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Space Interceptors: An Investigation of Seven Main Parameters

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Nomenclature

D = incident dose of nuclear radiation, rad
F = interceptor thrust, lb
g = acceleration of gravity, naut miles/sec^2
I = interceptor specific impulse, sec
k = warhead radiation flux coefficient, rad-naut miles^2/megaton
m = warhead yield coefficient, megatons/lb
R = radius of intercept $(r + x)$, naut miles
r = warhead kill radius, naut miles
t = time to intercept, sec
V = intercept volume, naut miles^3
W = $W(t) = w_i + w_w + w_p(t)$, interceptor gross weight, lb
w = weight, lb
X_{T_t} = distance of target from origin of interceptor's flight at intercept time
x = straight line distance travelled by interceptor, naut miles
y = mw = warhead yield, megatons
ρ = area density of target shielding (lb/ft^2)
λ = interceptor inert factor, $w_i/(w_i + w_p)$

Subscripts

b = condition at burnout time
i = interceptor inert components
N = neutron radiation
p, w = interceptor propellant and warhead, respectively
t, 0 = conditions at times t and $t = 0$, respectively
γ = gamma radiation

Introduction

The effectiveness of an interceptor can be measured by the volume within which it can perform intercept in a given time. This "reach" is in general made up of two components: 1) the destructive mechanism of the warhead, which establishes a kill-radius r about the interceptor, and 2) the propulsive maneuvering capability by which the interceptor moves to the target vicinity. In a gravity field, both the paths of the interceptor and the intercept volumes generated by them can become geometrically complex, especially for long intercept times.[1] Even so, useful parametric trends can be obtained from an analysis which assumes gravity-free space. In this case, the interceptor "reach" is merely $R = r + x$, and the intercept volume is the sphere generated by R. Intercept is assumed to occur if $X_{T_t} \leq R$. This simplified analysis allows direct investigation of the relationships among seven key parameters: 1) intercept volume V; 2) intercept time t; 3) r; 4) interceptor gross weight W_0; 5) propellant weight w_p; 6) specific impulse I; and 7) thrust level F.

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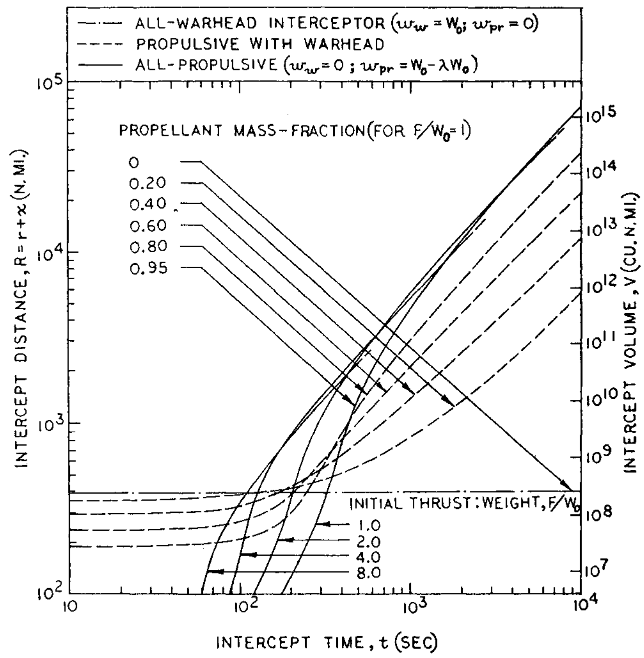

Fig. 1 Oxygen-hydrogen interceptors: intercept distance and volume vs intercept time for various warhead-propulsion combinations, and thrust: weight ratios (see Table 1 for assumptions).

Analysis

For straight-line flight through gravity-free space,

$$\dot{x}_{(t \leq t_b)} = gI \ln W_0/W_t, \; W_t = W_0 - (F/I)t \tag{1}$$

and integration gives

$$x_{(t \leq t_b)} = gI[(t - W_0I/F) \ln W_0/W_t + t] \tag{2}$$

At burnout, $t_b = w_pI/F$, and $W_t = W_0 - w_p$.

After burnout, the interceptor continues on at the burnout velocity, $\dot{x}_{tb}$, so that

$$x_{(t \geq t_b)} = x_{tb} + \dot{x}_{tb}(t - t_b) \tag{3}$$

$$= gI\{(t - W_0I/F) \ln[W_0/(W_0 - w_p)] + w_pI/F\} \tag{3a}$$

Warhead kill radius

If a non-nuclear warhead is used, r is quite limited, probably to the order of a mile. If a nuclear radiation mechanism is used, r may be hundreds of miles. Certain data pertaining to lethal dosages of gamma and neutron radiation[2] will be used here to construct an illustrative expression for r against manned vehicles. Thus, from Ref. 2 (Sec. 8.101) the general expression for nuclear bursts in the atmosphere is

$$D \approx k(y/r^2)e^{-r/\mathrm{const}} \tag{4}$$

where the exponential factor accounts for atmospheric attenuation. In space, there is no attenuation, and substituting $y = mw_w$,

$$D \approx kmw_w/r^2 \tag{5}$$

and

$$r \approx \{(km/D)[W_0 - w_p/(1 - \lambda)]\}^{1/2} \tag{6}$$

To estimate k and m, we use Eq. (5) and the data given in

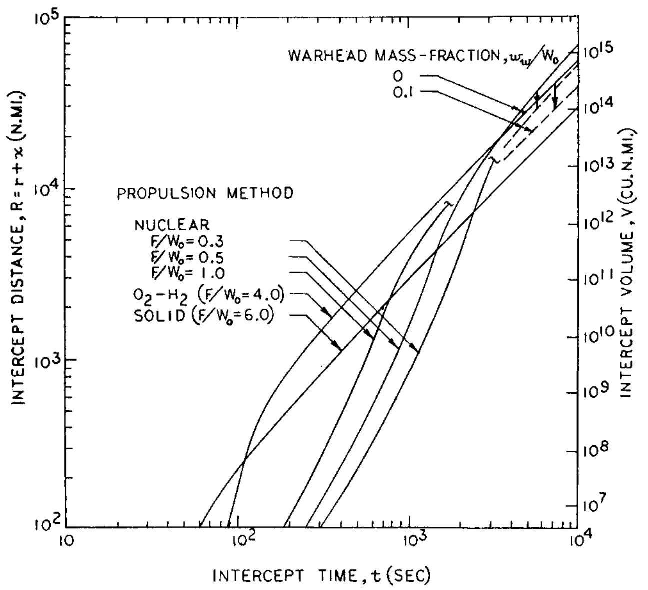

Fig. 2 Comparison of nuclear, hydrogen-oxygen and solid-propellant interceptors (see Table 1 for assumptions).

Secs. 8.101 and 8.104 (Ref. 1), to obtain

$$D_\gamma \approx k_\gamma m w_w / r^2 \text{ roentgens} \tag{7a}$$

$$D_N \approx k_N m w_w / r^2 \text{ neutrons/cm}^2 \tag{7b}$$

Accepting the recommended value of unity for both gamma and neutron relative biological effectiveness (Secs. 11.86 and 11.88), using the fission dose value of 1 neutron/cm² = 1.8×10^9 rad (Sec. 11.90), and assuming for present purposes that $D_{\gamma+N} = D_\gamma + D_N$, then

$$D_{\gamma+N} \approx (k_\gamma + 1.8 \times 10^9 k_N) m w_w / r^2 \tag{7c}$$

Inserting the data from Secs. 8.101 and 8.104 (after conversion to naut miles, megatons), we have

$$r \approx 2.13 \times 10^3 (m w_w / D_{\gamma+N})^{1/2} \tag{7d}$$

giving $k = 4.54 \times 10^6$ rad-(naut miles)²/megaton.

Indications of the possible magnitudes of m can be obtained from unofficial estimates such as the following Associated Press release of March 21, 1963, referring to a Titan III CBM payload: "The Air Force reports the warhead is twice as heavy as the nose cone carried by the operational Titan I, which would make it weigh around 8000 pounds. . . . Estimates of Titan II's nuclear delivery range up to 10 megatons, or 10 million tons of TNT." In this release it is not clear whether the 8000 lb refers to the nuclear warhead alone or includes the re-entry thermal shield and other subsystems, but if w_w is somewhat less than 8000 lb and/or we allow for a technological improvement in packaging, a reasonable assumption might be that $m = 0.002$ megaton/lb. Then $km = 9000$ rad-(naut miles)²/lb, and if an incident dose D of 600

Table 1 Interceptor characteristics assumed $km = 9000$ rad (naut miles)²/lb; $W_0 = 10{,}000$ lb

Propulsion	F/W_0	λ
Solid ($I = 300$ sec)	6	0.15
O_2/H_2 ($I = 450$ sec)	8	0.14
	4	0.09
	2	0.06
	1	0.05
Nuclear H_2 ($I = 900$ sec)	1	0.3
	0.5	0.2
	0.3	0.15

rad is lethal against an unshielded spacecraft (Ref. 2, Sec. 11.118), Eq. (6) gives

$$r \approx 3.9[W_0 - w_p/(1-\lambda)]^{1/2} \tag{8a}$$

For radiation-shielding for the manned spacecraft in question, let us assume the recommended value of $\rho \approx 200$ lb/ft² for tenth-value shielding against initial nuclear radiation (Sec. 8.37). A simple modification of Eq. (7d) gives

$$D_{\gamma+N} \approx 4.55 \times 10^6 (y/r^2) e^{-0.0116\rho} \tag{7}$$

which can be used to show that significant reduction of r can be brought about only by large weights of shielding, since even a two-man "storm cellar" can be expected to have at least 150 ft² of outside surface area which must be shielded. Thus, for example, to reduce r to one-tenth its shielded value, requires ~650 lb/ft² shielding which, for the above "storm cellar" would weigh 98,000 lb.

Intercept volume

The intercept volume is simply the sphere generated by $R = x + r$, or

$$V_{t \geq t_b} = \tfrac{4}{3}\pi(x_{t \geq t_b} + r)^3 \tag{8}$$

$$V_{t \leq t_b} = \tfrac{4}{3}\pi(x_{t \leq t_b} + r)^3 \tag{9}$$

into which we can substitute Eqs. (3a) and (8a) to plot $V(t)$ in Fig. 1, which is for O_2/H_2 interceptors having the characteristics shown in Table 1. A fixed W_0 of 10,000 lb is assumed. The curves in Fig. 1 show the effects of trading w_p against w_w within the limit of a fixed W_0 for $F/W_0 = 1$, and of varying F/W_0 for $w_w = 0$ (i.e., $w_p = 9500$ lb). For the assumptions taken, $r = 390$ naut miles when $w_p = 0$ (i.e., when all W_0 is allocated to warhead), and this decreases as w_p increases. For short intercept times, the maximum volume coverage is obtained by allocating all W_0 to the warhead, and the optimal allocation shifts toward the propulsion side as t increases, becoming all-propulsive as t exceeds ~500 sec. As F/W_0 increases from 1, more volume coverage can be obtained at earlier times, but progressively larger volume penalties are taken over longer times because of the higher inert weight of the additional engines and consequent lower coasting velocity. For an O_2/H_2 interceptor, it appears that the optimal F/W_0 might be near 4. To avoid confusion, the effects of varying w_p are not shown for each of the higher values of F/W_0, but the resulting curves would form patterns closely approximating those shown for $F/W_0 = 1$, but shifted toward the lower values of t related to each case.

The effects of varying D, k, m, and W_0 would all be reflected as merely a change in the level of the horizontal line representing maximum r; the (w_p vs w_w) patterns would essentially shift to maintain their relationship to this horizontal line, and the ($w_w = 0$) curve, which would remain unaffected. (Note that changes in W_0 produce only second-order effects in the $w_w = 0$ or all propulsive curve.)

Figure 2 compares the performances of an O_2/H_2 interceptor ($F/W_0 = 4$), a solid-propellant interceptor ($F/W_0 = 6$) and nuclear interceptors ($F/W_0 = 0.3$, 0.5 and 1.0). To avoid confusion, only all-propulsive ($w_w = 0$) curves are shown; the essential trade-offs can still be indicated in this way, and the effects of varying w_p are quite similar in the solid and nuclear cases to those already illustrated in Fig. 1 for the O_2/H_2 case. Although the high-acceleration solid offers slight gains for small t, the penalties paid for the relatively low I become very large as t increases. The nuclear interceptor, for which $F/W_0 = 0.5$ is near the optimum, is not competitive for early intercepts but becomes preferable to O_2/H_2 devices at longer times. This advantage is even more pronounced if, as shown by the dotted lines, some fixed warhead weight (here assumed to be 1000 lb) must be retained even in "all-propulsive" interceptors to allow for a proximity-type (perhaps non-nuclear) kill mechanism. Because of the smaller inert fraction of the O_2/H_2 vehicle, a given warhead weight reduces its performance more severely than that of a nuclear vehicle.

Observations and Concluding Remarks

When warhead kill radius r is several hundred miles or more as assumed here for nuclear radiation against a manned target, then there exist three regions in intercept time-distance ($t - X_{Tt}$) space, each of which calls for a different class of interceptor. When $X_{Tt} < r$ (a few hundred naut miles) and required t is small ($\lesssim 100$ sec), an all-warhead, nonpropulsive interceptor (i.e., a "space mine") may be indicated. For intermediate distances and allowable times, some optimal combination of warhead and maneuvering propulsion is required; for an O_2/H_2 interceptor having $F/W_0 = 1$, this combination region extends to $\sim$2000 naut miles and $\sim$500 sec. For larger X_{Tt} and t the all-propulsive interceptor is preferred. Although some shifting of these regions can be expected if r, F/W_0, and the propulsion method are varied, the foregoing values do not appear to be strong functions of any of these variables.

Solid propulsion is competitive only for very short intercept times where high acceleration can be decisive and where large nuclear bursts are ruled out for operational reasons. Oxygen-hydrogen propulsion is preferable throughout the intermediate range and up to $\sim$20,000 naut miles and 4000 sec; nuclear propulsion is preferable beyond these values. For example, an extrapolation from Fig. 2 reveals that where a warhead weight fraction of 0.1 is required in the "all-propulsive" case, then for $X_{Tt} \simeq 200{,}000$ naut miles and $t \simeq 20{,}000$ sec, a nuclear interceptor can cover nearly four times the volume of the given O_2/H_2 interceptor.

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Ocean-Height Measurement by Satellite Altimetry

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Introduction

SATELLITE altimeter experiments fundamentally represent a new way of gathering geodetic data. Because of this newness, very accurate orbits must be obtained for evaluation and use of the altimeter. Although this does not entirely preclude the use of altimeter data to determine the orbit, much more satisfactory and error-free analyses will be obtained if other tracking systems and methods are used for definitive determination of satellite position. The altimeter data can then be regarded as measurements from a known spacecraft position to the surface. This paper is concerned only with the accuracy attainable by dynamic long-arc orbit determination techniques. Other techniques such as short arcs and geometric determinations have not been treated.

A low inclination is favorable for a new geodetic satellite for several reasons. First, it is well known that present determinations of the zonal harmonics of the geopotential are deficient because of a lack of satellite data of geodetic quality with inclination below 30°. Second, the effect of longitude-dependent variations of the geopotential is generally reduced at low inclination, particularly in regard to resonance.[1] Since a resonance with a beat period of at least 2 days is always present, an inclination that minimizes resonance is a helpful factor.

The orbit determination requirements of satellite altimetry are severe. The separation of the geoid and the spheroid hardly reaches 100m. To be useful, measurement errors will have to be far smaller than this figure. Indeed, the nominal accuracy of the altimeter is planned to be 5m. The purpose of our investigation was to estimate the effect of model errors on the determination of satellite radial distance to see if the orbit determination requirements can be met and the altimeter accurately evaluated. Our results were obtained by simulation of least-squares solutions including the effect of model and instrument error in addition to random noise. The results show that if careful attention is given to scheduling of observations, orbits of the required accuracy can be obtained.

Tracking Systems

We considered tracking with STADAN optical, Unified S-Band (USB) radar, C-Band radar, and SAO Baker-Nunn optical systems. Figure 1 shows the positions of these instruments on a world map. In the case of the C-Band radars, we have studied only a partial system with, however, worldwide coverage.

The USB and SAO Baker-Nunn systems give excellent coverage of low inclination orbits whether inclined at $i = 20°$ or $i = 30°$. However, at $i = 20°$, the STADAN optical system is degraded, and certain important C-Band stations, e.g., Wallops Station, can no longer track. However, some STADAN and C-Band sites will be able to observe and will supplement previous GEOS analyses. A large amount of STADAN optical data is available for GEOS-I and II. These data plus SAO Baker-Nunn tracking data have been used with success to determine the locations of the STADAN optical stations. For the system as a whole, the center of mass position errors are 10m or less.[2] A third well-observed geodetic satellite would enable us to reduce this figure for some stations when the data is combined with data from GEOS-I and II.

Tracking Accuracy Studies

We analyzed (see Appendix) the error effects listed in Table 1 on the determination of the geocentric distance of a

Table 1 Tracking systems and model errors assumed for error analyses

Type	Frequency, min^{-1}	Data noise	Data bias	Station position error, m
SAO Baker-Nunn	10[a]	2 arcsec	none	20[c]
STADAN Optical	10[a]	2 arcsec	none	20
C-Band	60	5 m	2 m	20
Unified S-band-range	10	10 m	40 m[b]	20
USB range-rate	10	1 cm/sec	none	20

Gravity model error: SAO M1-SAO COSPAR (1969) to (8, 8) +20% error in resonant coefficients
GM error: 1:10^6

[a] Flash data, one sequence per pass.
[b] This bias is about $2\frac{1}{2}$ times the normal uncertainty (see text).
[c] 10 m survey, 10 m center of mass.

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