A Study of the Base Pressure Distribution of a Slender Body of Square Cross-Section

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A low-speed wind-tunnel investigation of a slender body of square cross-section was conducted to determine the effect of boattail geometry on the base pressure coefficient. The data were obtained over an angle-of-attack range of 0 to 8 degrees. Surface flow patterns were visualized using tufts. An exploratory numerical modeling effort was also performed. The base pressure showed the expected decrease from top to bottom as the body angle of attack was raised. The base pressure coefficient showed a rapid rise when the angle of attack increased past 4 deg.

Nomenclature

A	=	reference area (in. ²)
A_f	=	nose fineness ratio
$C_{p,b}$	=	base pressure coefficient
$C_{p,i}$	=	absolute pressure coefficient
D	=	length of square cross-section side (in.)
D_b	=	boattail diameter
i	=	numeric index
P_∞	=	freestream static pressure (in. H ₂ O)
q_∞	=	freestream dynamic pressure (in. H ₂ O)
Re	=	Reynolds number
r	=	center-body corner radius (in.)
T	=	temperature (deg. F)
x	=	Cartesian coordinate (in.)
α	=	angle-of-attack (deg.)
β	=	afterbody angle (deg.)

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I. Introduction

MUNITIONS with square or rectangular cross-section have better volumetric efficiency than conventional munitions of circular section; such configurations have been the subject of continued interest.¹⁻⁹ For a bibliography of earlier work, see Ref. 10. Most of the existing literature is concerned with the aerodynamics of such shapes which are complicated by the lack of axial symmetry. One aspect that had not been well investigated is the afterbody shape for minimum base drag. This paper reports an exploratory study of the base pressure coefficient of a body with a square cross-section. The experimental study is complemented by numerical modeling.

II. Wind Tunnel Facility

The experimental data were acquired in a closed circuit, low-speed wind tunnel with a closed test section. The test section is 61 cm high, 91 cm wide and 190 cm long (24 in. $\times$ 36 in. $\times$ 75 in.). The tunnel has a continuously variable speed capability from zero to approximately 50 m/s (160 ft/s). At the maximum operating condition, the tunnel is capable of obtaining a unit Reynolds number of 3 million/m (1 million/ft).

For the experiments, the tunnel was monitored by two Omega PX653 series differential pressure transducers with dynamic ranges of 0-10 and 0-5 in. H₂O. These transducers have an accuracy of one percent of full scale, which corresponds to an uncertainty of ± 0.05 in. H₂O. The data were acquired by a National Instruments PCI-MIO-16E-4 DAQ with a LabVIEW™ interface running on an Intel Pentium-III™ class, Windows XP™ workstation. The transducers were calibrated against a Meriam Model 40HE35 slant tube manometer.

III. Model

The test article comprised of five major components as shown in Fig. 1. The colors in the figure are used to distinguish the major model sections, comprising of the nose, the centerbody, the boattail and the base. The overall length of the model was 783.7 mm (30.853 in.) The nose cone was manufactured from acetal copolymer, which was selected for its ease of machining. The rest of the model was constructed of 6061-T6 aluminum. The model was mounted such that the sides were horizontal and vertical.

The sharp pointed nose pointed toward the flow, which is directed from right to left in the Fig. 1. The tangent ogive nose had a fineness ratio of 1.59. The base of the nose was a 101.6 mm square (4 in.). The nose cone contour was designed to enclose a large portion of the strut mount, thus minimizing any unnecessary disturbances to the external flow field. The centerbody had a 101.6 mm (4 in.) square section and it was 304.8 mm (12 in.) long. The corners of the square section were all sharp. A static pressure tap was located at the geometric center of each surface of the centerbody.

The boattail section had a contraction ratio of $D_b/D = 0.798$, which is defined as the diameter of the base section over the hydraulic diameter of the boattail section. This corresponded to an afterbody angle of 9 deg. and an overall length of 185.3 mm (7.296 in.). Fifteen pressure ports, spaced at 11.56 mm (0.455 in.) were located along the center of the bottom face of the boattail – each having a diameter of 1.59 mm (0.0625 in.). Ten additional pressure ports, of the same diameter and spacing, were placed along the bottom, rounded corner of the boattail, oriented 45 deg. with respect to the model centerline (Figure 2). These ports were located on the bottom of

Figure 1. Schematic of assembled model.

Figure 2. Boattail pressure ports.

the boattail to obtain measurements that were least disturbed by the mounting strut. The model base section boundary was defined by the extrapolation of the afterbody angle down to a diameter of 81 mm (3.19 in.).

For the base pressure port arrangement, a radial pattern was used such that there was one port at the geometric center of the base, and at radii of 11.7, 23.1 and 34.8 mm (0.46, 0.91 and 1.37 in.) giving 6, 13 and 19 equally spaced ports, respectively. Two additional sets of four ports each extended the pair of afterbody tap traces to the base of the model. The model was mounted in the test section by connecting it to a slender streamlined strut extending from the ceiling of the test section 25.5 mm (1 in.) fore of the windward edge of the centerbody.

The afterbody section of the missile model was designed to be modular, so that the main body of the model may be reused in future experiments with alternative boattail geometries. A boundary layer transition strip was attached on the model nose, 63.5 mm (2.5 in.) behind the cone tip as measured along the model surface per Ref. 11. The transition strip was an annular region of #37 sand grit, 1.6 mm (0.0625 in.) wide.

IV. Test Conditions

Experiments were conducted at zero yaw and roll angles, for the angle-of-attack range of 0 to 8 deg. with the wind tunnel set at its maximum operating speed. During each run, the model referenced Reynolds number (based upon the side length of the center-body section) decreased slightly from 9.47×10^5 to 8.95×10^5 due to an increase in the tunnel freestream temperature. A typical wind tunnel model setup is shown in Fig. 3 – note the digital protractor in the photograph being used for determining the angle-of-attack. The uncertainty of the angle measurement was estimated to be ± 0.1 deg.

Measurements were taken using a bank of 41 brass gang valves. The valves were manually switched one by one to an Omega PX653-05PV pressure transducer. Readings from all 78 pressure ports located on the test model were taken while the wind tunnel was at the maximum speed setting. All the surface pressure data along with test section temperature, atmospheric pressure, and wind tunnel static pressures were collected via a LabVIEW interface.

Other than the above measurements, tuft flow visualization was also attempted. Size T3 cotton threads were affixed to the sides of the missile model using Scotch™ tape to avoid clogging of the surface pressure ports with liquid adhesive. The resulting flow field was then photographed through the Plexiglas™ test section door using a Nikon D40 digital camera with ambient fluorescent lighting.

Figure 3. Typical tunnel model setup.

V. Discussion of Results

The primary results obtained from the experiments were the individual base port pressure measurements at different angles of attack. These individual pressure measurements were then reduced into contour plots of the base pressure coefficient and integrated numerically to yield an area-weighted, average base pressure coefficient for the given freestream tunnel conditions.

A. Base Pressure Variation Results

The variation of the missile base pressure with angle-of-attack is shown in Figs. 4-11 in terms of an “absolute” pressure coefficient

$$C_{p,i} = \left\| \frac{P_i - P_\infty}{q_\infty} \right\| \quad (1)$$

In Eq. (1), P_i is the static pressure at the i^{th} location on the model base, P_∞ is the test section static pressure, and q_∞ is the test section dynamic pressure. Note that for Figs. 4-8, the view shown is as the base would appear when looking upstream.

For a 0 deg. angle-of-attack, the result shown in Fig. 4 is what would be expected for an incompressible base flow over an axisymmetric slender body, that is, a relatively uniform base pressure distribution. At first glance this may not be evident, as several different contour intervals are shown. Note, however, that the difference between the maximum and minimum contour level is 0.05, which is within the uncertainty bounds of the differential pressure transducers.

As the angle-of-attack is increased, the effects of a lifting body become more pronounced in the base pressure contours. Beginning with Fig. 5, at an angle of attack of 2 deg., relatively high absolute pressure coefficients (corresponding to a more negative C_p value in the traditional sense) begin to emerge at the top of the base. This trend continues with increasing angle of attack until, at the maximum tested angle of attack of 8 deg., the upper portion of the base is at a much lower relative pressure than the lower portion.

One result, however, may be taken from a more thorough consideration of Figs. 8-11. Note that the developing low pressure zones on the upper portion of the base area are not symmetrical with respect to the y -axis. For a yaw and roll angle of 0 deg., we might expect to see this symmetry reflected in the flow field. From the data, however, the model appears to have a slight yaw with respect to the freestream. This can be attributed to the nature of the closed-circuit tunnel that imparts a small yaw of 0.3 deg. from the right of the tunnel axis as looking upstream (Figs. 4-8). Additionally, it should be mentioned that a 0.3 deg. positive angle-of-attack flow angularity was measured in the empty test section.

Figure 4. Base pressure contours for $\alpha = 0^\circ$.

Figure 5. Base pressure contours for $\alpha = 2^\circ$.

Figure 6. Base pressure contours for $\alpha = 4^\circ$.

Figure 7. Base pressure contours for $\alpha = 6^\circ$

Figure 8. Base pressure contours for $\alpha = 8^\circ$

B. Base Pressure Coefficient Variation

With the individual base pressure coefficients determined, the overall base pressure coefficient may be determined in terms of an area weighted summation of the individual base pressure coefficients. This may be expressed as:

$$C_{p,b} = \sum_{i=1}^n C_{p,i} \frac{A_i}{A_{base}} \quad (2)$$

where n is the number of base pressure ports, A_i is the effective area of the i^{th} port, and A_{base} is model base area (approximately 8.00 in^2).

The effective area for the i^{th} pressure port is approximated directly from Fig. 9. Using the geometric dimensions of the model base, the entire base area may be divided into annular segments, with the exception of the central port, which has a circular base area. All variables in Eq. (2) are now known and may be summed over the base area to give the overall base pressure coefficient.

Figure 9. Schematic of base area approximation.

This methodology is then repeated for each angle-of-attack orientation of the missile model to yield the overall base pressure coefficient as a function of α . The results of this analysis are shown in Fig. 10. Note that the variation of the overall base pressure coefficient with angle-of-attack bears a striking resemblance to the “drag-bucket” characteristic of many laminar-flow airfoils.

It is important to keep in mind that the flow interference arising from the “sting” mount is expected to be significant at high angles-of-attack. However, the primary concern here is the relative effects of afterbody

geometries on the overall base pressure coefficient. Thus, in the relative sense, the “sting” interference may be taken as a tare value without further correction of the gathered experimental data.

Figure 10. Overall base pressure coefficient variation with angle-of-attack α .

C. Tuft Flow Visualization

Tuft flow visualization was employed to gain further insight into the missile body flow field. The tufts do not show any indication of flow separation. Sample photographs of the “tuft” visualization tests are shown in Figs 11 and 12. For all angles of attack considered, (i.e., ± 8 deg.), the tufts were all pointed downstream with a small amount of fluttering. The only tufts that showed some fluttering were the first two immediately downstream of the “sting” mount – indicating the suspected perturbing wake discussed in Section VI. The absence of flow separation was not surprising due to the small range of angle of attack.. References 2 and 3 indicate that separation occurs at angles of attack of 25-30 deg for slender missiles of square cross-section, beyond the present test range.

Figure 11. Tuft visualization for $\alpha=0^\circ$ (flow direction from right to left).

Figure 12. Tuft visualization for $\alpha=7^\circ$ (flow direction from right to left).

VI. Numerical Analysis

The software package ANSYS CFX was used to supplement the experimental data. The simulation was run on a Pentium 4HT, 3.39 GHz processor with 3.25 gigabytes of SDDR RAM. Each run yielded the same results for the pressure, velocities and turbulence. The refined mesh, with over 400,000 elements, was used to construct the figures. The outer control volume, which included the test section inlet, outlet and walls, measured 24 in. high, 24 in. wide and 72 in. long. The slender body geometry used in the analysis was imported into ANSYS Workbench from Pro/ENGINEER solid modeler with all of the same dimensions as the experimental model. A tetrahedral mesh

for the surface and volume elements was generated through CFX-Mesh. Boundary conditions were set for the model in CFX-Pre. Inlet velocity conditions were set to 100 deg. F, with a unidirectional velocity of 100 mph (146.67 ft/s) and a reference pressure of 1 atm (406.78 in.H₂O). The outlet back pressure was set to zero to ensure no upstream disturbances. The walls of the wind tunnel were set to free-slip conditions to negate near-field effects, such as wall turbulence. In addition, the surrounding enclosure walls were set to an isothermal, adiabatic surface of 100 deg. F. The surface of the missile was set to a no-slip condition.

The fluid domain used for the simulation was incompressible and isothermal. A standard $k-\omega$ turbulence model was selected. All other default solver values were retained. Once the model is fully constrained, the solver is initiated. The actual time to solve the flow field depended directly upon the number of elements in the mesh. The results and figures shown are extracted from the model with the highest number of elements, in order to ensure that boundary layer flow over the model is accurately represented.

A plane is cut down the center of the model to show the pressure, velocity and turbulence of the model. The base pressures shown in Fig. 13 are local to the geometry. The computed body pressures are used to find the base pressure coefficient. Figures 14-16 shows the turbulence eddy frequencies, pressures and velocities localized at the base area of the missile.

Figure 13. Computational domain with square cross-section model.

Figure 14. Turbulence contour plot.

Figure 15. Pressure contour plot.

Figure 16. Velocity contour plot.

The initial computational simulations show similarities between the wind tunnel data and tuft flow visualization. Estimated $C_{p,i}$ values from the streamlines in Fig. 15 roughly correspond measurements from wind tunnel testing. However, exact data point locations corresponding to the pressure ports on the base and boattail wind tunnel model were not measured.

VII. Conclusions

The variation of base pressure distribution and overall base pressure ratio with angle-of-attack have been presented for a baseline missile body of square cross-section. The primary variable of interest, the overall base pressure ratio, has been shown to be a strong function of the model angle-of-attack. Additionally, preliminary CFD analysis has been offered for the purposes of validating the collected experimental data.

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