0	1.88	7.0	-2.75
1.5	2.06	7.5	-0.31
2.0	1.88	7.6	0.0
2.5	1.25	8.0	1.13
3.0	0.25	8.5	2.13
3.1	0.0	8.8	2.19
3.5	-1.06	9.0	1.88
4.0	-2.38	9.6	0.0
4.5	-3.44		
5.0	-4.00		
5.3	-4.06		
5.5	-4.00		

1 in = 2.54 cm

Figure D4-1. Shell and Ring Segment Orientations After Piercing Impact.

Table D4-3

RING DISTORTION MEASUREMENTS NEAR IMPACT POINT FOR PIERCING TEST

Ring Height	Location Left of Missile C.G.			Missile C.G.	Location Right of Missile C.G.		
	10.38 (in)	6.38 (in)	4 (in)		4 (in)	8 (in)	12 (in)
Interior Displacement Measurements							
0	0.0	0.0	0.0	0.0	0.0	0.0	0.0
2	.04	.09	.13	.21	.21	.13	.09
4	.08	.18	.26	.42	.42	.26	.18
6	0.13	0.44	0.69	0.94	0.94	0.56	0.31
8	0.19	-	3.13	1.75	1.63	1.06	0.38
10	0.19	-	5.44	3.25	2.38	1.50	0.44
12	0.19	-	4.13	1.75	1.63	1.31	0.38
14	0.13	0.38	0.50	0.94	0.88	0.63	0.19
16	-	0.19	0.19	0.44	0.44	0.31	0.13
18	0.0	0.13	0.06	0.13	0.13	0.06	0.06
20	0.0	0.0	0.0	0.0	0.0	0.0	0.0
Exterior Displacement Measurements							
0	0.0	0.0	0.0	0.0	0.0	0.0	0.0
2	.09	.09	.17	.21	.25	.12	.12
4	.18	.18	.34	.42	.50	.24	.24
6	0.25	0.38	0.69	0.94	1.00	0.56	0.37
8	0.32	0.63	1.13	1.63	1.44	0.87	0.50
10	0.38	0.88	1.38	1.88	1.63	1.06	0.56
12	0.32	0.75	1.25	1.75	1.50	1.00	0.50
14	0.32	0.44	0.88	1.25	1.00	0.68	0.44
16	0.25	0.25	0.57	0.69	0.44	0.37	0.31
18	0.13	0.19	0.44	0.25	0.38	0.18	0.17
20	0.0	0.0	0.0	0.0	0.0	1.00	0.0

1 in = 2.54 cm

Table D4-4

SHELL DEFORMATION MEASUREMENT FOR THE PIERCING TEST

Station No. (ft)	Displacement (in)	Station No. (ft)	Displacement (in)
0	0.0	10	140.0
2	67.0	12	140.5
4	94.0	14	135.0
6	108.5	16	118.0
8	129.0	18	79.0
		19.8	0.0

1 in = 2.54 cm

Figures D4-2, D4-3, D4-4, and D4-5 show the deformation of the impacted end of the turbine missile after the piercing test. The end of the rotor deformed approximately 2.54 cm (1 in). Approximately 0.3 m (1 ft) of flange was removed from the rotor. Figure D4-6 shows the unimpacted end of the rotor for comparison.

(Text continued on Page D-63)

Figure D4-2. End View of Impacted Surface of Turbine Rotor After Piercing Test

Figure D4-3. Top View of Impacted Surface of Turbine Rotor After Piercing Test

Figure D4-4. Side View of Impacted Surface (Bottom of Rotor in Test) of Turbine Rotor After Piercing Test

Figure D4-5. Side View of Impacted Surface (Top of Rotor in Test) of Turbine Rotor After Piercing Test

Figure D4-6. End view of Unimpacted Surface of Turbine Rotor After Piercing Test

Blunt Impact Test

Deformation measurements for the ring segment in the blunt impact test are presented in Table D4-5. The reference axis for the measurements was defined as the line connecting the midpoint of the attachment pads as shown in the diagram. The left pad was positioned at an angle of 47° to the reference and the right pad at an angle of 13° to the reference after the test. A photograph of the deformed ring is shown in Figure D4-7.

Local ring deformation measurements near the impacted surface of the ring for the blunt test are presented in Table D4-6. Figure D4-8 shows the condition of the impacted surface of the ring. The reference axes for the measurements are defined in the diagrams.

Table D4-5

RING DEFORMATION MEASUREMENTS FOR THE BLUNT TEST

<u>Station No.</u> (ft)	<u>Displacement</u> (in)
0	0.0
1	9.68
2	16.54
3	22.99
4	33.64
5	56.76
6	97.29
7	108.30
8	110.48
9	107.81
11	87.01
11.2	0.0

1 in = 2.54 cm

Figure D4-7. Deformed Ring and Shell After Blunt Impact Test

Table D4-6

RING DISTORTION MEASUREMENTS NEAR IMPACT POINT FOR BLUNT TEST

Ring Height (in)	Location Left of Missile C.G.						Missile C.G.	Location Right of Missile C.G.					
	36 (in)	30 (in)	24 (in)	18 (in)	12 (in)	6 (in)	0 (in)	6 (in)	12 (in)	18 (in)	24 (in)	30 (in)	
Interior Displacement Measurements													
0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0
2	.02	.04	.04	.09	.13	.17	.21	.17	.25	.21	.17	.04	
4	.04	.09	.08	.10	.26	.34	.42	.34	.50	.42	.34	.08	
6	0.06	0.13	0.31	0.31	0.56	0.69	0.81	0.94	0.94	0.81	0.63	0.31	
8	0.13	0.25	0.56	0.81	1.00	1.25	1.44	1.56	1.50	1.38	1.06	0.58	
10	0.25	0.38	0.56	0.88	1.13	1.38	1.56	>1.56	1.63	1.50	1.25	0.63	
12	0.19	0.31	0.50	0.75	1.00	1.25	1.38	1.56	1.44	1.38	1.06	0.50	
14	0.13	0.13	0.25	0.38	0.50	0.63	0.75	0.94	0.88	0.19	0.63	0.25	
16	0.0	0.06	0.06	0.19	0.25	0.25	0.38	0.50	0.50	0.44	0.31	0.13	
18	0.0	.03	.03	.10	.13	.13	.13	.19	.25	.22	.16	.07	
20	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	
Exterior Displacement Measurements													
0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0
2	-.05	0.0	.05	.04	.17	.17	.25	.25	.29	.21	.17	.05	
4	.06	.04	.10	.08	.34	.34	.50	.58	.58	.42	.34	.10	
6	-0.07	0.06	0.25	0.24	0.57	0.63	0.88	0.94	0.94	0.75	0.62	0.19	
8	0.0	0.13	0.32	0.50	0.75	1.00	1.25	1.31	1.25	1.06	0.87	0.32	
10	0.6	0.19	0.38	0.56	0.87	1.13	1.38	1.44	1.44	1.19	1.00	0.38	
12	0.6	0.13	0.32	0.50	0.82	1.05	1.32	1.38	1.38	1.13	0.94	0.32	
14	0.0	0.13	0.19	0.24	0.57	0.75	1.00	1.13	1.19	0.94	0.69	0.19	
16	-0.07	0.03	0.14	0.18	0.32	0.44	0.53	0.75	0.81	0.50	0.44	0.13	
18	-0.07	0.0	0.0	0.0	0.13	0.13	0.32	0.38	0.44	0.19	0.19	0.0	
20	0.06	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	

1 in = 2.54 cm

Figure D4-8. Impacted Surface of Ring After Blunt Impact Test

REFERENCES

1. E. O. Doebelin, Measurement Systems: Application and Design, 2nd Edition, New York: McGraw, 1971.

EPRI NP-2741

Below are five index cards that allow for filing according to the four cross-references in addition to the title of the report. A brief abstract describing the major subject area covered in the report is included on each card.

EPRI

EPRI NP-2741
RP399-1
Final Report
January 1983

Full-Scale Turbine Missile Casing Tests

Contractor: Sandia National Laboratories

This report presents the results of two full-scale tests simulating the impact of turbine disk fragments on simple ring and shell structures that represent the internal stator blade ring and the outer housing of an 1800-rpm steam turbine casing. Structural and finite element analysis and data interpretation, estimates of energy during impact, missile displacement and velocity histories, and selected strain gage data are included. 196 pp.

EPRI Project Manager: G. E. Sliter

Cross-References:

1. EPRI NP-2741 2. RP399-1 3. Risk Assessment Program 4. Turbine Missile

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RP399-1
Final Report
January 1983

Full-Scale Turbine Missile Casing Tests

Contractor: Sandia National Laboratories

This report presents the results of two full-scale tests simulating the impact of turbine disk fragments on simple ring and shell structures that represent the internal stator blade ring and the outer housing of an 1800-rpm steam turbine casing. Structural and finite element analysis and data interpretation, estimates of energy during impact, missile displacement and velocity histories, and selected strain gage data are included. 196 pp.

EPRI Project Manager: G. E. Sliter

Cross-References:

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