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EXPERIMENTAL DETERMINATION OF THE
LOSS COEFFICIENT IN AN ANNULAR DIFFUSER

Tony A. Angelelli

MASTER

OAK RIDGE GASEOUS DIFFUSION PLANT
OAK RIDGE, TENNESSEE

prepared for the U.S. ATOMIC ENERGY COMMISSION
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EXPERIMENTAL DETERMINATION OF THE
LOSS COEFFICIENT IN AN ANNULAR DIFFUSER

A Thesis

Presented for the

Master of Science

Degree

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ABSTRACT

There is a need for the experimental determination of the total pressure loss coefficient in an annular diffuser. Consequently, this investigation was undertaken to determine the relationship between the loss coefficient and other annular diffuser performance parameters, such as inlet boundary layer blockage, nondimensional length, and inlet Mach and Reynolds numbers. Also, the static pressure recovery as it relates to these parameters was studied.

The annular diffuser configuration tested consisted of a zero angle, circular cylinder outer wall and a tapering cone centerbody. Diffusers with an area ratio of 2.79 and nondimensional lengths of 12 and 15 were tested at blockage levels varying from 4% to 9% over an inlet Mach number range of 0.24 to 0.46 and a Reynolds number variation of 0.28×10^6 to 0.46×10^6 . In addition, the sharp inlet taper of the centerbody for the diffuser with nondimensional length of 12 was rounded to a more gradual taper and tested.

Results indicated that varying the nondimensional length significantly altered the loss coefficient. For the nondimensional length of 12, the loss coefficient was 0.300 at a 4% inlet blockage level and 0.316 at an 8% level; for the nondimensional length of 15, values of 0.214 at a 6% blockage level and 0.226 at a 9% level resulted. Inlet blockage effects remained distinct in the incompressible flow range, but results tended toward convergence in the compressible range where the variation of loss coefficient with both Mach and Reynolds numbers was nonlinear. Also, it was determined that rounding the sharp inlet taper to a more

gradual one produced as much reduction in loss coefficient as adding 25% to the diffuser's axial length. The static pressure recovery coefficient exhibited gains with the extended diffuser and the gradual inlet taper comparable to those of the loss coefficient and varied similarly with inlet blockage and Mach number levels.

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LIST OF SYMBOLS

A	Cross-sectional area
A_B	Blocked area, $A - A_E = \int_A [1 - (u/U)] dA$
A_E	Effective area, $\int_A (u/U) dA$
B	Fraction of area blocked by boundary layer, A_B/A
C_L	Total pressure loss coefficient, $(\overline{P}_{01} - \overline{P}_{02})/\overline{P}_{v1}$
C_p	Specific heat at constant pressure, 0.24 Btu/lbm-°R
C_{PR}	Static pressure recovery coefficient, $(P_2 - P_1)/\overline{P}_{v1}$
D	Axisymmetric diffuser diameter
e	Root-mean-square voltage
E	Hot-wire anemometer voltage
g_c	Gravitational constant, 32.172 lbm-ft/lbf-sec ²
L	Diffuser axial length
$\dot{m}$	Mass flow rate, $\bar{\rho} \bar{u} A$
M	Mach number
P	Static pressure
P_0	Total pressure
P_v	Dynamic pressure, $\bar{\rho} \bar{u}^2/2g_c$
P_w	Wetted perimeter
ΔP_0	Total pressure differential (gauge pressure)
R	Axisymmetric diffuser radius
$\tilde{R}$	Gas constant, 53.34 ft-lbf/lbm-°R
Re	Reynolds number, $\bar{\rho} \bar{u} D/\mu$
ΔR	Annulus height
T	Static temperature

T_0	Total temperature
Tu	Turbulence intensity, u'/u
u	Local velocity
U	Maximum velocity
γ	Ratio of specific heats, $C_p/C_v = 1.4$
θ	Diffuser circumferential angle
μ	Coefficient of viscosity
ρ	Static density
ϕ	Diffuser wall angle
ψ	Error in boundary layer blockage

Subscripts

i	Inner; incompressible
c	Compressible
e	Equivalent
o	Outer
1	At diffuser inlet
2	At diffuser discharge
$\sim \theta$	Circumferential average

Superscripts

$-$	Cross-sectional average value
$'$	Pseudo-value; fluctuating value
$\wedge$	Time-varying value

INTRODUCTION

Although various studies have been undertaken to determine the importance and the interrelationship among performance parameters for several types of annular diffusers, there appear to be at present no general performance correlations. A significant amount of information is available concerning static pressure recovery over limited ranges, but there remains a need for the experimental determination of the total pressure loss coefficient. Since the relationship between the loss coefficient and other diffuser parameters has not been firmly established, and because a loss in total pressure represents an energy loss which requires added power to resupply, this investigation was undertaken to determine for an annular diffuser with a zero angle outer wall and a tapering cone centerbody the relationships between total pressure loss coefficient and other performance parameters, such as inlet boundary layer blockage, nondimensional length, and inlet Mach and Reynolds numbers. Additionally, the static pressure recovery as it relates to these parameters was studied.

A review of the pertinent literature provided a background for the study and indicated the many gaps in the complete picture of annular diffuser performance which exist. Particularly with the type of diffuser of interest in this project, possible reasons for the lack of complete information include the problems of supporting the diffuser centerbody for testing without altering flow patterns, the difficulties of obtaining reliable pressure data in turbulent flows, and the specific and limited uses of this type of diffuser in isolated pieces of machinery. Before this project began, facility restrictions and limited time allotment

narrowed the range of flows and configurations that could be tested, so that the study will include only a small part of the overall picture.

Four types of straight-walled annular diffusers were defined by S. J. Stevens^[14] in his study of diffuser performance and are illustrated in Figure 1. Also defined in Figure 1 is the concept of an "equivalent wall angle (ϕ_e)" which is a function of inner (ϕ_i) and outer (ϕ_o) wall angles and inner (R_i) and outer (R_o) radii. He explained that the initial rate of diffusion for a type 1 diffuser was greater than the average rate due to a larger initial equivalent angle. This accounted for the tendency toward early separation which this type of diffuser exhibited.

Stevens' work was conducted on a type 2 diffuser, and he determined that the inlet boundary layer on the outer wall was of major importance to the performance of the diffuser over the range of inlet velocity profiles studied. He employed gauzes across the pipe cross section to produce artificial boundary layers, allowing a settling length between the gauzes and the inlet sufficient to reduce the velocity profile rate of change to a negligible amount. Figure 2 illustrates axial nondimensional velocity profiles produced by four sets of gauzes.

The suggestion was made by Stevens that although there must be a significant amount of information concerning the performance of annular diffusers used in many existing machines, there is still a need for a detailed study of the effects of inlet conditions on overall diffuser performance.

An important contribution to the understanding of conical, two-dimensional, and annular diffusers has been made by Gino Sovran and Edward D. Klomp^[13]. Their study primarily concerned itself with defining

NOTATION:

 R_e Equivalent Radius, $R_o^2 - R_i^2$
 ϕ_e Equivalent Wall Angle, $(R_o \tan \phi_o - R_i \tan \phi_i) / R_e$
 X/L Nondimensional Length

Figure 1

EQUIVALENT WALL ANGLE ALONG FOUR TYPES OF
STRAIGHT-WALLED ANNULAR DIFFUSERS [14]

Figure 2
INLET VELOCITY PROFILES PRODUCED BY GAUZES [14]

optimum geometries for the design problems of specified area ratio with nondimensional length variable and of specified nondimensional length with area ratio variable. The effects of the inlet boundary layer were also studied.

With reference to straight-walled annular diffusers, their study concluded that the optimum diffuser geometry with respect to pressure recovery was mainly determined by the area ratio and the nondimensional length. For optimum diffuser designs, they concluded that the effects of inlet boundary layer could best be correlated by use of a boundary layer area blockage. It was also determined that an increased inlet boundary layer thickness resulted in a decrease in pressure recovery, primarily because of a reduction in the amount of diffusion possible. This work was conducted on diffusers of types 3 and 4 at an inlet blockage area of 2% of the geometric area.

Sovran and Klomp finally expressed the need for detailed diffuser performance data to serve as a basis for analytical work. They suggested that any testing should consider where the particular test geometry was located with respect to optimum lines.

Two type 3 annular diffuser configurations which conformed with the Sovran and Klomp requirements for an optimum at specified nondimensional length were tested in a study conducted by R. A. Tyler and R. G. Williamson^[17]. Presented in Figure 3 is a plot of the variation of pressure recovery coefficient with inlet blockage that these experimenters developed for both conical and annular diffusers. To be noted is the rise of pressure recovery coefficient with blockage for conical diffusers, while the opposite is true for annular diffusers.

A study reported by John Henry, et al^[5], which presented a summary of subsonic diffuser data for rectangular, annular, and conical diffusers, resulted in some experimental loss coefficient data for a type 2 annular diffuser, as seen in Figure 4. This work was conducted to show the effect of Reynolds number on the friction component in the loss coefficient. As the figure illustrates, the loss coefficient for the lower angle diffuser remained constant or decreased slightly as the inlet Mach number increased, a phenomenon attributed to a decrease in the friction component. The loss coefficient for the higher angle diffuser did exhibit an increase with Mach number, but the experimenters admitted that the rate of increase could be dramatically reduced by refairing the curve within the data scatter.

An earlier study reported by C. C. Wood and J. T. Higgenbotham^[20] was concerned with annular diffusers comprised of constant diameter outer walls and varying centerbodies, including a type 1 diffuser. The total pressure loss coefficient, the static pressure recovery coefficient, and inlet Mach number effects were studied. Difficulty was encountered in obtaining reliable total pressure measurements for some centerbody configurations because of poor velocity distributions and a high degree of turbulence. For the type 1 diffuser, however, the experimenters judged the data as realistic, and it was determined that the loss coefficient increased with decreasing diffuser length, as seen in Figure 5. With regard to static pressure recovery, a rapid decrease was seen with decreased diffuser length, as Figure 6 illustrates, and a negative value was even reached for a sharp-edged, abrupt expansion diffuser. Finally, considering the variation of pressure recovery and loss coefficient

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Figure 4
EFFECTS OF INLET MACH NUMBER ON LOSS COEFFICIENT [5]

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Figure 5
VARIATION OF LOSS COEFFICIENT WITH CENTERBODY LENGTH^[20]

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Figure 6

VARIATION OF PRESSURE RECOVERY COEFFICIENT
WITH DIFFUSER LENGTH [20]

with inlet Mach number, the variation was judged to be small and random, but the range was not sufficient to draw reliable conclusions.

I. EXPLANATION OF TEST PARAMETERS

The dimensionless diffuser performance parameters to be determined and interrelated are the total pressure loss coefficient, the static pressure recovery coefficient, inlet boundary layer blockage, nondimensional length, and inlet Mach and Reynolds numbers.

The loss coefficient (C_L) stated simply is a measure of the available energy loss between any two points in a system. It is the difference between total pressures at two points referenced usually to the dynamic pressure at one of the two points. In this study, the loss coefficient is the difference between the area averaged total pressure measured at the diffuser inlet (1) and the area averaged total pressure measured at the diffuser discharge (2), referenced to the average inlet dynamic pressure (1). Stated mathematically,

$$C_{L(1,2)1} \equiv \frac{\overline{P_{01}} - \overline{P_{02}}}{\overline{P_{v1}}}.$$

However, if the relationship between pressures is assumed to be

$$\overline{P_0} = P + \overline{P_v},$$

where P is static pressure and is constant at a cross section, then

$$\overline{P_v} = \overline{P_0} - P,$$

and

$$C_{L(1,2)1} \equiv \frac{\overline{P_{01}} - \overline{P_{02}}}{\overline{P_{01}} - P_1}.$$

Similar in form to the loss coefficient, the static pressure recovery coefficient (C_{PR}) is a measure of the amount of diffusion and is the difference between the static pressures at any two points referenced to the average dynamic pressure at one of the points. For these tests, therefore,

$$C_{PR(1,2)1} \equiv \frac{P_2 - P_1}{\frac{\rho}{2} v_1^2},$$

or

$$C_{PR(1,2)1} \equiv \frac{P_2 - P_1}{P_{01} - P_1}.$$

Boundary layer blockage (B) refers to the ratio of the blocked area (A_B) to the cross-sectional area (A) of the diffuser inlet and is a function of the cross-sectional velocity profile. The blocked area is the difference between the cross-sectional area and the area (A_E) which would pass the particular volume flow rate of the diffuser inlet if the velocity were uniform and equal in magnitude to the maximum cross-sectional velocity. Therefore, the blocked area is a percentage greater than zero unless there is an entirely uniform cross-sectional velocity profile. In mathematical terms for incompressible flow,

$$B = \frac{A_B}{A} = \frac{A}{A} - \frac{A_E}{A} = \frac{1}{A} \int_A (1 - u/U) dA.$$

When this incompressible relationship was employed in the range of compressibility, no significant error was introduced, as is illustrated by the evaluation in Appendix A.

The nondimensional length parameter is comprised of the ratio of diffuser axial length (L) to inlet annulus height (ΔR) and is employed in conjunction with the area ratio, which is the ratio of diffuser discharge area (A_2) to inlet area (A_1), in prescribing the diffuser configuration.

The Mach number (M), the ratio of the average velocity to the speed of sound, and the Reynolds number (Re), the ratio of the inertia forces to the viscous (friction) forces, are the similarity parameters used as independent variables in determining correlations. In dimensional analysis of incompressible flows, the Reynolds number is the significant parameter, but compressible flow is a function of both Mach and Reynolds numbers.

II. DESCRIPTION OF TEST SETUP

Model Description

The annular diffuser to be tested is of type 1, as defined by S. J. Stevens^[14]; i.e., its outer wall is a circular cylinder and its centerbody is a tapering cone. An overall view of the model is illustrated in Figure 7, while Figure 8 shows model dimensions for the two configurations tested. The diffuser outer wall is made of Plexiglas so that flow visualization may be utilized. The centerbodies are wood, and the concentric inlet cylinders used to establish developed flow are aluminum. An annular diffuser performance chart for an inlet blockage of 2%, developed by Sovran and Klomp^[13], was used to approximate an "optimum" diffuser with regard to static pressure recovery for the area ratio of 2.79 as required to accommodate existing equipment. A diffuser with the suggested optimum length, as well as one with 25% greater length, was constructed. Figure 9 shows the diffuser test section with the "optimum" diffuser configuration, and Figure 10 illustrates the two centerbodies in detail.

Facility and Instrumentation

The model was tested in the air model test facility of the Compressors and Stage Equipment Development Department, Gaseous Diffusion Development Division, Oak Ridge Gaseous Diffusion Plant. The diffuser was set up to a 13-1/8-inch test pipe so that air could be drawn through it by any of the 600- or 700-horsepower exhaust compressors available.

Instrumentation used for gathering test data consisted of a PDP 8/L digital computer-controlled data acquisition system, associated

PHOTO NO. PH-73-949

Figure 7
ANNULAR DIFFUSER MODEL SETUP

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a) $L/AR = 12$ CONFIGURATION b) $L/AR = 15$ CONFIGURATION

Figure 8
ANNULAR DIFFUSER TEST CONFIGURATIONS

PHOTO NO. PH-73-947

Figure 9
ANNULAR DIFFUSER TEST SECTION

DWG. NO. G-74-721

Figure 10
DETAILS OF TEST SECTION AND CONFIGURATIONS TESTED

probing, and a chart recorder. The PDP 8/L operated a double Scanivalve manifold system with a capacity of 64 pressure ports per unit. A differential pressure was received by a designated Scanivalve port and was transmitted to a transducer of appropriate range which converted the pressure signal to an electrical signal. The electrical signal was then fed back into the computer through a multiplexing device, where it was again converted to a pressure reading in psid and was printed out on a Teletype unit, along with its 95% confidence interval. For each pressure printed, the computer had averaged 20 readings, each taken at approximately 200-ms intervals and fed through a manually or automatically selected transducer.

Probing consisted of special boundary layer total pressure probes, wall static taps, Kiel total pressure probes, and hot-wire anemometer probes. The boundary layer probes allowed total pressure measurement within the boundary layer, as well as in the core of the inlet flow to the diffuser, and enabled a good estimate of the blocked area to be made. The Flow Corporation hot-wire anemometer and its associated probing were used to measure the turbulence intensity. All probes were mounted on L. C. Smith actuators to enable continuous scans to be made, and if desired, readings could be plotted on a continuous chart recorder.

Data reduction was performed by the PDP 8/L computer using established aerodynamic evaluation programs.

III. TEST PROCEDURE

General Test Outline

The basic test procedure was first to establish the loss coefficient and the static pressure recovery coefficient for the first design at two levels of inlet blockage. Second, the first centerbody was replaced by the extended centerbody, and testing to establish the loss coefficient and static pressure recovery coefficient for two blockage levels was again performed. At this point, an examination of the parameters revealed that the second configuration demonstrated improved performance over the first; i.e., that the first centerbody chosen was not optimum for the flow conditions and geometric constraints established. As a result, the first centerbody configuration was reworked, in that the sharp corner of the centerbody taper was rounded for a more gradual transition in hopes of reducing separation and improving its performance, and this was retested.

Detailed Procedure

The detailed procedure for determining a loss coefficient for a given configuration and set flow conditions was comprised of (1) establishment of the flow conditions, and (2) total pressure data acquisition and reduction.

Establishment of Flow Conditions. Establishment of the flow conditions included production of the proper amount of blockage, preliminary data acquisition at flow and model stations, and reduction of the data to determine the resulting flow parameters. Inlet boundary layer blockage

was produced and varied by the addition of screening to the inlet piping. The inlet piping, both inner and outer, was removable; but for all testing, approximately 38 inches of piping was provided upstream of the inlet measuring station to allow flow development. The high blockage level was produced by a set of three screens located approximately 30 inches upstream of the inlet measuring station, and the low blockage level resulted from the addition of a second set of three screens 8-1/2 inches upstream of the same station. The inlet measuring station itself was located 1-1/2 inches upstream of the centerbody taper.

The preliminary data acquisition included wall static and in-stream total pressure measurements at the permanent flow measuring station in the test pipe and wall static pressure measurements at the inlet and discharge of the diffuser. The permanent flow station, located in the 13-1/8-inch test pipe, was approximately 12 diameters downstream of the model. Eight wall static pressure readings taken circumferentially 45 degrees apart in conjunction with 20 total pressure readings which represented four circumferential positions and five equal annular areas, as seen in Figure 11, were used to determine a mass flow. Four wall static taps in the plane of the diffuser's inlet measuring station and four static taps in the plane of the discharge measuring station were used to determine the static pressure at these stations. Static pressure was assumed constant at a cross section.

The data reduction was performed by the PDP 8/L computer, utilizing an existing aerodynamic evaluation scheme. Additional inputs to the program were ambient temperature ($^{\circ}\text{C}$), atmospheric pressure (cm Hg), and the specific gravity of mercury at ambient temperature. The program averaged the pressures at each station and, having station geometries

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Figure 11
MASS FLOW MEASURING STATION

already stored in memory, calculated the quantities shown in Appendix B, Table I, for the flow station, the inlet station, and the discharge station in that order. Details of the data reduction program may also be found in Appendix B.

Acquisition of Measured Total Pressure. Total pressure data acquisition was accomplished at the inlet measuring station by four radial traverses, 90 degrees apart circumferentially, taken with an actuator-mounted boundary layer probe. The data was fed through a pressure transducer to a continuous chart recorder, where total gauge pressure was plotted against radial position for the four traverses made at each flow condition. Figure 12 is a superposition of two scans taken from one such set of data, and it illustrates the type of flow pattern that was produced for the first configuration at a low blockage level. Circumferential variations may be detected in the figure, and these variations were the reason for four traverses.

The area averaged gauge total pressure at this station was determined from these traverses by the relationship,

$$\overline{\Delta P_0} = 1/A \int_A \Delta P_0 \, dA.$$

Applied to the measurements made here,

$$\overline{\Delta P_0} = \frac{1}{\pi(R_o^2 - R_i^2)} \int_{R_i}^{R_o} \left(\int_0^{2\pi} \Delta P_0 \, d\theta \right) R \, dR,$$

Figure 12
INLET TOTAL PRESSURE TRAVERSES

where

$$\int_0^{2\pi} \Delta P_0 d\theta = 2\pi \sum_{i=1}^4 \frac{\Delta P_0 \text{ in increments of } \pi/2}{4} = 2\pi \left[(\Delta P_0)_{\tilde{\theta}} \right].$$

Substituting into the above equation,

$$\overline{\Delta P_0} = \frac{2\pi}{\pi(R_o^2 - R_i^2)} \int_{R_i}^{R_o} \left[(\Delta P_0)_{\tilde{\theta}} \right] R dR,$$

or since $d(\frac{R^2}{2}) = R dR$,

$$\overline{\Delta P_0} = \frac{2}{(R_o^2 - R_i^2)} \int_{R_i}^{R_o} \left[(\Delta P_0)_{\tilde{\theta}} \right] d\left(\frac{R^2}{2}\right).$$

These last two relationships for ΔP_0 are equivalent and produce identical results. The actual integration was performed by use of the trapezoidal rule, with pressure measurements taken from each of the four scans at 20 increments across the inlet annulus.

At the discharge measuring station, four radial traverses were again taken with actuator-mounted Kiel probes. The Kiel probes were desirable at this location since flow visualization indicated the turbulent nature of the flow, and these probes are relatively insensitive to flow direction, reading true values of total pressure at flow angles of up to ± 40 degrees. This data was also plotted on a chart recorder, averaged, and integrated in the same manner as for the inlet station. Figure 13 is a superposition of traverses from the set of discharge data corresponding to the inlet data in Figure 12.

Calculation of Velocity and Blockage. From the inlet total pressure data, velocity profiles and boundary layer blockage levels were also

Figure 13
DISCHARGE TOTAL PRESSURE TRAVERSES

determined. The velocity was extracted from an incompressible relationship for total pressure at a point.

$$P_0 = P + \frac{\bar{\rho} u^2}{2g_c},$$

and

$$P_0 - P = \frac{\bar{\rho} u^2}{2g_c}.$$

Therefore,

$$u^2 = \frac{2g_c}{\bar{\rho}} (P_0 - P),$$

and as a result,

$$u = \sqrt{\frac{2g_c}{\bar{\rho}} (P_0 - P)}.$$

Using measured static pressure assumed constant at a cross section and density calculated from the measured static pressure and the ideal gas law, the local velocity was found as a function of the local total pressure. Here again, reference may be made to Appendix A for an explanation of the validity of this approach. Figure 14 is a dimensionless velocity profile from the inlet data of Figure 12.

The local velocity determined by the above procedure was also employed in the boundary layer blockage calculation. The ratio of blocked to geometric area was derived in the manner of Sovran and Klomp^[2].

As previously stated,

$$A_B = A - A_E.$$

Figure 14
INLET VELOCITY PROFILE

Dividing through by the geometric area,

$$\frac{A_B}{A} = 1 - \frac{A_E}{A} = \frac{1}{A} \int_A \left(1 - \frac{u}{U}\right) dA.$$

With areas in terms of radii, separation into two integrals yields

$$\frac{A_B}{A} = \frac{2}{R_o^2 - R_i^2} \left\{ \int_{R_i}^{R_o} R dR - \frac{1}{U} \int_{R_i}^{R_o} u R dR \right\}.$$

Finally,

$$B = \frac{A_B}{A} = 1 - \frac{2}{[(R_o^2 - R_i^2)U]} \int_{R_i}^{R_o} u R dR.$$

A FOCAL computer program operated on the PDP 8/L computer determined the local value along the radius of the (uR) quantity. This local value, plotted against radius, was then integrated using the trapezoidal rule and inserted into the above equation from which blockage was determined. The remaining variable in the equation, the maximum cross-sectional velocity (U) , was taken from the velocity profile. The (uR) profile with the local velocities of Figure 14 is illustrated in Figure 15.

At this point, the necessary data to determine the loss coefficient for an established blockage level was in hand. With inlet Mach number as a similarity parameter and independent variable, one point on a plot of loss coefficient versus Mach number was fixed. The remaining test plan consisted of repeating the aforementioned procedures over a range of inlet Mach numbers for two levels of inlet blockage and two centerbody configurations. The resulting four curves demonstrated the

Figure 15
INLET (uR) PROFILE

relationship between loss coefficient and Mach number over the range tested and also included the effects of blockage change and of center-body change; i.e., of change in the axial length versus inlet annulus height parameter. The relationships for the gradual inlet taper configuration were similarly determined, as were the relationships for static pressure recovery.

IV. DISCUSSION OF RESULTS

Comparison of Configurations

First Configuration. Test results of the annular diffuser configuration comprised of area ratio = 2.79 and $L/\Delta R = 12$ at inlet blockage levels equal to 8% and 4% are illustrated in Figures 16 and 17, along with subsequent results. It can be seen from the upper set of curves in Figure 16 that in the incompressible flow range, there appears to be independence of the loss coefficient on Mach number, with an 0.316 value at the higher blockage level and an 0.300 value at the lower level. In the range of compressibility, however, the relationship is not linear, with the rate of rise of the loss coefficient increasing as the Mach number increases. Also to be noted from the plot is that, as the Mach number increases, the effects of inlet blockage lessen, and the two curves tend toward convergence.

The lower pair of curves of static pressure recovery versus Mach number in Figure 17 again illustrates a blockage effect and a slight nonlinearity between the two parameters in the compressible flow range. For the lower blockage level, the pressure recovery coefficient varies from 0.550 to 0.505, while for the higher level, the variation is from 0.520 to 0.460. Here the effects of inlet blockage remain distinguishable over the Mach number range tested, in contrast to the effects on loss coefficient.

Second Configuration. Results of testing of the second (extended) configuration, which consisted again of a 2.79 area ratio but with

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Figure 16
VARIATION OF LOSS COEFFICIENT WITH INLET MACH NUMBER

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Figure 17
VARIATION OF PRESSURE RECOVERY COEFFICIENT
WITH INLET MACH NUMBER

$L/\Delta R = 15$ at inlet blockage levels of 9% and 6% are also illustrated in Figures 16 and 17. From the lower pair of curves in Figure 16, in the incompressible range the loss coefficient is 0.226 at the higher blockage level and 0.214 at the lower level. A significant reduction in loss coefficient with the extended centerbody and its more gradual taper is a major point to be observed. The effects of inlet blockage and inlet Mach number are similar to the effects illustrated by the upper set of curves, with even a greater tendency toward convergence indicated. These relationships varied in a similar manner in the annular diffuser work of John Henry, et al^[5], reported on page 7.

With regard to the variation of pressure recovery coefficient with Mach number shown by the upper pair of curves in Figure 17, the relationships exhibit characteristics similar to those of the previous configuration once again. The pressure recovery coefficient is seen to vary from 0.664 to 0.620 at the lower blockage level and from 0.615 to 0.580 at the higher level. Perhaps it should be noted that the general trend of pressure recovery coefficient decreasing with increased blockage is substantiated by the work of Tyler and Williamson^[17], shown in Figure 3, page 6, and by the Sovran and Klomp study^[13], reported on page 5. Finally, the obvious improvement in pressure recovery coefficient over results with the first centerbody is illustrated in the plot.

Effects of Other Parameters and Measurements

Reynolds Number. As was indicated earlier, compressible flows cannot be characterized solely as functions of Mach number but are functions of Reynolds number as well. Therefore, to more fully describe the parameters investigated, first the relationship between Reynolds

number and Mach number is presented, and second, the previously determined parameters are related to Reynolds number. Figure 18 illustrates the relationship between Reynolds number and Mach number. The variation between the two is linear up to a Mach number of approximately 0.35, at which point the Mach number increases more rapidly than does the Reynolds number due to property variations in this flow region.

Figures 19 and 20 are comparable to Figures 16 and 17, respectively, except that this time the Reynolds number is the independent parameter. In Figure 19, the two pairs of loss coefficient curves exhibit a greater tendency toward convergence than in the Mach number plot. The pairs of pressure recovery curves of Figure 20 are quite similar to those of Figure 16 and again indicate a convergence tendency which was not as evident in the Mach number plot. Although these results are far from conclusive, Reynolds number effects may play a role in the merging trends exhibited by these parameters.

Turbulence Intensity Measurement. At this stage of the test program, it was decided that a measurement of the inlet turbulence intensity, i.e., the ratio of turbulence velocity fluctuations to the local time-averaged velocity at a cross section, would be helpful. This measurement was included because the screening used to vary the blockage level affected not only the velocity profile but the turbulence structure as well. Using a hot-wire anemometer, measurements were made with the second configuration tested. For the 9% blockage level, the midstream inlet turbulence intensity was 2.0%, while for the 6% level, the intensity was 1.3%. The effect of this seemingly small change in turbulence intensity was most probably overshadowed by the greater change in blockage level.

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Figure 18
REYNOLDS NUMBER VARIATION WITH MACH NUMBER

DWG. NO. G-74-729

Figure 19
VARIATION OF LOSS COEFFICIENT
WITH INLET REYNOLDS NUMBER

DWG. NO. G-74-731

Figure 20

VARIATION OF PRESSURE RECOVERY COEFFICIENT
WITH INLET REYNOLDS NUMBER

The details of the turbulence intensity measurements may be seen in Appendix C.

Flow Visualization. A qualitative look was taken at the $L/\Delta R = 15$ centerbody configuration at its low blockage level. Although this setup produced generally the lowest losses, flow visualization with both yarn tufts and smoke indicated that separation was still present in the diffuser. Figure 21 illustrates dramatically the effects of separation on surface flow. At the instant this picture was taken, the yarn tufts clearly indicate backflow at the downstream end of the diffuser. Smoke also showed areas of separation extending further out into the stream and nearer to the inlet, as predicted by Stevens[14], page 2. From these visualizations, it was surmised that a still longer diffuser might further reduce losses and increase performance.

Gradual Inlet Taper

The gradual inlet taper configuration resulted from modifying the $L/\Delta R = 12$ centerbody by rounding the taper at the diffuser inlet. Instead of a sharp corner, therefore, a more gradual transition to the cone taper was produced by the 4-inch radius applied to the corner, as seen in Figure 22. It was hoped that improved performance could be achieved by delaying separation off the inner wall with this transition.

Results of several tests at a blockage level of about 6% indicated that the gradual inlet taper was in fact a definite improvement over its sharply tapered counterpart. The loss coefficient produced was essentially contained within the envelope of loss coefficients resulting from the $L/\Delta R = 15$ centerbody, indicating that rounding the sharp inlet corner could produce as much improvement in loss coefficient as adding

PHOTO NO. PH-74-903

Figure 21
FLOW VISUALIZATION

DWG. NO. G-74-736

Figure 22
DETAILS OF GRADUAL INLET TAPER

25% to the diffuser's length. Gains were also seen in the pressure recovery coefficient, although these were not quite so dramatic as in the loss coefficient. This could quite possibly be explained by the supposition that pressure recovery continued in the tailpipe of the diffuser where there were no measuring stations. Comparisons between the gradual inlet taper and the original centerbody for the diffuser with $L/\Delta R = 12$ are illustrated by the loss coefficient and the static pressure recovery coefficient plots of Figures 23 and 24, respectively.

The obvious advantage of the gradual inlet taper lies in the fact that no increase in diffuser axial length was necessary to reduce the losses. Reducing the space needed for diffusion could conceivably be a prime concern where housing for equipment is at a premium; i.e., where there is only a limited amount of space between mating components. Also, a cost saving could result from the use of less material in diffuser construction.

DWG. NO. G-74-737

Figure 23
LOSS COEFFICIENT VARIATION WITH
INLET MACH NUMBER FOR GRADUAL INLET TAPER

DWG. NO. G-74-738

Figure 24
PRESSURE RECOVERY COEFFICIENT VARIATION
WITH INLET MACH NUMBER FOR GRADUAL INLET TAPER

V. CONCLUSIONS

The relationship between the total pressure loss coefficient and other diffuser performance parameters was sought in this study and was obtained for the flow conditions and geometric configurations tested. Several performance characteristics of this type of annular diffuser have been defined as a result of the study.

1. The nondimensional length parameter was shown to have definite effects on both the loss coefficient and the pressure recovery coefficient.
2. The relative independence of loss coefficient on either Mach or Reynolds number in the incompressible flow range was evident. Also, the linear relationship of static pressure recovery coefficient on either independent parameter in this range was demonstrated. In the compressible flow range, however, the loss coefficient exhibited a nonlinear relationship with both Mach and Reynolds numbers. This nonlinear tendency appeared, although less noticeably, in the relationship between pressure recovery coefficient and the independent parameters.
3. In the incompressible range, differences in the inlet blockage level produced small but consistent differences in the loss coefficient. As the flows increased into the range of compressibility, however, the tendency toward convergence obscured any distinction between blockage levels.

4. Perhaps the most important result to be derived from this project was the demonstration that rounding of the center-body to produce a gradual inlet taper could significantly improve diffuser performance.

VI. SUGGESTIONS FOR FURTHER STUDY

This study has contributed annular diffuser performance information over a specific area of concern but has also reinforced the suggestions of Stevens^[14] and Sovran and Klomp^[13] that a general evaluation of annular diffuser performance parameters remains a primary course for future experimental work. Relating to the present study, there are certain areas which would benefit from continued testing.

1. It is probable that the best of the diffuser configurations tested ($L/\Delta R = 15$) did not result in the optimum performance that could be expected from this type of annular diffuser, so extending the axial length even further until the true "optimum" configuration is achieved would be a profitable course for further study.
2. A thorough investigation of the gradual inlet taper has potential value. In this study, only one centerbody was rounded to only one radius. The results were impressive, but several new questions arose. What would be the optimum radius for the inlet corner? How much, if any, would the performance of the $L/\Delta R = 15$ configuration be improved by the addition of a radius? These and other similar questions make a thorough study of rounding the centerbody taper on this type of annular diffuser seem attractive and potentially profitable, and it is hoped that such an investigation will be undertaken.

3. Only a limited amount of data was taken in the compressible flow range, and results in this area were inconclusive with regard to inlet blockage effects. Therefore, it would seem advantageous to extend the data to higher flows and also to reinforce the existing data to more concretely establish diffuser characteristics there.

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APPENDIXES

APPENDIX A

EFFECTS OF COMPRESSIBLE FLOW ON INCOMPRESSIBLE BLOCKAGE RELATIONSHIP

The incompressible blockage (B_i) relationship used in this study was

$$B_i = \frac{1}{A} \int_A \left(1 - \frac{u_i}{U}\right) dA.$$

This definition differs from the compressible blockage (B_c) relationship,

$$B_c = \frac{1}{A} \int_A \left[1 - \frac{(\rho_c u_c)}{(\rho_c u_c)_{\max}}\right] dA,$$

and use of the incompressible equation in the compressible flow range does introduce some error. To determine the magnitude of this error and to evaluate its significance, the variables within the integrals above will be examined.

Incompressible local velocity (u_i) is taken from the expression

$$P_0 - P = \frac{\rho_i u_i^2}{2g_c},$$

where total pressure (P_0) varies locally, and static pressure (P) is assumed constant at a cross section. From this relationship,

$$u_i = \sqrt{\frac{(P_0 - P) 2g_c}{\rho_i}}$$

Also, if the incompressible density (ρ_i) is determined from the ideal gas law and an average static temperature ($\bar{T}$),

$$\rho_i = \frac{P}{\bar{R}\bar{T}}.$$

Introducing total temperature (T_0),

$$\rho_i = \frac{P}{\bar{R}\bar{T}} \left(\frac{T_0}{\bar{T}} \right) = \frac{P}{\bar{R}T_0} \left(\frac{T_0}{\bar{T}} \right) = \rho' \left(1 + \frac{\gamma - 1}{2} \bar{M}^2 \right),$$

where $\rho' = \frac{P}{\bar{R}T_0}$, and from basic gas dynamics, $\left(1 + \frac{\gamma - 1}{2} \bar{M}^2 \right) = \frac{T_0}{\bar{T}}$.

Considering compressible flow, local compressible velocity (u_c) is determined from the relationship,

$$\frac{P_0}{P} = \left(1 + \frac{\gamma - 1}{2} M^2 \right)^{\frac{\gamma}{\gamma - 1}},$$

from which $P_0 - P = \frac{\rho_c u_c^2}{2g_c} \left(1 + \frac{M^2}{4} + \frac{M^4}{40} + \dots \right)$ can be derived by expansion (see page 64). From this relationship,

$$u_c = \sqrt{\frac{(P_0 - P) 2g_c}{\rho_c f(M)}},$$

where $f(M) = \left(1 + \frac{M^2}{4} + \frac{M^4}{40} + \dots \right)$. In addition, local compressible density (ρ_c) is found from the ideal gas law to be

$$\rho_c = \frac{P}{\bar{R}T} = \frac{P}{\bar{R}T_0} \left(\frac{T_0}{T} \right) = \rho' \left(1 + \frac{\gamma - 1}{2} M^2 \right).$$

From the expressions obtained for velocity and density, the compressible quantities may be related to the incompressible quantities, as is done below.

$$u_i = \sqrt{\frac{(P_0 - P) 2g_c}{\rho' (1 + \frac{\gamma - 1}{2} M^2)}} ;$$

$$u_c = \sqrt{\frac{(P_0 - P) 2g_c}{\rho' (1 + \frac{\gamma - 1}{2} M^2) f(M)}} ;$$

therefore,

$$\frac{u_c}{u_i} = \sqrt{\frac{(1 + \frac{\gamma - 1}{2} M^2)}{(1 + \frac{\gamma - 1}{2} M^2) f(M)}} ,$$

and

$$\frac{\rho_c}{\rho_i} = \frac{(1 + \frac{\gamma - 1}{2} M^2)}{(1 + \frac{\gamma - 1}{2} M^2)} .$$

If the compressible blockage relationship is rewritten as

$$B_c = \frac{1}{A} \int_A \left[1 - \frac{\rho_c u_c}{(\rho_{c,u_{c,max}}) (u_{c,max})} \right] dA ,$$

then the integrand becomes

$$1 - \frac{\sqrt{\frac{(1 + \frac{\gamma - 1}{2} M_{max}^2) f(M_{max})}{(1 + \frac{\gamma - 1}{2} M_{max}^2) f(M)}} \left(\frac{u_i}{U} \right)}{\left(\frac{u_i}{U} \right)} = 1 - g(M, M_{max}) \frac{u_i}{U} .$$

Inserting extreme conditions into this expression, i.e., $M = 0.0$ and

$M_{max} = 0.5$, the quantity becomes

$$1 - 1.0067 \frac{u_i}{U} ,$$

and the blockage relationship is

$$B_c = \frac{1}{A} \int_A (1 - 1.0067 \frac{u_i}{U}) dA.$$

To evaluate blockage error (ψ), the compressible blockage is used as a reference, and

$$\psi = \frac{B_i - B_c}{B_c} .$$

At the incompressible blockage levels listed below, the errors and resulting compressible blockage limits were determined.

B_i	ψ	B_c
4%	0.19	3.2%
6%	0.12	5.3%
8%	0.08	7.3%
10%	0.06	9.4%

From an examination of $g(M, M_{\max})$, it can be seen that the incompressible blockage is a maximum value, so that a given compressible blockage would fall in the interval between B_i and B_c . It should be noted again that blockage limits calculated are extreme values which could not be approached over a cross section, since even at high blockage levels, the velocity profiles of Figures 25 and 26 indicate that the velocity over the majority of the radius is at least 95% of maximum velocity. Another significant factor is that the difference between

Figure 25
INLET VELOCITY PROFILES FOR $L/\Delta R = 12$

Figure 26
INLET VELOCITY PROFILES FOR $L/\Delta R = 15$

given incompressible blockage levels actually increases when compressibility is considered. As a result, use of the incompressible relationship could not be responsible for the tendency toward convergence seen in the loss coefficient plots at higher Mach numbers.

APPENDIX B

DETAILS OF DATA REDUCTION PROGRAM

The data reduction program utilized the aerodynamic relations listed below to calculate the parameters at the mass flow measuring station.

$$1. \quad P[\text{lbf/sq ft}] = P_{\text{atm}} - 144(\Delta P).$$

$$2. \quad \bar{P}_0[\text{lbf/sq ft}] = P_{\text{atm}} - 144(\Delta P_0).$$

$$3. \quad \bar{M} = \frac{2}{\gamma - 1} \left[\frac{P_0^{(\gamma-1)/\gamma}}{P} - 1 \right].$$

$$4. \quad \bar{T}[^\circ\text{R}] = \frac{T_0}{1 + \left(\frac{\gamma - 1}{2}\right) \bar{M}^2}.$$

$$5. \quad \bar{\rho}[\text{lbm/cu ft}] = \frac{P}{53.34 \bar{T}}.$$

$$6. \quad \bar{u}[\text{ft/sec}] = \bar{M}(49.02 \sqrt{\bar{T}}).$$

$$7. \quad \dot{m}[\text{lbm/sec}] = \bar{\rho} \bar{u} A.$$

$$8. \quad \text{Re} = \frac{\bar{\rho} \bar{u} D}{\mu},$$

where $\mu = 10.1789 \times 10^{-10} \frac{\bar{T}^{3/2}(734.7)}{\bar{T} + 216}$ is Sutherland's

viscosity equation.

$$9. \quad \bar{P}_v[\text{lbf/sq ft}] = \frac{\bar{\rho} \bar{u}^2}{2g_c}.$$

Using the mass flow determined above, parameters at the model stations, where only static pressures were measured in the preliminary run, were calculated. The values of P , $\bar{M}$, $\bar{u}$, $\bar{\rho}$, and $\bar{P}_v$ were found in a manner similar to the above; $\bar{T}$, $\bar{P}_0$, and Re calculations varied.

$$\bar{T} [^\circ\text{R}] = \frac{\sqrt{1 + 4(C_1)T_0} - 1}{2C_1}.$$

$$\bar{P}_0 [\text{lb}/\text{sq ft}] = P + \bar{P}_v \left(1 + \frac{\bar{M}^2}{4} + \frac{\bar{M}^4}{40}\right).$$

$$\text{Re} = \frac{\bar{\rho} \bar{u} (D_o - D_i)}{\mu}.$$

$$\text{Derivation of } \bar{T} = \frac{\sqrt{1 + 4(C_1)T_0} - 1}{2C_1}$$

$$C_p T_0 = C_p \bar{T} + \frac{\bar{u}^2}{2g_c J}.$$

$$T_0 [^\circ\text{R}] = \bar{T} [^\circ\text{R}] + \frac{\bar{u}^2}{2g_c} [\text{sq ft}/\text{sec}^2] \left[\frac{\text{lb f-sec}^2}{\text{lb m-ft}} \right].$$

$$C_p \equiv [\text{Btu}/\text{lb m-}^\circ\text{R}]; \quad J \equiv [\text{ft-lb f}/\text{Btu}].$$

$$C_p T_0 [\text{Btu}/\text{lb m}] = C_p \bar{T} [\text{Btu}/\text{lb m}] + \frac{\bar{u}^2}{2g_c J} [\text{ft-lb f}/\text{lb m}] [\text{Btu}/\text{ft-lb f}].$$

$$C_p T_0 = C_p \bar{T} + \frac{\bar{u}^2}{2g_c J}.$$

$$\bar{T} = T_0 - \frac{\bar{u}^2}{2g_c J C_p}.$$

$$\dot{m} = \bar{\rho} \bar{u} A; \quad \bar{\rho} = \frac{P}{\bar{R} \bar{T}}.$$

$$\bar{u} = \frac{\dot{m}}{\bar{\rho} A} = \dot{m} \frac{\tilde{R}\bar{T}}{PA}$$

$$\bar{T} = T_0 - \frac{(\dot{m}\tilde{R}\bar{T})^2}{(PA)^2 (2g_c J C_p)}$$

$$\bar{T} = T_0 - \left(\frac{\dot{m}\tilde{R}\bar{T}}{PA}\right)^2 \frac{1}{12025}, \text{ for air}$$

$$\bar{T}^2 + \left(\frac{PA}{\dot{m}\tilde{R}}\right)^2 (12025)\bar{T} - \left(\frac{PA}{\dot{m}\tilde{R}}\right)^2 (12025)T_0 = 0$$

$$k = \left(\frac{PA}{\dot{m}\tilde{R}}\right)^2 (12025)$$

$$\bar{T}^2 + k\bar{T} - kT_0 = 0$$

$$\bar{T} = \frac{-k \pm \sqrt{k^2 + 4kT_0}}{2} = \frac{k}{2} \left(\sqrt{1 + 4\frac{T_0}{k}} - 1 \right)$$

$$C_1 = \left(\frac{\dot{m}}{PA}\right)^2 \left(\frac{R^2}{12025}\right) = \frac{1}{k}$$

$$\therefore \bar{T} = \frac{\sqrt{1 + 4(C_1)T_0} - 1}{2C_1}$$

$$\text{Derivation of } \bar{P}_0 = P + \bar{P}_v \left(1 + \frac{\bar{M}^2}{4} + \frac{\bar{M}^4}{40}\right)$$

$$\frac{\bar{P}_0}{P} = \left(1 + \frac{\gamma - 1}{2} \bar{M}^2\right)^{\gamma/\gamma-1}$$

$$\text{Form of Expansion: } (x + y)^n = x^n + nx^{n-1}y + n\left(\frac{n-1}{2!}\right)x^{n-2}y^2$$

$$+ n(n-1)\left(\frac{n-2}{3!}\right)x^{n-3}y^3 + \dots$$

$$\frac{\overline{P}_0}{P} = 1 + \left(\frac{\gamma}{\gamma-1}\right) (1) \left(\frac{\gamma-1}{2} \bar{M}^2\right) + \left(\frac{\gamma}{\gamma-1}\right) \left(\frac{1}{\gamma-1}\right) \left(\frac{1}{2}\right) \left(\frac{\gamma-1}{2}\right)^2 \bar{M}^4$$

$$+ \left(\frac{\gamma}{\gamma-1}\right) \left(\frac{1}{\gamma-1}\right) \left(\frac{\gamma-2}{\gamma-1}\right) \left(\frac{1}{2 \cdot 3}\right) \left(\frac{\gamma-1}{2}\right)^3 \bar{M}^6$$

$$\frac{\overline{P}_0}{P} = 1 - \frac{\gamma}{2} \bar{M}^2 + \frac{\gamma}{8} \bar{M}^4 + \frac{2\gamma - \gamma^2}{48} \bar{M}^6$$

$$\overline{P}_0 = P + \frac{P}{2} \gamma \bar{M}^2 + \frac{\bar{M}^2}{4} \frac{P}{2} \gamma \bar{M}^2 + \frac{\bar{M}^4}{24} (2-\gamma) \frac{P}{2} \gamma \bar{M}^2$$

$$P_v = \frac{\bar{\rho} \bar{u}}{2g_c} = \frac{1}{2g_c} \left(\frac{P}{RT}\right) (\bar{u}^2) \left(\frac{\gamma}{\gamma}\right); \quad \bar{M}^2 = RTg_c$$

$$P_v = \frac{P}{2} \gamma \bar{M}^2$$

$$\overline{P}_0 = P + P_v + P_v \frac{\bar{M}^2}{4} + P_v \left(\frac{2-\gamma}{24}\right) \bar{M}^4$$

$$\therefore \overline{P}_0 = P + P_v \left(1 + \frac{\bar{M}^2}{4} + \frac{\bar{M}^4}{40}\right)$$

$$\text{Explanation of } Re = \bar{\rho} \bar{u} \left(\frac{D_o - D_i}{\mu}\right)$$

$$\text{Annular Area: } A = \frac{\pi}{4} (D_o^2 - D_i^2)$$

$$\text{Wetted Perimeter: } P_w = \pi(D_o + D_i)$$

$$\text{Characteristic Length: } \frac{4A}{P_w} = \frac{4\left[\frac{\pi}{4} (D_o^2 - D_i^2)\right]}{\pi(D_o + D_i)} = D_o - D_i$$

$$\therefore Re = \bar{\rho} \bar{u} \left(\frac{D_o - D_i}{\mu}\right)$$

Table I
TYPICAL PRINTOUT OF PRELIMINARY CALCULATIONS

22.0	74.63	13.5413	T _{ATM} (°C), cm Hg, s.g. Hg	
+0.2960043E+01	Avg ΔP			
+0.2870721E+01	Avg ΔP ₀		FLOW STATION	
ATM T	ATM P	STAT P	AREA	
+0.5312900E+03	+0.2069783E+04	+0.1643536E+04	+0.9395655E+00	
TOT P	MACH	STAT T	RHO	
+0.1656399E+04	+0.1055862E+00	+0.5301080E+03	+0.5811396E-01	
VEL	FLOW	VEL P	REYN	
+0.1191717E+03	+0.6506999E+01	+0.1282601E+02	+0.6191786E+06	
+0.3512952E+01	Avg ΔP ₁		INLET STATION	
ATM T	ATM P	STAT P	AREA	
+0.5312900E+03	+0.2069783E+04	+0.1563917E+04	+0.2895919E+00	
TOT P	MACH	STAT T	RHO	
+0.1707007E+04	+0.3558729E+00	+0.5181652E+03	+0.5657327E-01	
VEL	FLOW	VEL P	REYN	
+0.3971116E+03	+0.6506999E+01	+0.1386441E+03	+0.1897886E+07	
+0.2987536E+01	Avg ΔP ₂		DISCHARGE STATION	
ATM T	ATM P	STAT P	AREA	
+0.5312900E+03	+0.2069783E+04	+0.1639577E+04	+0.8091384E+00	
TOT P	MACH	STAT T	RHO	
+0.1656925E+04	+0.1227131E+00	+0.5296947E+03	+0.5801920E-01	
VEL	FLOW	VEL P	REYN	
+0.1384482E+03	+0.6506999E+01	+0.1728270E+02	+0.6668640E+06	

APPENDIX C

TURBULENCE INTENSITY

If a local velocity is considered to be composed of a time-averaged velocity, u , and a small, time-varying component, $\hat{u}$, then the turbulent velocity fluctuations may be described by the root-mean-square of $\hat{u}$, or

$$u' = \sqrt{(\hat{u})^2} . .$$

The relative turbulence intensity (Tu), therefore, is the ratio of the turbulent velocity fluctuations (u') to the local time-averaged velocity (u), or

$$Tu = \frac{u'}{u} .$$

Measurements of the turbulence intensity were performed with a Flow Corporation Model 900 hot-wire anemometer system. For constant resistance operation with the hot-wire held perpendicular to the average direction of flow in a stream where the temperature was constant, the turbulence intensity was determined from the relation^[2],

$$Tu = \frac{\sqrt{(\hat{u})^2}}{u} = \frac{4}{1 - \frac{E_0^2}{E_v^2}} \frac{e_v^2 - e_0^2}{E_v^2} .$$

In the above relation, E_0 and E_v represent hot-wire voltages at no flow (E_0) and in the flow stream (E_v), while e_0 and e_v are the root-mean-square voltages at no flow (e_0) and in the flow stream (e_v).

As was stated in the previous discussion of turbulence intensity, page 37, for the blockage level of 9%, the midstream turbulence intensity was 2.0%, while for the 6% level, it was 1.3%. Figures 27 and 28 illustrate turbulence intensity profiles for the 9% and 6% levels, respectively. It can be seen that over the range of Mach numbers checked at each level, there was no significant difference in the midstream turbulence intensity, and the profiles exhibited only a slight variation.

Figure 27
INLET TURBULENCE INTENSITY PROFILES FOR
 $L/\Delta R = 15$ AND $B = 9\%$

Figure 28
INLET TURBULENCE INTENSITY PROFILES FOR
 $L/\Delta R = 15$ AND $B = 6\%$

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