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Simulation of Jet Plume Interference Effects During the Launch Phase of Missiles

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The firing of rockets from launchers is accompanied by strong jet-wall interactions. So-called non-tip launchers, which have changes in cross-sectional areas, produce especially complicated flow patterns with massive blow-by over the missile body suspected to cause initial trajectory degradation. A comprehensive analysis for this launch phase is presented which delineates component flow mechanisms, subsequently integrating them into an overall model with the solution obtained by a convergent process of iterations. Since the analysis is free of empirical input, it provides a firm basis for comparison with available experimental data. Quantitative agreement between theoretical and experimental results not only supports the correctness of the overall physical model and its mathematical simulation, but also allows the designer to explore the effects of individual design and operational parameters.

Nomenclature

A	= area, m^2
D	= diameter, m
e	= absolute roughness, m
f	= friction factor
k	= specific heat ratio
L_2	= length of fore-tube, m
L_3	= length of aft-tube, m
$\dot{m}$	= mass flow rate, kg/s
M	= Mach number
p	= absolute pressure, N/m^2
R	= gas constant, Nm/(kg-K)
Re_{D_e}	= Reynolds number, based on equivalent diameter D_e
T	= absolute temperature, K
x	= length coordinate, launcher
x_0	= instantaneous location of missile base during launch

Subscripts (see Fig. 3)

b	= base of missile
B	= blow-by (cross section ①)
E	= nozzle exit
e	= equivalent
N	= nozzle
v	= vent port
0	= stagnation condition
1	= at missile base (nozzle exit)
2	= one-dimensional subsonic flow, upstream of vent ports
$2'$	= one-dimensional subsonic flow, downstream of vent ports
3	= aft-tube entrance
4	= exit, aft-tube
5	= exit, front-tube

Superscript

$(\ )^*$	= throat condition, rocket nozzle

Gas Dynamics Functions (see Ref. 10)

Isentropic: $A/A^*(k,M)$; $p/p_0(k,M)$.
Simple adiabatic: $T/T_0(k,M)$, $F/F^*(K,M)$; $p/p^*(k,M)$, $fL_{\max}/D(L,M)$, FANNO; $p_y/p_x(k,M)$, normal shock.

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Introduction

THE firing of rockets from launch tubes is accompanied by strong jet plume-wall interactions. So-called "non-tip-off" launch tubes, having discontinuities in cross-sectional areas, exhibit rather complex flow patterns often resulting in massive blow-by as the rocket exhaust gases encounter area restrictions.[1-3] As the rocket advances through and exits the launcher, three regions of jet-wall interaction can be identified, as shown schematically in Fig. 1. As long as the propulsive afterbody is fully supported in the aft-tube, the flow pattern is well understood and amenable to theoretical analysis, even though quantitative uncertainties related to the fully supersonic base pressure problems still persist[4,5] and are subject to continuous reviews,[6,7] semiempirical corrections,[4] and searching experimentation including geometrical and dynamic plume[8,9] modeling.

When the afterbody has cleared the smaller diameter aft-tube, the constrictive influence of the area reduction together with the opening of a potential blow-by passage between the rocket and the larger forward tube results in a flow pattern involving rather complicated viscid-inviscid interactions and shock patterns.

While qualitative models have been proposed[1] and some quantitative studies have been carried out to explore various anticipated aspects of inviscid and viscid choking phenomena,[2] no comprehensive analytical model has yet been advanced.

The main objective of the present paper is the establishment of an overall model for the configuration shown in Fig. 2, synthesized from well-understood flow components completely free of empirical input, and the critical evaluation of its merits by comparison with experimental data.

In this manner, a better understanding of geometrical and operational parameters shall be gained and favorable design

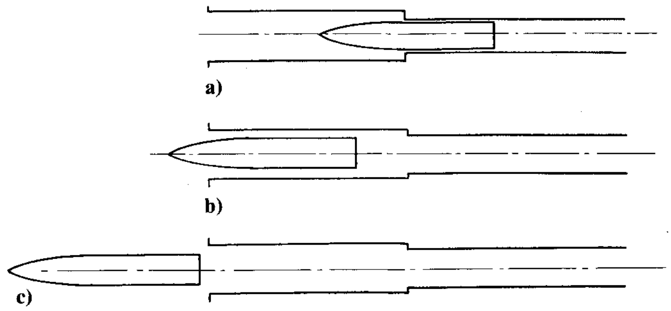

Fig. 1 Plume-wall interaction phases during missile launch from a constrictive non-tip-off tube: a) rocket exhaust into cylindrical aft-tube; b) rocket exhaust interacting with tube constriction and missile body (blow-by); c) plume-launcher interaction after missile exit from front-tube.

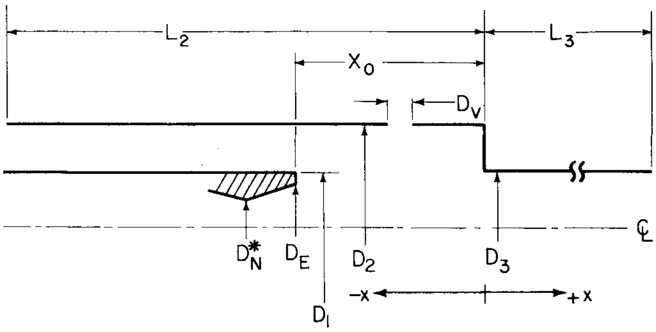

Fig. 2 Geometry and principal dimensions for a missile in a non-tip-off launcher tube.

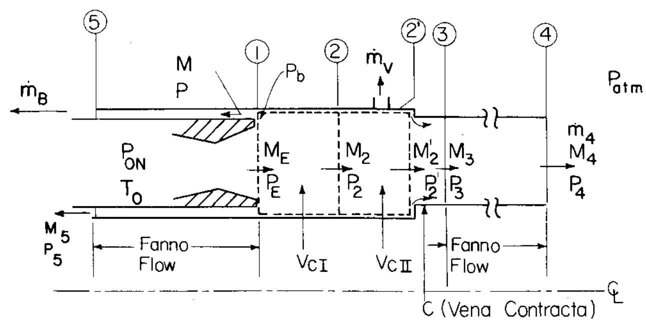

Fig. 3 Flow model components and control volume definitions for analysis of launch phase according to Fig. 1b.

criteria established, particularly aimed at eliminating or minimizing misalignment problems resulting from blow-by flow over the missile.

Analysis

Some surprisingly simple assumptions can be extracted from a large body of experimental evidence: 1) that quasi-steady mechanisms dominate, and 2) that the interaction mechanism in the forward-tube becomes established within a few nozzle exit diameters as the afterbody alights from the aftertube.[3] Only one further assumption is required for the following analysis—namely, that the very complex inviscid-viscid interaction mechanism upstream of the constrictive diameter change results in subsonic flow which, due to the rather low level of dynamic pressure variations, can be treated as one-dimensional.[4] The remaining components of the overall flow model are conventional throughout and are linked together by the outflow conditions from the control volume V_{CI} depicted in Fig. 3.

Specialization of the continuity equation together with the energy equation for simple adiabatic flow (T_0 = const) yields

$$A_N^*[2/(k+1)]^{(k+1)/2(k-1)}$$
$$= (p_2/p_{0N})A_2M_2\sqrt{1+[(k-1)/2]M_2^2}$$
$$+ (p_2/p_{0N})(A_2-A_1)M_B\sqrt{1+[(k-1)/2]M_B^2} \qquad (1)$$

The momentum equation together with the utilization of well-known compressible flow functions[10] result in an expression for the base pressure ratio

$$p_b/p_{0N} = z_1/z_2 \qquad (2)$$

where

$$z_1 = F/F^*|_{M_2} - F/F^*|_{M_E}$$
$$z_2 = \tfrac{1}{2}[(k+1)/2]^{1/(k-1)}\{[(A_2-A_1)/A_N^*]$$
$$\times[1+(k-1)M_B^2/2]^{-k/(k-1)}[1+kM_B^2] + [(A_1-A_E)/A_N^*]\}$$
$$+F/F^*|_{M_2}[(A_2-A_1)/A_N^*]/(A/A^*)|_{M_B}$$

which becomes the main parameter for component matching and, by itself, is the controlling pressure level for blow-by flow. For any given value of p_b/p_{0N}, one can determine

$$p_2/p_{0N} = z_3/z_4 \qquad (3)$$

where

$$z_3 = 2[2/(k+1)]^{1/(k-1)}F/F^*|_{M_E}$$
$$+(p_b/p_{0N})\{[(A_2-A_1)/A_N^*][1+(k-1)M_B^2/2]^{-k/(k-1)}$$
$$\times(1+kM_B^2) + [(A_1-A_2)/A_N^*]\}$$

and

$$z_4 = (A_2/A_N^*)(1+kM_2^2)$$

so that the analysis can proceed downstream.

If vent holes are provided in the front-tube, in close proximity of the constriction, a one-dimensional analysis can be applied to the control volume V_{CII} (see Fig. 3). Critical outflow through an effective area A_v perpendicular to the x-direction is assumed, yielding from a combination of the continuity, energy, and momentum equations,

$$F/F^*|_{M_2} = F/F^*|_{M_2'}/z_5 \qquad (4)$$

where

$$z_5 = 1+\{(A_v/A_2)[2/(k+1)]^{(k+1)/2(k-1)}$$
$$\times\sqrt{2(k+1)}F/F^*|_{M_2'}\}/[1+kM_2^2]$$

(an implicit equation for M_2 for given M_2'). In the absence of vent holes, $A_v = 0$ and $M_2 = M_2'$.

Depending upon the shoulder geometry and the effect of the shock pattern and/or adverse pressure gradient along the upstream wall, the flow entering the aft-tube may be subject to a contraction decreasing the effective one-dimensional entrance cross section (vena contracta). While exact solutions for such contractions can be derived for potential flow by means of hodograph methods, it is evident that the actual approach and downstream conditions preclude the usefulness of such concepts. A parametric study of contracting flows by one-dimensional compressible flow modeling suggests that even the lower limit (producing initial conditions in a "free sonic throat") will not result in a substantial modification of both Mach number and stagnation pressure as the entrance condition for the aft-tube flow component, so that an isentropic relation may be assumed to relate the flow conditions in cross sections 2′ and 3. (It should, however, be noted that the presence of a vena contracta is indicated by experimental data showing a localized pressure drop just downstream of the entrance cross section in the constriction.)

Based on the preceding reasoning, entrance into the front-tube can then also be considered to be isentropic, generated by a stagnation condition p_b, T_0 prevailing near the nozzle base.

Flows through the aft-tube as well as in the (generally eccentric) annular region between missile body and launcher fore-tube shall be treated as FANNO line problems.[10] This is certainly an approach suggested by convenience which can, however, be justified by close scrutiny of experimental data obtained with flows individually discharged through either one of the conduits. Use of the functional relationship between entrance and exit Mach numbers is expressed with the help of the auxiliary function $[fL_{max}/D_e]$, where D_e is the equivalent passage diameter ($D_e = D_3$ for the aft-tube and $D_e = D_2 - D_1$ for the fore-tube).[10] The friction factor

$$f = f[Re_{D_e},\quad e/D_e]$$

can be calculated from Colebrook's equation[11] (small changes in Reynolds number along the passages not being significant):

$$1/f^{1/2} = -0.86\, \ln(e/3.7\, D_e) + 2.51/(Re_{D_e} f^{1/2}) \qquad (5)$$

Use of this (implicit) equation, originally derived for circular pipes and incompressible flow, is justified in view of the limited effects of annular eccentricity (fore-tube)[12] and the prevailing of subsonic flow in most of both passages. More questionable is the assumption of fully developed turbulent flow in the aft-tube; yet, experimental evidence seems to support such a treatment. Pressure distributions along FANNO lines can be evaluated with the help of the auxiliary functions $[(fL)/D_e](k,M)$ determining location and $(p/p^*)(k,M)$ determining the local pressure levels.

Outflow conditions for each exit will be given by

$$M_{exit} = 1 \quad \text{if} \quad p_{exit}/p_{atm} > 1$$

or

$$p_{exit}/p_{atm} = 1 \quad \text{if} \quad M_{exit} < 1$$

The individual flow model components are linked together, and a unique solution for the entire system can be found by an iterative computational procedure following this brief outline: Assuming first critical outflow from both fore- and aft-tubes, and choosing a reasonable value for the friction coefficients, one finds, for a given geometry, first values for M_B and M_3. This, in turn, allows one to find M_2' and in absence of, or ignoring initially, the flow through the vent ports, M_2. Consequently, p_b/p_{0N} can be found from Eq. (3), and one now has to carry the calculations downstream (along both aft- and fore-tubes) having established first approximations for pressure levels and temperature levels so that Reynolds numbers and friction factors can be found; thus, flow rates through aft- and fore-tubes and vent ports can be determined. Exit conditions for both fore- and aft-tubes will now be examined and the entire procedure repeated until subsequent iterations for p_b/p_{0N} agree within a prescribed limit. Obviously, such calculations necessitate the use of digital computers. Programs have been developed in basic language for both the HP9830 desk calculator and the Cyber 175 (see Table 1).

Table 1 Input-output and printout for program NONTIP, performed on a Cyber 175 at the University of Illinois OCS (operational condition also identified in Fig. 6)

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LAUNCHER FLOW ANALYSIS, ONE DIMENSIONAL, HHK 5/4/79
AIR, YES OR NO= ? YES

LAUNCHER GEOMETRY, DIMENSIONS IN METERS

DE= .0287 DN*= .0193 D1= .03241 D2= .04445 D3= .03429
L2= .3871 L3= .4585 ROUGHNESS= 1.50000E-6

OPERATIONAL PARAMETERS

ENTER VENT DIAMETER(METERS)
? .017
VENT DIAMETER= .017
ME= 2.30909
P,B/P,0, NORMAL SHOCK= .477306
ENTER BASE LOCATION, XO= ? .28413
BASE LOCATION= .28413 XO/DE=-9.9
ENTER PO(KPA),TO(DEG.CELSIUS),P9(KPA),ATM.= ? 9660,25,100
STAGNATION PRESSURE= 9660 (KPA)STAGN. TEMP.= 298.15 (K)
ATMOSPHERIC PRESSURE (KPA)= 100


ITERATION #    P,B(PA)     M4       P4(PA)      M5      P5(PA)   M2
        0.    4610781.   1.0000          0.   1.0000        0.  .0000
        1.     201032.   1.0000    1252045.    .9856    98322.  .4447
        2.     146530.   1.0000    1281675.    .7537    73848.  .4649
        3.     150913.   1.0000    1282398.    .7434    98495.  .4650
        4.     151365.   1.0000    1282471.    .7423    99843.  .4650

FINAL SOLUTION

PRESSURES (PA)
BASE PRESSURE= 151413. (PA)
SHOULDER PRESSURE= 2.48413E+6 P2= 2.14207E+6
EXIT PRESSURES, FANNO (COMPARE TO ATMOSPHERIC)
EXIT, FRONT= 99983.2 M= .742348 LOC.=-13.4878
EXIT, REAR= 1.28248E+6 M= 1 LOC.= 15.9756

PRESSURE RATIOS

  P,B/PO     P2/PO      PO,2/PO    P5/PO      P4/PO
  .0157      .2217      .2572      .0104      .1328

FRONT TUBE FANNO FLOW,REYNOLDS#= 220132. FR. COEF.= 1.66508E-2
  X/DE              MACH#          P/PO
   -9.9000          .6670          .0116
  -10.4280          .6758          .0115
  -10.9561          .6852          .0113
  -11.4841          .6953          .0111
  -12.0122          .7062          .0109
  -12.5402          .7181          .0107
  -13.0683          .7310          .0105

REAR TUBE FANNO FLOW,REYNOLDS#= 1.14177E+7 FR. COEF.= 1.08154E-2
   X/DE             MACH #         P/PO
    .0000           .7376          .1873
   1.5378           .7475          .1845
   3.0756           .7581          .1817
   4.6135           .7698          .1786
   6.1513           .7826          .1754
   7.6891           .7970          .1719
   9.2269           .8133          .1681
  10.7648           .8324          .1637
  12.3026           .8556          .1588
  13.8404           .8862          .1526
  15.3782           .9366          .1432

MASS FLOW RATES

BLOW-BY MASS RATIO= .034755
VENT MASS RATIO= .172044
REAR TUBE MASS RATIO= .793282
MASS FLOW THROUGH ROCKET NOZZLE= 6.6152 (KG/SEC)
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Comparison of Theory with Experiment

Studied here exclusively, due to its importance for the quality of the missile trajectory, is the blow-by phase (Fig. 1b),§ for which a wealth of experimental data for a variety of launch-missile geometries and operating conditions is

§Analytical and experimental studies concerning the operating conditions for the initial launch phase depicted in Fig. 1a are reported in Ref. 9.

Table 2 Launcher configurations and propellants used in analysis

Configuration geometry[a], m	I Current investigation		II Bertin, two-scale model[3]	III Bertin, Batson[5]
D_N^*	0.009347		0.01930	0.0785
D_E	0.010262		0.02870	0.1439
D_1	0.015875		0.03241	0.1520
D_2	0.018593		0.04445	0.2223
D_3	0.015875		0.03429	0.1715
L_2	0.19050		0.3871	1.7411
L_3	0.25400		0.4585	2.033
Propellants	Air	Argon	Air	Hot propellant[b]
k	1.4	1.667	1.4	1.18
R,kJ/kg – K	287	208.13	287	395.81
NO[c] $\times 10^{-5}$,N-s-m^{-2}	1.724	2.101	1.724	1.29
SO[c],K	110.0	135.14[d]	110.0	237.2
T_0,°C	~25°C	~25°C	~25°C	~2500°C

[a] See Fig. 2.
[b] Values adapted from Ref. 13 for illustrative purposes only.
[c] NO and SO are constants used in Sutherland's equation for dynamic viscosity: μ[Ns/m^2] = NO·SQR(T/273)(1 + SO/273)/(1 + SO/T).
[d] Calculated from Lennard-Jones (third-order) potential.

available.[14,15] In addition, a series of supporting studies has been conducted addressing itself to both the general problem of evaluating the merits of the theoretical model and the investigation under controlled conditions of the relation of operational characteristics obtained with dissimilar propellants.[8,9]

Shown in Table 2 is a summary of cases (launcher geometries and propellant specifications) which have been selected for theoretical analysis on the basis of well-documented experimental data.

First, some general qualitative observations are in order. From detailed wall pressure measurements,[14,15] it can be noted that the transition between the launch phases (Figs. 1a, b) is essentially monotonous and occurs generally within a distance of two rocket nozzle exit radii upstream from the tube constriction. Also, even for missile locations nearing front-tube end, there is no noticeable pressure drop along the launcher wall upstream of the constriction once the recompression process had produced subsonic flow. This is both opportune for the analysis which does not account for wall friction in control volume V_{CI}, but also is understandable inasmuch as the flow along the wall due to the initial presence of unfavorable pressure gradients will not produce significant levels of wall shear forces.

Shown in Fig. 4 is a comparison between experimentally determined pressure distributions (Figure 11g of Ref. 3) with the theoretical results obtained on the Cyber 175 computer without introduction of any empirical coefficients. As can be seen, the quantitative agreement is quite good for this case with a rocket nozzle exit position – 8.41 nozzle radii upstream of the cross-sectional constriction. The relative insensitivity of pressure ratios to absolute nozzle stagnation pressure levels is also borne out by the computer solution. The existence of a vena contracta is evident downstream of the constriction from the experimental data, but quick recovery to FANNO-line type flow is also observed.

Next, a comparison between calculated and experimentally determined[3] blow-by mass flow rates is given in Fig. 5. Both the level and dependency upon nozzle stagnation pressure show good agreement. By using the computer option for vent port effectiveness, the predicted blow-by rates for different "equivalent diameter" vent ports are also shown.

The presence or elimination of blow-by flow obviously depends on the base pressure level compared to the atmospheric pressure; for $p_b > p_{atm}$, blow-by will occur, while for $p_b < p_{atm}$, the launcher will operate as an ejector (negative blow-by). (The latter mode has not been included in the present computer program.)

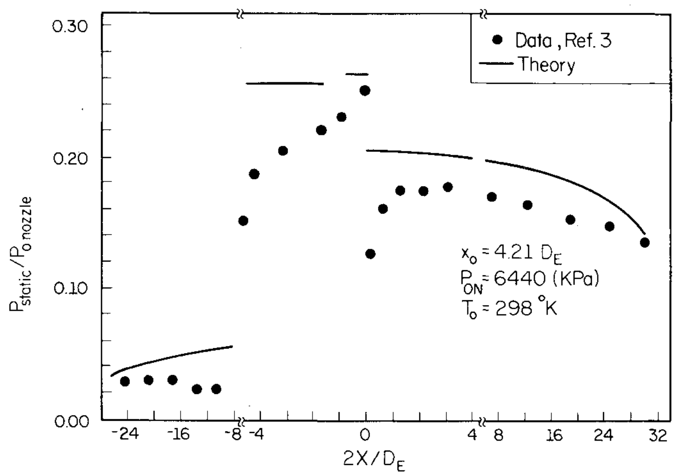

Fig. 4 Experimental and theoretical pressure distributions for a constrictive tube launcher, configuration II of Table 2.

Since blow-by rates increase both as a fraction of the nozzle mass flow and in an absolute sense with increasing nozzle stagnation pressure, the effectiveness of relief measures can best be illustrated for the highest experimental pressure levels. According to the theory, for a nozzle stagnation pressure of 9.66 MPa, an equivalent vent port diameter of 0.019 m will be needed to fully eliminate blow-by, while an equivalent diameter of 0.0142 m is required to reach subcritical outflow conditions (see Fig. 6).

As an alternative to vent ports, an increase in aft-tube diameter can also be considered as a means for controlling blow-by.[2] Shown in Fig. 7 is the result of theoretical calculations for the geometry II of Table 2, representing a parametric study on the effect of aft-tube diameter increase. The theory predicts elimination of (positive) blow-by for aft-tube diameters in excess of 0.79 area ratio A_3/A_2. This agrees well with the predictions[3] and the experimental evidence presented by Batson.[5]

A limited study on vent port effects was also conducted for configuration I of Table 2. The theoretical pressure ratio distributions are compared with experimental data and confirm the relatively weak dependency observed earlier.[14] This, however, does not reflect on the effectiveness of vent ports since it is the relatively low absolute base pressure level which controls blow-by, while the high and sustained pressure levels upstream of the constriction determine the by-pass rate (see Fig. 8).

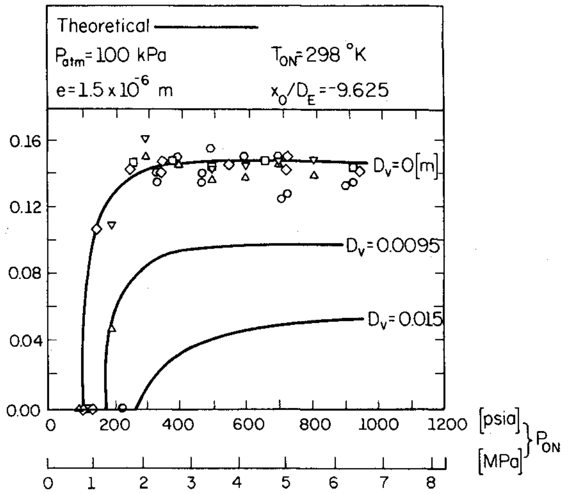

Fig. 5 Theoretically and experimentally determined blow-by mass flow rates for a constrictive tube launcher, configuration II of Table 2.

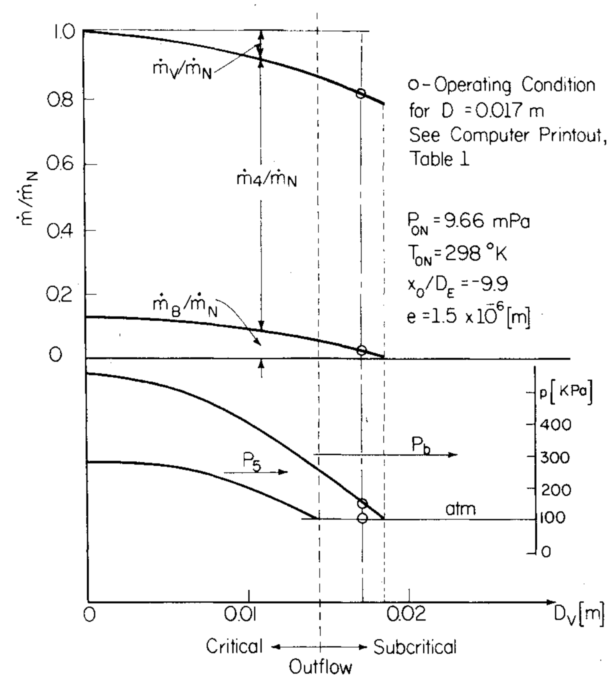

Fig. 6 Reduction and elimination of blow-by through vent ports, configuration II of Table 2.

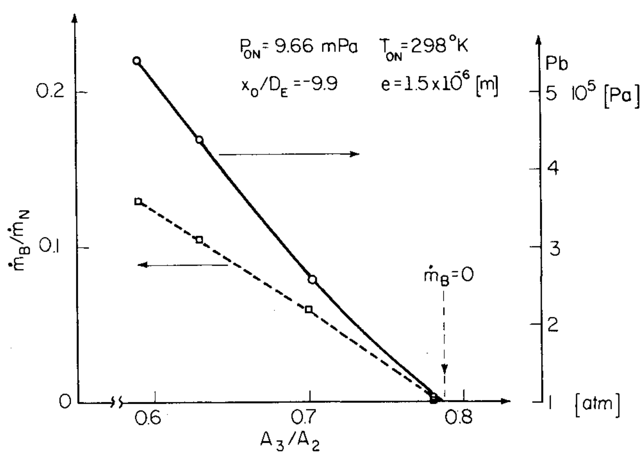

Fig. 7 Effect of aft-tube diameter increase on blow-by rate, configuration II of Table 2.

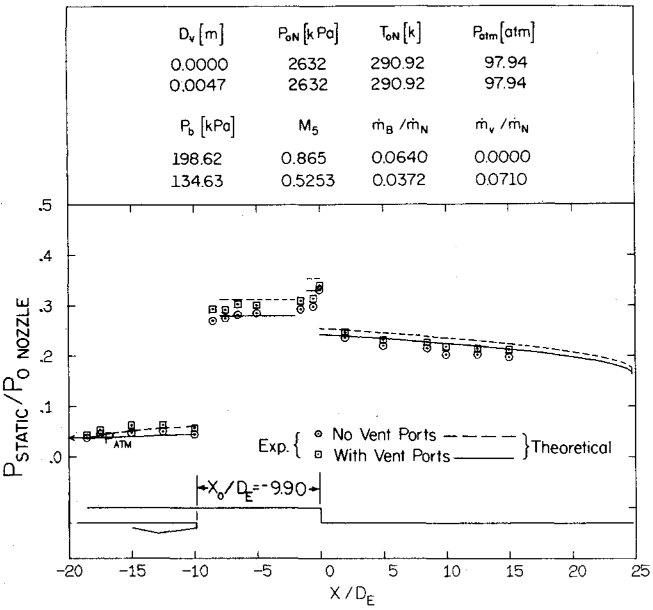

Fig. 8 Pressure distributions and blow-by rates for a low Mach number highly underexpanded rocket nozzle in a constrictive tube launcher, configuration I of Table 2.

It has been suggested[2,14] and is supported by limited data obtained in free-flight testing[14] that the simulation of hot propellants by cold air flow experiments not only offers qualitative information but also shows reasonable quantitative correlation of pressure ratios. Implied in this statement is the retention of all geometrical parameters, except a scale factor,[14] and in addition the practical insensitivity with respect to the specific heat ratio. Obviously, the quasi-steady analysis does not account for the acceleration of the propellant gas; however, it can be shown that the effects of nonsteady gas motion are unimportant at least for the initial stages of the launch process. By contrast, one expects that differences caused by the use of dissimilar gases and scale effects have to be carefully examined. This requires the study of the influence of specific heat ratio and Reynolds number dependency.

Since it is easy to explore the isolated parametric influence of the specific heat ratio (retaining gas constant and viscosity coefficients for air) by using the computer program, a sweeping appraisal of this factor—extending to k values near unity—was carried out; it showed no appreciable effects for either pressure distributions or blow-by mass rate ratios.

The availability of an argon-blowdown facility made it attractive to conduct experimentation to clarify the influence of the specific heat ratio while practically eliminating scale (Reynolds number) effects (see Table 2).

Again, comparison between experimental and theoretical pressure levels showed excellent agreement (see Fig. 9). Even though the nozzle exit Mach numbers for air and for argon were different and all gas dynamics functions appearing in the analysis are affected by the specific heat ratios, their respective (dimensionless) pressure levels correlated closely (see Fig. 9).

Simulation of hot air runs and surface roughness influence was again carried out with the help of the computer programs. Stagnation temperatures ranging from 25 to 2000°C for the RIP-ZAP configuration III of Table 2, at a pressure level of 9660 kPa, showed only insignificant increases in pressure level and blow-by mass rate fractions with increasing temperatures. Changes in absolute roughness parameters were somewhat more noticeable, as shown in Fig. 10 where theoretical pressure distribution (for configuration I of Table 2) is presented as compared to experimental data for a nozzle exit location of $x/D_E = x_0/D_E = -7.44$. The theoretical

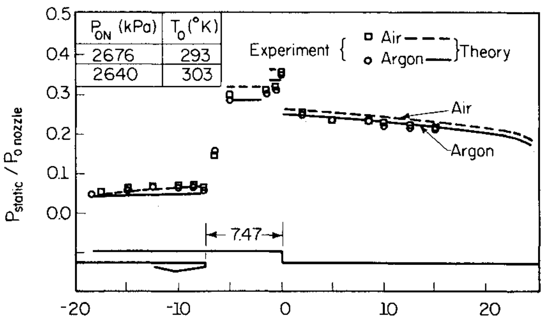

Fig. 9 Results obtained with air and argon as propellants, configuration I of Table 2.

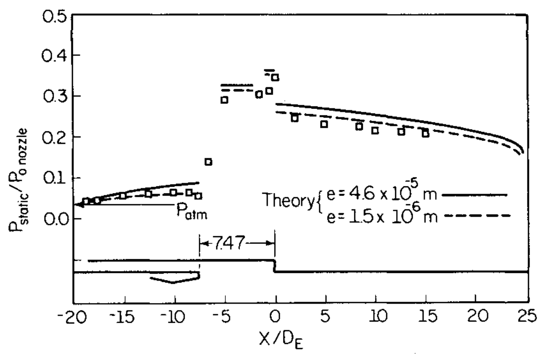

Fig. 10 Influence of absolute launch tube roughness, configuration I of Table 2.

curves for an absolute roughness $e = 1.5 \times 10^{-6}$ m (drawn tubing) show good agreement with the experimental points, while the analytical results for $e = 4.6 \times 10^{-5}$ m (commercial steel) fall noticeably above. (The model used had a smooth finish on all surfaces exposed to exhaust flow.)

Bertin and Batson[14] presented and discussed experimental data from a limited number of full-scale RIP-ZAP rocket launchings and made comparisons to their own[3,15] complementary cold-gas test programs. (The principal geometrical parameters are summarized as configuration III in Table 2.)

Of special interest are their references to the effects of constriction geometry changes (ramps) and vent ports. That modifications of originally sharp-edged tube constrictions were not of significance agrees well with calculations on the limited influence of the vena contracta formation on the following, friction-choking controlled, FANNO line flow in the aft-tube. Computer simulation, while confirming that pressure levels upstream of the constriction are not strongly altered suggest, however, that vent ports should provide an effective means for controlling and eliminating blow-by flows. Indeed, retention of high pressure levels just upstream of the tube constriction (where port holes are to be located) together with their substantial influence on the base pressure level (which controls blow-by) should explain their potential as a practical design feature. A parametric computer analysis for different effective vent port diameters, while keeping other parameters constant, yielded the results shown in Fig. 11. It can be seen that an equivalent hole diameter of 0.070 m could eliminate blow-by entirely, while values larger than 0.034 m reduce blow-by to subcritical outflow Mach numbers. For actual vent port arrangements, one would interpret the equivalent diameter as corresponding to the combined cross-sectional areas of all vent ports, considering their individual area contraction coefficients for critical outflow.

Direct comparison of these results with full-scale RIP-ZAP experimental data[14] is, however, not completely satisfactory.

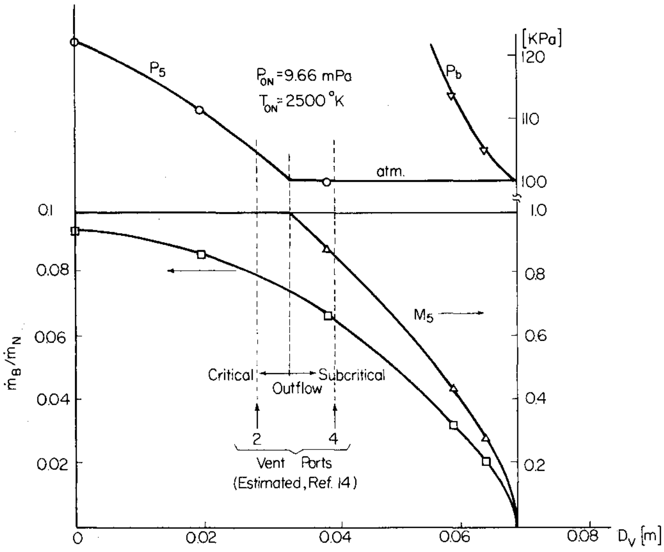

Fig. 11 Analytical evaluation of vent port effectiveness in controlling or eliminating blow-by for full-scale RIP-ZAP launchers, configuration III of Table 2.

Particularly, it appears from the sparse information on operation with vent ports (flights 9 and 11 in Ref. 14) that pressure levels in the aft-tube of the constrictive launcher are consistently lower than theoretical calculations predict. Such discrepancies should not be dismissed as isolated disagreement due to experimental uncertainties (which certainly may have contributed to the problem and which are also reflected by wide scattering of data and possible inaccuracies in assessing rocket chamber pressures), but should be examined critically by extending the present analytical method to include nonsteady concepts. Such analytical approaches have been developed for dealing with the internal ballistics and dynamics of rocket motor operation[16] and for similar efforts[17] which had to take into account the time-dependency of events affecting mainly the processes in control volume V_{CI} of Fig. 3.

Conclusions

The development of an analytical model for missile firings from non-tip-off launchers with area constrictions allows the identification and systematic study of performance parameters. Pressure levels, exhaust mass flow rates, and in particular, blow-by and vent port fractions can be obtained without the use of empirical input. Comparisons with small-scale steady-state experiments as well as with the results of extensive experimental programs show excellent quantitative agreement, confirming the physical correctness of the assumptions made in defining flow components and their integration into the overall system concept. The theoretical analysis supported by computer programs, therefore, is giving weight to both qualitative and quantitative conclusions how to control or fully alleviate blow-by related contributions to missile trajectory degradation during launch from non-tip-off tubes.

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